ANL-7120

Reactor Technology (TID-4500)

AEC Research and

Development Report

Argonne National Laboratory

9700 South Cass Avenue

Argonne, Illinois 60439

**PROCEEDINGS OF THE CONFERENCE ON
SAFETY, FUELS, AND CORE DESIGN
IN LARGE FAST POWER REACTORS**

October 11-14, 1965

David Okrent

Program Chairman

Administrative Arrangements

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Operated by The University of Chicago

under

Contract W-31-109-eng-38

with the

U. S. Atomic Energy Commission

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Preface

This volume contains the proceedings of the second in a series of annual topical conferences sponsored by Argonne National Laboratory and the U. S. Atomic Energy Commission on various specific aspects of fast reactor science and technology. The first conference, which was held in October 1963, was entitled "Breeding, Economics and Safety in Large Fast Power Reactors." The proceedings of that conference were issued as ANL-6792. No conference was held in 1964 because the Third International Geneva Conference on the Peaceful Uses of Atomic Energy had been scheduled for that year. In October 1959, a related conference entitled "The Physics of Breeding" was held at Argonne. The proceedings of that conference were issued as ANL-6122.

We at Argonne appreciate very much the assistance of all who participated in this conference. In particular, the open discussion and the willingness to examine controversial questions contribute much to the value of these meetings. We appreciate, too, the cooperation of the AEC in cosponsoring this program.

These proceedings have been edited by A. Glassner, Joyce Kopta, and D. Okrent. In the interest of hastening publication, the speakers themselves have not been given an opportunity to edit the discussions. The editors hope that a reasonable portrayal of the actual discussion has been accomplished.

David Okrent
Program Chairman

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October 11, 1965

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Session I
October 11, 1965

Physics Uncertainties
and
Safety Studies

Chairman: M. SHAW
U. S. Atomic Energy Commission

Secretary: C. E. Dickerman
Argonne National Laboratory

Summary of Intercomparison Calculations Performed in Conjunction with Conference on Safety, Fuels, and Core Design in Large Fast Power Reactors

D. OKRENT

*Argonne National Laboratory
Argonne, Illinois*

It was decided to conduct an intercomparison of calculations in connection with this Conference for the following reasons:

1. At the Conference on Breeding, Economics and Safety in Large Fast Power Reactors held at Argonne National Laboratory in October, 1963, a considerable difference of opinion arose concerning the magnitude of reactivity coefficients to be expected as a function of reactor design. Furthermore, as the group at the 1963 conference discussed large reactor designs, it was difficult to evaluate the relative merits of these concepts, since it was not clear which groups were calculating relatively optimistic numbers, and which groups were obtaining pessimistic numbers for each of the various parameters. Hence, for this conference an intercomparison of calculations on relatively simple systems was planned, to identify areas of difference and to provide more perspective for concept evaluations.

2. In a crude way the intercomparison of calculations was expected to provide some measure of the current uncertainties in predictions of nuclear parameters for large power reactors.

A group of representative, single-region, "bare" cores was defined. In addition, several of these cores were provided with blankets in spherical geometry. Finally, one reactor of a flat, cylindrical shape was specified. For all cases except the pancaked core, it was asked that core radius be varied to achieve criticality. Several parameters of potential interest were suggested in connection with each type of problem. Participation was completely voluntary, and the various institutions computed those problems which they found to be of particular interest.

Not all of the resulting numbers could be correlated in the short time available between receipt of the calculated results and the beginning of the Conference. Nor was there sufficient time to identify in detail reasons for the various differences which have appeared. The following tables and graphs, however, provide a rather good indication of the kind of comparison which has resulted.

In some cases, participating institutions did not have cross sections available at the specified fuel temperatures. Where possible, a rough correction to specified fuel temperatures was made at Argonne, but in some cases numbers have been reported for the original calculated temperature.

Most of the participating groups turned in a rather detailed description of the computing methods used, more than could be included in the proceedings. Only an abstract including some of the most vital information has been provided herein for each participant.

A detailed discussion of the resulting differences will not be provided in the proceedings. This is left for the reader to determine in whatever detail he desires. The reader will quickly ascertain that very large discrepancies have appeared in buckling or critical mass, and in the sodium void effect. Excepting the low values obtained by Brookhaven National Laboratory (BNL), differences in calculations of the Doppler effect were significant, but not of such great magnitude. The low values obtained by BNL can be traced rather directly to a reduced fraction of neutrons at lower energies. The variation between computed values of the breeding ratio and of the various cross-section ratios was considerably smaller. There appears to be some correlation between low values of the

$$\frac{\sigma_c \text{U}^{238}}{\sigma_f \text{Pu}^{239}}$$

and high values of buckling or overall reactivity.

In general, there seems to have been a consistent pattern to the discrepancies. Participating institutions tended to obtain either more reactive or less reactive results for all reactors. Similarly, void coefficients tended to be either more positive or more negative for all reactors. This is illustrated quite clearly for buckling on Figures 1 and 2.

Although there is some general trend of agreement for the adjoint flux, considerable differences do occur among the results from some institutions at the high and the low energies. Some have computed adjoints having a much flatter character at low energies, at

high energies, or both. Although some differences were introduced by fission products and Pu^{240} , the general trends of discrepancies observed for clean reactors were not modified markedly when reactors containing fission products and Pu^{240} were computed.

Further analysis into the causes for some of the discrepancies is being undertaken by H. H. Hummel of Argonne National Laboratory.

In conclusion, we wish to thank all the individuals and installations whose participation in this intercomparison has made possible this interesting group of results.

**SPECIFICATIONS OF CALCULATIONS FOR
INTERCOMPARISON TO BE MADE AT CONFER-
ENCE ON SAFETY, FUELS AND CORE DE-
SIGN IN LARGE FAST POWER REAC-
TORS TO BE HELD AT ARGONNE
NATIONAL LABORATORY,
OCTOBER, 1965**

Information Desired—Bare Cores

1. Buckling to go critical.
2. Doppler effect, $T(dK/dT)$. Assume:
Fuel OD = 0.25 in.; clad OD = 0.28 in.;
center to center distance = 0.38 in. (triangular pitch).
- 2a. (Optional). Compare consequences of specific theoretical assumptions on Doppler effect.
3. Coolant Void Effect (50 % Voiding):
 - a) contribution from elastic, inelastic, and capture terms;
 - b) adjoint flux as a function of energy.
4. Reactor Spectrum
 - a) as a function of energy;
 - b) as characterized by fissions below 25 keV and below 2.1 keV;
 - c) as characterized by spectral indices:

$$\frac{\sigma_f^{\text{U-238}}}{\sigma_f^{\text{Pu-239}}}; \quad \frac{\sigma_c^{\text{U-238}}}{\sigma_f^{\text{Pu-239}}}; \quad \frac{\sigma_c^{\text{Pu-239}}}{\sigma_f^{\text{Pu-239}}}; \quad \frac{\sigma_f^{\text{Pu-240}}}{\sigma_f^{\text{Pu-239}}}.$$
5. Error estimates.
6. Define basis of calculation, particularly method of handling cross sections.

Information Desired—Blanketed Spheres

1. Core radius for criticality, holding compositions fixed, as specified.
2. Critical mass of fissile isotopes. Breeding ratio.
3. Doppler effect, assuming space-independent ΔT in core, no ΔT in blanket.
4. Coolant coefficient of reactivity:
 - a) uniform voiding of core (50 %);
 - b) leakage component of coolant coefficient at 100 % coolant density by perturbation theory;

- c) spatial distribution of coolant worth per gram of coolant.

Information Desired—Blanketed Pancake Core

1. Take pancake core 2 ft high and 10 ft in diameter. Use composition typified by bare-core cases 11–14 (see Table 1). Vary ratio of Pu^{239} to U^{238} in core to go critical, holding total heavy atoms and ratio of Pu^{240} to Pu^{239} constant. Use 18-in.-thick blanket, radial and axial. Blanket composition, atoms/cc ($\times 10^{-24}$), but see Table 2 also.

	Na	Pu^{239}	U^{238}	Stainless Steel	O
Axial	0.0123	0.00007	0.0072	0.011	0.0144
Radial	0.0069	0.00012	0.012	0.011	0.024

2. Critical mass and breeding ratio.
3. What is sodium void effect for 50 % loss of sodium from core?
What are leakage and spectral components?

**BRIEF DESCRIPTIONS OF METHODS
USED**

E. R. SPECHT, D. W. LATHAM, AND O. R. HILLIG
Atomics International

The calculations were done with 15-group diffusion and perturbation theory. Homogeneous mixtures of the core materials were assumed in each case. Group-dependent boundary conditions characteristic of a vacuum interface were imposed at the outer radii of the cores.

Multigroup cross-section sets were prepared by averaging energy-dependent data from the AI-evaluated nuclear data tape over ELMOE spectra. Direct-flux weighting was used for all processes except neutron transport, for which ELMOE current spectra were employed.

Separate spectra, characteristic of each core composition, were used to average the elastic removal and transport cross sections for light and intermediate weight materials (Na, O, C, and SS). The spectra of cores 9 and 13 (see Table 1) were used to average the nonresonance portion of the heavy element cross sections for the oxide and carbide cores, respectively. This simplification is made possible by the insensitivity of these "smooth" cross sections to the details of the flux fine structure.

Resonance shielding and Doppler broadening of the fuel isotopes were computed by the narrow resonance approximation. Breit-Wigner single-level line shapes were assumed. Statistical sampling from distributions of neutron and fission widths ($\gamma = 3$) for unresolved

TABLE 1. BARE CORES
Concentration in Atoms/cc (10^{-24})

Case	Fuel Density,** g/cc	Na	Pu ²³⁹	U ²³⁸	Pu ²⁴⁰	C	Stainless Steel*	O	Fission Products	H ₂ O (molecules) cc	Avg Fuel Temp, °K
1	10.77	0.0123	0.0006	0.0066	—	—	0.011	0.0144	—	—	1500
2	10.77	0.0123	0.00072	0.00648	—	—	0.011	0.0144	—	—	1500
3	10.77	0.0123	0.0009	0.0063	—	—	0.011	0.0144	—	—	1500
4	10.77	0.0123	0.0012	0.0060	—	—	0.011	0.0144	—	—	1500
5	10.77	0.0123	0.0006	0.0066	—	—	0.011	0.0144	0.00072	—	1500
6	10.77	0.0123	0.0012	0.0060	—	—	0.011	0.0144	0.00072	—	1500
7	10.77	0.0123	0.0006	0.0066	0.0003	—	0.011	0.0144	0.00072	—	1500
8	10.77	0.0123	0.00072	0.00648	0.00036	—	0.011	0.0144	0.00072	—	1500
9	10.77	0.0123	0.0009	0.0063	0.00045	—	0.011	0.0144	0.00072	—	1500
10	10.77	0.0123	0.0012	0.0060	0.0006	—	0.011	0.0144	0.00072	—	1500
11	12.8	0.0123	0.00075	0.00825	0.0003	0.009	0.011	—	0.0009	—	1100
12	12.8	0.0123	0.0009	0.0081	0.00036	0.009	0.011	—	0.0009	—	1100
13	12.8	0.0123	0.001125	0.007875	0.00045	0.009	0.011	—	0.0009	—	1100
14	12.8	0.0123	0.0015	0.0075	0.0006	0.009	0.011	—	0.0009	—	1100
15	17.6	0.0123	0.0012	0.0108	0.0004	—	0.011	—	0.0012	—	900
16	10.77	—	0.0006	0.0066	0.0003	—	0.011	0.0144	0.00072	0.0016	1500
17	10.77	—	0.00072	0.00648	0.00036	—	0.011	0.0144	0.00072	0.0016	1500
18	10.77	—	0.0009	0.0063	0.00045	—	0.011	0.0144	0.00072	0.0016	1500
19	10.77	—	0.0012	0.0060	0.0006	—	0.011	0.0144	0.00072	0.0016	1500

* Assume stainless steel composition is like SAE 304 (~18% Cr, 8% Ni). Do not include manganese.

** Density of Pu²³⁹, U²³⁸, and appropriate light elements, assuming 30 v/o fuel.

resonances was used. Resonance overlap was taken into account approximately by neglecting the temperature dependence of the integrated flux in the denominator of the effective cross sections for Pu²³⁹. Most of the resonance parameters were taken from Garg *et al.*,⁽¹⁾ (U²³⁸), Schmitt (Pu²³⁹), and Barrington (Pu²⁴⁰). The resonance weighting functions include only local flux fine structure and neglect more gradual depletion of the flux due to accumulative absorption.

The methods of cross-section generation and eigenvalue analysis used in the present study are the same as those employed in the modular-core work with certain unavoidable exceptions. [The early modular surveys used an old cross-section set derived from Hansen-Roach data and AETR experience. Our present cross-section methods came to completion during the coupled core work and were used in the final analysis of reference modular design.] Our modular analyses required a group structure extending into the thermal energies to describe the effects of strong local moderation. We have retained this structure in order that there be some basis of evaluation of the work to be described at the conference.

(1) See NAA-SR-Memo-11517.

TABLE 2. BLANKETED SPHERICAL REACTORS
(18-IN.-THICK BLANKETS)

Cases 8 and 10

Blanket Composition, Atoms/cc ($\times 10^{-24}$)

Na	Pu ²³⁹	U ²³⁸	Stainless Steel	O
0.0069	0.00012	0.012	0.011	0.024

Case 12

Blanket Composition, Atoms/cc ($\times 10^{-24}$)

Na	Pu ²³⁹	U ²³⁸	Stainless Steel	C
0.0069	0.00015	0.015	0.011	0.015

Case 15

Blanket Composition, Atoms/cc ($\times 10^{-24}$)

Na	Pu ²³⁹	U ²³⁸	Stainless Steel
0.0069	0.0002	0.02	0.011

Cases 17 and 19

Blanket Composition, Atoms/cc ($\times 10^{-24}$)

Pu ²³⁹	U ²³⁸	Stainless Steel	O	H ₂ O mol/cc
0.00012	0.012	0.011	0.024	0.0016

Preparations of Cross Sections

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Fission and capture cross sections were generated by use of the current version of the Argonne cross-section code MC². This code calculates cross sections for 0.25 lethargy groups, which are then collapsed to the 22 groups used in the calculations. Fission and capture cross sections above 25 keV were assumed independent of temperature and composition. Below 25 keV, resonance integral calculations were performed. At present, MC² uses Goldstein-Cohen interpolation between narrow and wide resonance calculations. Heterogeneity was taken into account by the use of equivalence theory.

A one-level calculation was used in all cases. The Columbia resonance parameters, extending up to 4 keV, were used for U²³⁸. Flux correction factors were used to provide an approximate fertile-fissile overlap correction.

Fundamental mode calculations for Cases 7–15 (see Table 1) were performed directly by ELMOE, using the double lethargy spacing. Apparent transport and elastic removal cross sections for voiding sodium were obtained by subtracting the appropriate macroscopic cross sections for the voided case from those for the unvoided case and dividing by the atoms/cc of sodium voided. For Cases 1–6 of Table 1, the appropriate ELMOE homogenized macroscopic core cross sections were altered by removing Pu²⁴⁰ and fission product contributions. Light-element cross sections were assumed to be unaffected by this process.

Blanket-homogenized macroscopic cross sections were obtained by running ELMOE with $B^2 = 0$ and inputting a fission spectrum source.

Heavy-element Cross Sections

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Infinite-dilution cross sections for U²³⁸, Pu²³⁹, and Pu²⁴⁰ above 1 keV were taken from the English Data File.⁽¹⁾ Below 1 keV, Pu²³⁹ cross sections were those of Schmidt.⁽²⁾ U²³⁸ cross sections up to 3.9 keV were calculated from the resonance parameters.⁽³⁾

Self-shielding was calculated both at core sodium densities of 100% and 50% by means of the APDA Doppler program, IDIOT, which is described in more detail in the section on Doppler calculations. Twenty-four groups were used for the calculations. Groups 15 to 20 were each subdivided into two groups for the

cross-section calculation and then flux weighted by the calculated ELMOE spectrum for the 24-group calculations. Heterogeneity was taken into account using a modified nonresonant cross section given by

$$(1) \quad \Sigma_N = \Sigma_p + \Sigma_m + \frac{\Sigma_s V_s / V_f}{1 + (1 - \tau) \frac{\Sigma_s V_s \bar{l}}{V_f}};$$

$$\tau = \sqrt{\frac{\pi}{2\sqrt{3}}} \frac{\sqrt{1 + (V_s/V_f)} - 1}{V_s/V_f} - 0.12.$$

This formula is obtained from the Dancoff corrections given by Sauer⁽⁴⁾ for the limiting case where the Dancoff factor C approaches unity. In this formula, Σ_p is the heavy-element nonresonant scattering in the rod, Σ_m is the oxygen scattering in the rod, Σ_s is the homogenized scattering of steel and sodium, V_s/V_f is the volume ratio of steel and sodium to that of fuel, $\bar{l}$ is the mean chord length for the fuel rod, and τ is the geometrical index given by Sauer for a hexagonal lattice. The choice of V_s/V_f lies between the ratio for the lattice and for the homogenized core. For the calculations presented here, the lattice ratio was chosen. This yields a sodium/fuel ratio of 1.3 and a cladding/fuel ratio of 0.25, whereas the homogeneous ratios are estimated to be about 1.86 and 0.43. This section of lattice ratios tends to give the largest difference between the heterogeneous and homogeneous nonresonance cross sections. Although the effect of the heterogeneity correction is small for criticality calculations, it is more significant for Doppler calculations.

Doppler Calculations

Self-shielded effective cross sections and temperature derivatives for the Doppler calculations were obtained from the IDIOT Doppler program. This program, written by D. G. Green at APDA, is currently under development. The following approximations are available in this program: resolved resonance calculations with optional use of the intermediate resonance approximation of Goldstein,⁽⁵⁾ unresolved resonance calculations in the isolated resonance approximation, overlap corrections to a single resonance sequence using the series expansion of Hwang,⁽⁶⁾ and the high-energy Method A approximation.⁽⁷⁾ Overlap corrections between different isotopes or different resonance sequences are not calculated as they have been shown to be negligible when the temperature-dependent flux normalization is included in the calculation. The

(1) K. Parker, AWRE 0-79/63; A. C. Douglas and J. F. Barry, AWRE 0-79/64; A. C. Douglas, AWRE 0-91/64.

(2) J. J. Schmidt, KFK 120 (EAN)C-E-35 U).

(3) J. B. Garg *et al.*, Phys. Rev., **134**, 5B (1964).

(4) A. Sauer, Nucl. Sci. Eng., **16**, 329 (1963).

(5) R. Goldstein and E. R. Cohen, Nucl. Sci. Eng., **13**, 132 (1962).

(6) R. N. Hwang, Nucl. Sci. Eng., **21**, 523 (1965).

(7) R. B. Nicholson, APDA-139 (1960).

Doppler calculations were made by means of the above methods over their appropriate energy ranges. Through an input error in the program, the temperature derivatives were evaluated at a temperature of 1800°K. A $1/T$ temperature dependence was applied to obtain the values for the given reactivity coefficients at 1500°K. It should be noted that the temperature derivatives were evaluated directly rather than using the difference between cross sections over a temperature difference. Heterogeneity was included by use of the nonresonant cross section of Equation 1.

For U^{238} the resolved resonance data of Garg *et al.*,⁽³⁾ was used to 3.9 keV. Above this energy an average spacing of 19.0 eV and strength functions of 0.96×10^{-4} and 1.25×10^{-4} for s and p waves respectively were assumed. The calculated cross sections were then normalized so that the infinite-dilution cross section agreed with the English Data File. The U^{238} Doppler coefficient was also calculated.

Resonance parameters for Pu^{239} were obtained by requiring that the parameters yield the fission cross section and alpha values of the English Data File. When only s waves are present, both the fission and neutron widths are obtained by this fitting procedure. When p waves are present, an s-wave strength function of 1.0×10^{-4} was assumed to obtain the s-wave neutron widths, and the fission and p-wave neutron widths were obtained by the fitting procedure. A chi-squared distribution parameter of 2.0 was assumed for the fission widths.

For Pu^{240} , the resonance parameters of Froehlich and Ott⁽⁸⁾ were used. The s-wave neutron width was obtained from a strength function of 1.0×10^{-4} and the p-wave widths by requiring that the cross section for compound nuclear formation by p waves be of the same as that for U^{235} .

Calculation of Various Fast Reactor Cores

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Basis of Calculation

The calculations were performed utilizing the Babcock and Wilcox LIFE X code in combination with the current Babcock and Wilcox Standard Fast Cross Section 34 Group Library. The LIFE X nuclear design package consists of a series of individual nuclear design codes arranged in such a manner as to provide a complete program for one-dimensional criticality and core depletion studies. The core depletion portion was not used in this study. Three fast and one thermal macro-

group coefficients were generated by means of RIP, MUFT 5, and KATE 1 subroutines. RIP calculates the resonance integral; self-shielding and Doppler broadening of neutron resonances are calculated by the Adler-Nordheim method with the Dancoff factor calculated by Sauer's method. MUFT 5 uses a Fourier transform to calculate group constants for the three fast groups. The B1 approximation was used. The capture and fission cross sections used in MUFT 5 consist of a smooth and a resonance portion. The resonance term is only that part due to the neutron resonances. The smooth and resonance portions are combined together for each isotope by use of the relationship

$$\sigma_m = \left(\sigma_m^{\text{smooth}} + \frac{R_m}{\Delta U_m} \right),$$

where σ_m^{smooth} is the smooth cross section of micro-group m , R_m is the shielded resonance integral calculated by RIP for micro-group m , and ΔU_m is the lethargy width of the micro-group m . In the present set of calculations, only U^{238} , Pu^{239} , and Pu^{240} have nonzero values of R_m . The 34-group fast micro-group cross sections are condensed into 3 fast macro-groups by RIP and MUFT 5. KATE 1 calculates the spectrum of thermal neutrons according to the Wigner-Wilkins theory and averages those quantities important to reactor design over this spectrum. The fast and thermal macro-group few-group constants were then used in the WANDA segment of LIFE X to calculate k_{eff} and the few-group fluxes.

A pancake core, 2 ft high and 10 ft in diameter, with a composition typified by bare cores 11–14 (see Table I) was also studied. Axial and radial blankets, 18 in. thick, were placed around the core. The blanket compositions were as follows:

	Na	U^{239}	U^{238}	Stainless Steel	O
Axial	0.0123	0.00007	0.0072	0.011	0.0144
Radial	0.0069	0.00012	0.012	0.011	0.024

The Pu/U ratio was varied in the core portion until the reactor was just critical. The results of these calculations are listed in Table 3.

In order to obtain the perpendicular buckling (and hence the perpendicular leakage), calculations were made for the core in the perpendicular direction with the blanket removed. The core dimension in that direc-

TABLE 3. PANCAKE CORE RESULTS

k_{eff}	= 1.00008
k_{eff} (50% void)	= 1.00539
Mass of U^{238} in core	= 13,829 kg
Mass of Pu^{239} in core	= 1,884 kg
Mass of Pu^{240} in core	= 757 kg
Breeding Ratio	= 1.42

(8) R. Froehlich and K. Ott, Nucl. Sci. Eng., **22**, 490 (1965).

tion was increased until k_{eff} was identical to the blanketed core. This bare-core buckling was then assumed to give the proper perpendicular leakage when the core was calculated in the normal direction.

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The AIM-6 and the 20-GRAND multigroup codes with Yiftah, Okrent, and Moldauer cross sections were used for all this work. The cross-section changes due to Doppler effect were obtained with a modification of the GAM code which allows resonances to have fission as well as capture. The statistical distributions of the neutron and fission widths were also taken into account.

The pancake core was calculated in a two-dimensional representation with the use of six-group cross sections. These were obtained by collapsing the sixteen-group cross sections over spectra obtained from suitable one-dimensional calculations. The overall reactivity change due to sodium voiding was obtained by direct calculation; the breakup into separate components due to leakage, spectral, and capture effects was done by perturbation theory.

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Characteristics of the Cross-section Set

The main part of this intercomparison has been made by using the Russian 26-group set⁽¹⁾ with minor modifications. For each reactor the resonance self-shielding correction factors $f(\sigma_0, T)$ have been applied to the cross sections of fissile and fertile isotopes and to the capture cross sections of higher nuclei.

Transport cross sections and elastic removal cross sections of carbon, oxygen, sodium, iron, chromium, and nickel have been calculated by using ELMOE and its attached library of B_L (Legendre polynomial expansion).⁽²⁾

Bare Reactors

For the six bare reactors the ELMOE code has been used with 670 energy groups and the P_1 approximation. Flux and buckling of each reactor have been calculated. Transport and elastic removal cross sections collapsed in 24 groups from the ELMOE calculation have been used by the adjoint "HOB0" code.

A special code "SEPR 508" starts from direct and adjoint fluxes and computes the Doppler effect.

Blanketed Reactors

The three blanketed reactors have been calculated by using a one-dimensional diffusion code "MUDE." Elastic removal cross sections in the core had been given by ELMOE; removal cross sections in the blanket have been given by the method described below.

Doppler Coefficient

In the energy range where resonances strongly overlap, the temperature dependence of the effective cross section has been calculated by the classical method A of Nicholson.

In the energy range in which resonances are well isolated, experimental resonance parameters have been used with the one-level Breit-Wigner formula to calculate the effective cross section as a function of the dilution and the temperature.

In the energy range in which resonances weakly overlap, the same formalism as above has been followed; it was assumed that the resonance parameters can be extrapolated from the measured ones at lower energy, using an integration step of 0.1 eV.

All the Doppler coefficients have been calculated at 900°K, assuming a homogeneous medium.

The influence of heterogeneity has been evaluated on blanketed reactor No. 8 (see Table 2). The Bell theory has been applied to the heterogeneous case of two zones (the first one with U^{238} - Pu^{239} -O-fission products; the second one with Na-Fe-Cr-Ni) to calculate the dilutions. Flux and adjoint flux of the homogeneous case have been used.

The various components of the Doppler effect have been increased by 23 % for Pu^{239} , by 42 % for U^{238} , and by 17 % for Pu^{240} ; this leads to an overall increase of the Doppler coefficient of 1.8 % only due to compensation between different isotopes.

Comments

Core data with 100 % Na and 50 % Na have been obtained through two successive ELMOE calculations. Removal cross sections for the blanket have been calculated using the methods described in the section below.

Comparison of Various Cross-section Sets—Influence of the Elastic Slowing Down

Blanketed reactor No. 8 was chosen for the comparison of the following cross-section sets:

The Russian 26-group set,⁽¹⁾ of which the elastic removal cross sections have been treated by three different methods.

The elastic removal cross sections have been recalculated

(1) L. P. Abagyan *et al.*, Atomizdat (1964).

(2) A. L. Rago and H. H. Hummel, ANL-6805.

TABLE 4. COMPARISON OF THE MAIN PARAMETERS FOR THE BLANKETED REACTOR NO. 8

Parameters	Russian Elmoe	Russian evaluated b	Russian $b = 1$	Hansen and Roach	SEPR 2	SEPR 2 Elmoe
Critical Radius (cm)	176.19	165.58	155.64	156.0	160.21	156.90
Critical Mass of Pu-239 (kg)	6546	5433	4512	4543	4921	4622
<i>Breeding Ratio</i>						
Internal	1.315	1.297	1.266	1.256	1.250	1.250
External	0.193	0.245	0.268	0.282	0.245	0.241
Total	1.508	1.542	1.534	1.538	1.496	1.491
<i>Doppler Coefficient</i>						
$(\Delta k/k)/^{\circ}\text{C} \times 10^5$						
U-238	-1.174	-1.132	-1.281	-1.518	-1.488	—
Pu-239	+0.129	+0.117	+0.140	+0.146	+0.132	+0.132
Pu-240	-0.034	-0.033	-0.041	-0.046	-0.042	—
Total	-1.079	-1.048	-1.182	-1.418	-1.398	—
<i>Sodium Coefficient of Reactivity (50% voiding)</i>						
a) Direct Calculation						
$k_{0.5\text{Na}} - 1$	0.0326	0.0248	0.0241	0.0203	—	—
b) Component at 100% Na by Perturbation Theory						
Capture	+0.0014	+0.0013	+0.0013	+0.0017	+0.0021	+0.0021
Degradation	+0.0318	+0.0286	+0.0266	+0.0226	+0.0265	+0.0269
Leakage	-0.0055	-0.0061	-0.0066	-0.0071	-0.0058	-0.0059
$(\Delta k/k)$ Total	+0.0278	+0.0238	+0.0213	+0.0172	+0.0228	+0.0231
Prompt Neutron Life-Time (10^{-8} sec)	43.22	43.65	44.84	52.16	49.18	48.66

lated through the ELMOE code ("Russian ELMOE" set).

The elastic removal cross sections of Ref. 1 have been corrected by the b factor (ratio of the flux at the lower boundary to the average flux of the group). b was evaluated by a one-iteration procedure ("Russian procedure" ("Russian evaluated b " set).

The elastic removal cross sections of Ref. 1 were used without any correction ("Russian $b = 1$ " set).

16-group Hansen and Roach set.⁽³⁾

A special 25-group set designed at Cadarache from the following hypotheses:

Group width is 0.5 between 3.7 MeV and 275 eV; the energy range from 14 MeV to 0.4 eV is covered by 24 groups; the 25th group is thermal.

Fundamental data result from various evaluations: the British⁽⁴⁾ for U-238 and Pu-239 at high energy, Na, O, and Fe; the Germans⁽⁵⁾ for Pu-239 below 10 keV; the French⁽⁶⁾ for U-238 at low energy, Ni, Cr, fission products, Pu-240, and ν of Pu-239.

Multigroup constants have been calculated by the GALAXY-3 code.⁽⁷⁾ The weighting spectrum was

$1/\Sigma_t E^n$, where Σ_t is the total macroscopic cross section of the medium and n varies from -2 to $+2$ according to the energy range.

Below 50 keV, resonance self-shielding corrections have been evaluated by two codes:

RDT: a resonance integral code based on the narrow resonance approximation;

SEPR 549 S: a code computing the effective cross section directly from fundamental microscopic cross sections generated by a Doppler-broadened one-level resonance formula. The resonance parameters had been extrapolated from the lower energy region by means of statistical laws.

In the "SEPR 2 ELMOE" set, removal and transport cross sections have been evaluated through ELMOE.

Results of this comparison are summarized in Table 4.

Comments

In order to explain the differences between the calculated critical radii, the differences between the various calculated core k_{∞} have been split into the contributions given in Table 5. It can be seen that the Hansen and Roach set is the most reactive. Up to now studies carried out at Cadarache on reactors without Pu-240 and fission products had shown the contrary.⁽⁸⁾ This

(8) J. M. Chaumont *et al.*, PNR/SEPR 65.017.

(3) G. E. Hansen and W. H. Roach, LAMS-2543.

(4) E. Barrington *et al.*, AEEW-R-351 (July 1963).

(5) J. J. Schmidt, KFK-120.

(6) J. Ravier, PNR/SEPR/CA 65.010.

(7) V. Bell *et al.*, AREW-R-379.

TABLE 5. CONTRIBUTIONS TO k_∞

Isotopes	$k_{\text{SEPR}}^\infty - k_{\text{Russian ELMOE}}^\infty$	$k_{\text{Russian b=1}}^\infty - k_{\text{Russian ELMOE}}^\infty$	$k_{\text{Russian evaluated } b}^\infty - k_{\text{Russian ELMOE}}^\infty$	$k_{\text{H\&R}}^\infty - k_{\text{Russian ELMOE}}^\infty$
U-238	-0.008	+0.016	+0.003	-0.002
Pu-239	+0.020	+0.002	+0.004	+0.023
Pu-240	+0.008	+0.001	-0.0003	+0.029
O-Na-Fe-Cr-Ni	-0.004	+0.001	+0.001	-0.028
Fission Products	-0.004	-0.003	-0.0002	+0.012
Δk Total	+0.011	+0.016	+0.008	+0.034

can be explained by the too-weak capture cross sections in both the fission products and Pu-240 of the Hansen and Roach set (without fission products and Pu-240, $k_{\text{H\&R}}^\infty - k_{\text{Russian ELMOE}}^\infty = -0.007$). On the other hand, capture in chromium and nickel seems to be over-evaluated.

The Russian set with three different ways of dealing with elastic scattering gives different results. However, Russian ELMOE and Russian with evaluated b give very similar data.

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The bare cores were calculated by use of 60 energy groups. The basic 60-group fast reactor data file used at General Electric carries essentially the same information as the ANL set No. 224 (ANL-6858). This 60-group file has been adjusted to agree, for all practical purposes, with ANL No. 224, with the following exceptions:

The Fe σ_c above 1 MeV is higher in our set.

U^{238} σ_c above 67 keV is higher (as in ANL-5800), close to the U.K. and Russian sets.

Pu^{239} σ_f and σ_c from 0.5 to 3.3 keV are lower and agree with the data of J. J. Schmidt (KFK 120, EANDC-E-35U, December, 1962).

For the sodium resonance $\Gamma_\gamma = 0.35$ was used.

For each of the blanketed cores an appropriate zero-dimensional calculation was performed with the 60 groups; then, based on the resulting energy distribution of the flux, the cross-section information was condensed to a 24-group set.

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For these calculations the Karlsruhe 26-group set (called KFK 26-10) and partially the Russian 26-group set⁽¹⁾ (called ABN) was used. Both sets are shielded-

group-constant sets with self-shielding factors depending on the special composition. The energy boundaries, the lethargy intervals, and the mechanism of shielding is the same.

In the beginning of our calculations the KFK 26-10 SET was available for a 900°K fuel temperature only. Therefore all results obtained by the KFK 26-10 SET are valid for 900°K. For comparison and interpolation we used in some cases (Blanketed Spheres, Cases 8 and 12 of Table 2) the KFK 26-10 SET (at 900°K) and the 26-ABN SET (for 300, 900, and 2100°K), respectively.

For the Doppler-effect calculations all cores were treated as homogeneous.

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Computational Procedure

A two-dimensional model was used for the calculations. The cross sections used were those of the Hansen-Roach 16-group library.⁽¹⁾ For fission products, a capture cross section based on Moldauer's data⁽²⁾ was used, but no scattering cross section. Resonances in the fuel and fertile materials were treated according to the scheme suggested by Hansen and Roach,⁽³⁾ and cross sections so modified are designated as "shielded" in the results.

Two-dimensional criticality calculations by means of CRAM⁽⁴⁾ were done, followed by two-dimensional S_n calculations.⁽⁵⁾ Also, one-dimensional S_n calculations were done in which the reactor was taken to be a "circular" slab, i.e., input data for the 1-D slab was derived. The side leakage was approximated with a buckling equivalent to $(j_0/R)^2$.

(1) L. D. Connolly, LAMS-2941 (July 1963).

(2) *Proceedings of the Conference on the Physics of Breeding*, ANL-6122 (Oct 1959), p. 73.

(3) G. E. Hansen and W. H. Roach, LAMS-2543 (Dec 1961).

(4) A. Hassitt, UKAEA TRG Report No. 229(R) (1962).

(5) B. G. Carlson and K. D. Lathrop, LA-3251-MS (April 1965).

(1) L. D. Abagian *et al.*, *Group Constants of Fast and Intermediate Neutrons for Calculation of Reactors*, Physical-Energetical Institute of the State Committee for the Use of Atomic Energy in USSR.

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The calculations, corresponding to a fuel temperature of 300°K, were made with the use of the multi-group diffusion theory program CRAM,⁽¹⁾ with the 33-group cross-section set called F.D.2. This cross-section set has been constructed for calculations on large dilute fast reactors. Most of the group cross sections have been obtained from U.K.A.E.A. Nuclear Data Library using the data-processing program GALAXY⁽²⁾ with a weighting spectrum approximately representing the spectrum of a large fast reactor core voided of coolant.

The code ELMOE⁽³⁾ has been used for selected group elastic moderation cross sections for oxygen, sodium (the sodium cross sections are the differences between the macroscopic group cross sections for the core with and without sodium per atom of sodium), iron, chromium, and nickel. Use has also been made of the recent compilation of Yiftah and Sieger⁽⁴⁾ for nickel and chromium and of Schmidt⁽⁵⁾ for iron.

The group structure and the resonance shielding factors were the same as those in the set F.D.1,⁽⁶⁾ and ten down-scatter groups are given where appropriate.

In separating the infinite-dilution cross sections given by GALAXY into resonant and nonresonant components, the ratio of resonant to nonresonant was kept approximately equal to that in F.D.1 whilst the sum was kept equal to the GALAXY infinite-dilution cross section.

The only allowance made for the heterogeneity of the composition has been to modify the resonance shielding factors for uranium and plutonium isotopes by using adjusted values of σ_p^g , the nonresonant cross section per absorber atom for group g . The contributions to the σ_p^g 's from material outside the fuel (sodium and steel) were reduced by a Bell-type approximation, and these reduced values were input to the programme.

(1) A. Hassitt, TRG Report No. 229(R).

(2) V. J. Bell *et al.*, AEEW-R.379.

(3) A. L. Rago and H. H. Hummel, ANL-6805.

(4) S. Yiftah and M. Sieger, IA.980.

(5) J. J. Schmidt, KFK 120 (EANDC-E-35 U).

(6) J. L. Rowlands and D. Wardleworth, AEEW-11.472.

G. M. S. Computations in Support of the 1965 A. N. L. Intercomparison of Fast Reactor Calculations

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Method of Calculation Used—Cases 16–19 of Table 1

The method of calculation used was that contained within the computer code GMS⁽¹⁾ written in FORTRAN for the KDF 9 and the IBM 7030 computers.

(1) C. P. Gratton and P. E. Smith, AEEW-M.458.

GMS is a generalised multigroup system of calculations which has been used for the study of both thermal and fast reactors. The code is coupled with a nuclear data library system and, depending on the reactor, either a data set with emphasis in the thermal-energy region is selected or one appropriate for fast reactor studies. In the present work, the fast reactor data set known as FD 2⁽²⁾ has been used.

GMS is written to use 40 energy groups of neutrons, the lowest seven of which cover the region of thermal energy. From Group 1 to Group 14 the structure follows that of the YOM set,⁽³⁾ and from Group 15 to Group 33 that of the extension by Rowlands and Wardleworth.⁽⁴⁾ The lowest seven groups are included primarily to deal with hydrogen-containing systems, but their inclusion also allows calculations of blanket conditions where thermal-neutron events are of importance.

The Winfrith DSN code⁽⁵⁾ is included as a subroutine within GMS, allowing transport calculations of system heterogeneity and overall reactors to be performed by DSN methods. These calculations are all in one dimension, and include options for slab, cylindrical, and spherical geometry. For many calculations, fewer groups provide a sufficiently accurate solution and because of this, a feature has been included to permit a condensation of the data to fewer groups. To carry out this condensation, a 40-group neutron spectrum characteristic of the system is required. This is obtained in one of two ways:

- (i) For a core region a fundamental-mode calculation is carried out using the full 40-group representation.
- (ii) For a blanket region a flux spectrum for an infinite medium is evaluated, using as a source a fission spectrum.

Having obtained these spectra for a particular system, calculations with an increasing number of groups can be performed to establish the number required to achieve a sufficiently accurate solution to the problem.

(2) R. W. Smith, to be published.

(3) S. Yiftah, D. Okrent, and P. Moldauer, *Fast Reactor Cross Sections*, Pergamon Press, New York (1960).

(4) J. L. Rowlands and D. Wardleworth AEEW-M.472.

(5) S. Francescon, AEEW-R.273.

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The calculations were performed using DS₄ approximation to the transport-theory equation for cores with radii less than 110 cm, and DS₂ for cores with radii greater than 110 cm. The cross sections used are those given by the 16-group infinite-dilution Hansen-Roach⁽¹⁾ set modified as follows:

1. Self-shielding and Doppler broadening were ac-

(1) G. E. Hansen and W. H. Roach, LAMS-2543.

counted for using the procedures⁽²⁾ programmed into the United Nuclear BIGSUM code.

2. Sodium cross sections in the region of the 2.85-keV resonance were corrected in terms of the data of Hibdon.⁽³⁾

(2) C. C. Graves, UNC-5064 (May 1964).

(3) C. T. Hibdon, Phys. Rev., **118**, 515 (1960).

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Calculations of criticality, spectral indices, and reactivity changes were performed by means of multigroup, one-dimensional diffusion theory. Cross sections were based on the YOM⁽¹⁾ set, but with the following modifications: (1) energy structure below 10 keV modified

(1) S. Yiftah, D. Okrent, and P. A. Moldauer, *Fast Reactor Cross Sections*, Pergamon Press, New York (1960).

and extended; (2) plutonium and uranium cross sections updated and calculated for self-shielding effects of each case;^(2, 3, 4) (3) scattering cross sections of light-element resonances determined from the ELMOE fine-energy-structure code.^(3, 5)

The Doppler coefficients of reactivity were determined from temperature-dependent cross sections of the heavy isotopes. The Doppler-broadened cross sections of U²³⁸, U²³⁹, and Pu²⁴⁰ were determined from the ARES-I code,⁽³⁾ based on resonance integral techniques. The NR (narrow resonance) and NRIM (narrow resonance infinite mass) approximations were assumed. No provision was made for interference effects between resonances.

(2) F. M. Heck, WCAP-3251-1 (1964).

(3) M. G. Blide and H. H. Hummel, ANL-6601 (1962).

(4) F. L. Fillmore, AMTD-130 (1962).

(5) A. L. Rago and H. H. Hummel, ANL-6805 (1964).

SUMMARY AND COMPARISON OF RESULTS

	$B^2 \times 10^4$	% δk for Removal of 50% Na		Doppler Effect ($-Tdk/dt$)	$\frac{\sigma_f U^{238}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c U^{238}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c Pu^{239}}{\sigma_f Pu^{239}}$	% Fissions Below:	
		Total	Spectral + Capture					25 keV	2.1 keV
BARE CORE NO. 1 11:1 Oxide, no F. P. of Pu ²⁴⁰									
AI	2.78	1.46	2.39	.0237	.017	.14	.33	33.9	15.8
ANL	-0.46		3.54	.0184	.016	.16	.30	32.7	15.7
BNL	1.0	4.09	4.23	.0042	.019	.16	.24	21.6	5.1
B&W	1.42	.77		.0243	.017	.15	.33	31.4	23.5
GE	1.67	1.59	1.94	.0208	.017	.14	.27	30.4	15.8
Karlsruhe	neg								
UK	neg		1.9 (300°)	.030	.016	.16	.31	36.2	20.8
Median Value			2.39	.0222	.017	.15	.30	32.0	15.8
% Spread:									
Plus			77% BNL	35% UK	12% BNL	6% ANL	10% AI- B&W	13% UK	49% B&W
Minus			20% Uk	81% BNL	6% UK- ANL	6% AI-GE	20% BNL	32% BNL	68% BNL
BARE CORE NO. 2 9:1 Oxide, no. F. P. or Pu ²⁴⁰									
AI	3.34	2.24	2.66	.0140	.018	.16	.28	29.3	13.2
BNL	4.7	1.56	3.26	.0030	.022	.15	.23	18.9	4.1
B&W	5.18	- .91		.0184	.020	.15	.30	27.8	20.0
France	~3.5	1.3 (900°)	2.4 (900°)	.0116	.020	.16	.27	26.3	10.8
GE	5.10	- .31	1.56	.0150	.020	.14	.25	26.7	12.8
Karlsruhe	~2.8	1.6 (900°)	2.8 (900°)	.0149	.020	.17	.26	27.2	11.0
UK	~2.3		1.6 (300°)	.0223	.019	.16	.29	32.4	17.3
UNC	3.02	1.18		.0100	.022	.16	.21		
Median Value	3.42	1.3	2.53	.0145	.020	.16	.26	27.2	12.8
% Spread									
Plus	51% B&W	72% ANL	29% BNL	54% UK	10% BNL- UNC		15% B&W	19% UK	56% B&W
Minus	33% UK	B&W	38% GE	79% BNL	10% ANL	12% GE	19% UNC	30% BNL	68% BNL

	$B^2 \times 10^4$	% δk for Removal of 50% Na		Doppler Effect ($-Tdk/dt$)	$\frac{\sigma_f U^{238}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c U^{238}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c Pu^{239}}{\sigma_f Pu^{239}}$	% Fissions Below:	
		Total	Spectral + Capture					25 keV	2.1 keV
BARE CORE NO. 3 7:1 Oxide, no F. P. or Pu ²⁴⁰									
ANL	8.34	-.75	2.05	.0092	.021	.15	.25	24.9	10.2
BNL	9.6	-.96	2.26	.0018	.026	.14	.20	15.6	3.0
B&W	10.28	-2.76		.0125	.023	.15	.27	23.4	15.9
France	~8.4	-1.0 (900°)	1.7 (900°)	.0083	.024	.15	.24	22.2	8.0
GE	9.80	-2.37	1.11	.0093	.023	.14	.22	22.2	9.5
Karlsruhe	~7.7	-.46 (900°)	~1.7 (900°)	.0098	.024	.17	.24	23.2	8.4
UK	~7.6		1.2 (300°)	.0147	.022	.15	.25	27.7	13.3
UNC	8.11	-.75		.0060	.026	.16	.19		
Median Value	8.37	-.96	1.7	.0092	.023	.15	.24	23.2	9.5
% Spread:									
Plus	23% B&W	K	33% BNL	60% UK	13% BNL- UNC	7% UNC	12% B&W	19% UK	67% B&W
Minus	9.0% Uk	B&W	35% GE	80% BNL	8% ANL	7% GE- BNL	21% UNC	33% BNL	58% BNL
BARE CORE NO. 4 5:1 Oxide, no F. P. or Pu ²⁴⁰									
AI	18.2	-3.64	.53	.0067	.030	.12	.23	15.6	4.84
ANL	15.6	-3.65	1.02	.0049	.027	.14	.21	19.3	6.71
APDA	16.6	-3.79	.97	.0055	.031	.14	.19	17.8	6.18
BNL	16.6	-3.46	1.26	.0009	.032	.13	.17	11.6	1.87
B&W	17.9	-4.53		.0068	.029	.14	.23	18.0	11.2
France	15.6	-3.4 (900°)	1.04 (900°)	.0034	.030	.14	.21	17.1	5.1
GE	16.5	-4.49	.60	.0046	.029	.13	.19	16.8	6.0
Karlsruhe	~15.0	-2.8 (900°)	~1.1 (900°)	.0052	.030	.15	.20	18.3	5.5
UK	~14.9		.8 (300°)	.0077	.028	.15	.21	21.7	8.9
UNC	15.8				.032	.14	.16		
Median Value	16.2	-3.64	1.00	.0052	.030	.14	.21	17.8	6.0
% Spread:									
Plus	12% AI	23% K	26% BNL	48% UK	7% BNL- UNC	7% UK	9% AI- B&W	22% UK	87% B&W
Minus	8% UK	24% B&W	47% AI	83% BNL	10% ANL	14% AI	24% UNC	35% BNL	69% BNL
BARE CORE NO. 5 11:1 Oxide with F. P.									
AI	1.16	2.97	3.14	.0201	.017	.14	.32	32.7	14.5
ANL	-2.10		4.44	.0146	.016	.16	.29	31.6	14.3
BNL	-.2		4.54	.0033	.018	.16	.24	21.2	4.5
B&W	neg								
GE	.37	2.85	2.62	.0173	.017	.15	.27	29.8	14.7
Karlsruhe	neg								
UK	neg		3.4 (300°)	.0222	.016	.16	.30	34.3	17.9
Median Value			3.4	.0173	.017	.16	.29	31.6	14.5
% Spread:									
Plus			34% BNL	28% UK	6% BNL		10% AI	9% UK	23% UK
Minus			23% GE	81% BNL	6% UK- ANL	12% AI	17% BNL	38% BNL	69% BNL

	$B^2 \times 10^4$	$\% \delta k$ for Removal of 50% Na		Doppler Effect ($-Tdk/dt$)	$\frac{\sigma_f U^{238}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c U^{238}}{\sigma_c Pu^{239}}$	$\frac{\sigma_c Pu^{239}}{\sigma_f Pu^{239}}$	$\frac{\sigma_f Pu^{240}}{\sigma_f Pu^{239}}$	$\% \text{ Fissions Below:}$	
		Total	Spectral + Capture						25 keV	2.1 keV
BARE CORE NO. 6 5:1 Oxide with F. P.										
AI	17.4	-3.40	.84	.0056	.029	.12	.22	19.0	5.45	
ANL	14.8	-2.84	1.41	.0040	.026	.14	.20	18.8	6.15	
APDA	15.9	-2.99	1.34	.0045	.031	.14	.19	16.3	5.49	
BNL	16.1	-2.90	1.42	.0007	.031	.13	.17	11.5	1.69	
B&W	17.3	-3.59		.0059	.029	.14	.23	17.2	10.31	
GE	16.1	-3.90	.82	.0040	.028	.13	.18	16.7	5.7	
Karlsruhe	~14.3	-2.1 (900°)	~1.5 (900°)	.0044	.029	.15	.20	17.9	5.1	
UK	~14.0		1.4 (300°)	.0057	.027	.15	.21	20.9	7.9	
UNC	15.1				.032	.14	.15			
Median Value	15.9	-2.99	1.4	.0044	.029	.14	.20	17.6	5.6	
% Spread:										
Plus	9% AI	30% K	7% K	34% B&W	10% UNC	7% UK	15% B&W	19% UK	84% B&W	
Minus	12% UK	30% GE	41% GE	84% BNL	10% ANL	14% AI	25% UNC	35% BNL	70% BNL	
BARE CORE NO. 7 11:1 Oxide with F. P. and Pu ²⁴⁰										
AI	1.42	3.43	3.45	.0065	.017	.14	.31	.13	29.4	12.1
ANL	-1.86		4.97	.0103	.016	.16	.28	.15	28.0	11.8
BNL	.40	4.64	4.49	.0028	.019	.16	.23	.14	18.8	3.8
B&W	neg									
GE	.17	3.66	3.38	.0142	.017	.14	.26	.12	26.4	12.0
Karlsruhe	neg									
UK	neg			.0167	.017	.16	.29	.14	30.4	14.9
Median Value			4.0	.0103	.017	.16	.28	.14	28.0	12.0
% Spread:										
Plus			24% ANL	62% UK	12% BNL		11% AI	7% ANL	9% UK	24% UK
Minus			15% GE	73% BNL	6% ANL	12% AI-GE	18% BNL	14% GE	33% BNL	68% BNL
BARE CORE NO. 8 9:1 Oxide with F. P. and Pu ²⁴⁰										
AI	5.88	.98	2.66	.0131	.020	.13	.28	.15	26.7	10.5
ANL	2.46	3.49	4.02	.0079	.019	.15	.26	.17	24.6	9.6
BNL	4.50	2.31	3.58	.0020	.022	.15	.22	.16	16.2	3.0
B&W	3.54	1.53		.0110	.021	.15	.28	.15	22.6	15.0
France	~2.5	3.1 (900°)	3.35 (900°)	.0095	.021	.16	.26	.16	23.6	8.0
GE	4.15	1.54	2.82	.0100	.020	.14	.23	.14	22.9	9.5
Karlsruhe	1.3	3.3 (900°)	~3.8 (900°)	.0096	.020	.17	.25	.15	23.0	7.8
UK	~1.0		~4.0	.0120	.019	.16	.26	.16	26.8	12.1
UNC	2.74	2.50		.0060	.022	.16	.20	.16		
Median Value	2.73	2.4	3.58	.0096	.0205	.15	.26	.16	23.3	9.5
% Spread:										
Plus	115% AI	45% ANL	12% ANL	27% AI	7% BNL-UNC	7% UK-UNC	8% AI-B&W	6% ANL	15% UK	58% B&W
Minus	63% UK	59% AI	26% AI	79% ANL	7% ANL-UK	13% AI	23% UNC	12% GE	30% BNL	68% BNL

	$B^2 \times 10^4$	% δk for Removal of 50% Na		Doppler Effect ($-Tdk/dt$)	$\frac{\sigma_f U^{238}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c U^{238}}{\sigma_c Pu^{239}}$	$\frac{\sigma_c Pu^{239}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c Pu^{239}}{\sigma_f Pu^{239}}$	% Fissions Below:	
		Total	Spectral + Capture						25 keV	2.1 keV
BARE CORE NO. 9 7:1 Oxide with F. P. and Pu ²⁴⁰										
AI	11.56	-1.08	1.88	.0088	.024	.12	.25	.18	27.3	7.39
ANL	8.27	.82	3.07	.0055	.022	.15	.23	.19	20.4	7.10
BNL	10.30	-.17	2.61	.0012	.026	.14	.19	.19	11.9	1.70
B&W	9.78	-.56		.0073	.025	.15	.25	.17	18.6	11.6
GE	9.55	-.66	2.19	.0063	.024	.14	.21	.16	18.7	6.8
Karlsruhe	~7.1	1.0 (900°)	~3.0 (900°)	.0062	.024	.16	.22	.17	19.2	5.7
UK	~6.9		~3.1	.0074	.023	.15	.23	.18	22.5	9.0
Median Value	9.55	-.36	2.8	.0063	.024	.14	.23	.18	19.2	7.10
% Spread: Plus	21% AI	K	11% UK	40% AI	8% BNL		9% AI-B&W	6% ANL-BNL	42% AI	63% B&W
Minus	28% UK	AI	33% AI	81% BNL	8% ANL	14% AI	17% BNL	11% GE	38% BNL	76% BNL
BARE CORE NO. 10 5:1 Oxide with F. P. and Pu ²⁴⁰										
AI	19.9	-2.89	1.20	.0046	.031	.12	.22	.21	16.7	4.39
ANL	16.9	-1.99	2.07	.0027	.028	.13	.19	.23	15.2	4.38
APDA	18.2	-2.29	1.87	.0032	.033	.13	.18	.21	13.1	3.92
BNL	18.8	-2.47	1.68	.0005	.033	.12	.16	.22	9.3	1.22
B&W	19.3	-2.66		.0039	.031	.14	.21	.20	13.9	7.76
GE	17.7	-2.94	1.48	.0033	.030	.13	.18	.20	13.7	4.0
Karlsruhe	15.6	-1.4 (900°)	~1.9 (900°)	.0033	.030	.15	.19	.21	14.6	3.6
UK	~16.0		~2.2	.0034	.028	.14	.20	.21	17.1	5.7
UNC	18.4	-1.55			.033	.14	.14	.22		
Westing-house	17.6	-2.17		.0026	.032	.13	.19	.21	13.8	2.6
Median Value	18.0	-2.29	1.87	.0033	.031	.13	.19	.21	13.9	4.0
% Spread: Plus	11% AI	39% K	18% UK	28% AI	6% APDA	8% B&W-UK	16% AI	10% ANL	23% UK	94% B&W
Minus	13% K	28% GE	36% AI	85% BNL	10% ANL-UK	8% AI-BNL	26% UNC	5% B&W-GE	33% BNL	70% BNL
BARE CORE NO. 11 11:1 Carbide with F. P. and Pu ²⁴⁰										
AI	4.73	1.42	2.64	.0131	.020	.12	.27	.16	24.0	9.00
ANL	1.58	~4.0	3.96	.0082	.019	.15	.24	.18	21.7	8.25
BNL	3.5	2.73	3.73	.0019	.022	.14	.20	.17	14.2	2.3
B&W	1.73	2.01		.0115	.020	.15	.28	.15	21.8	14.4
GE	2.80	2.23	3.00	.0111	.020	.14	.23	.15	21.9	9.0
Karlsruhe	0.6	4.0 (900°)	~4.2 (900°)	.0091	.020	.16	.23	.17	19.8	6.3
UK	~0		3.7 (300°)	.0122	.019	.15	.24	.16	22.0	9.5
UNC	1.19	3.50			.021	.15	.19	.16		
Median Value	1.65	2.73	3.7	.0111	.020	.15	.24	.16	21.8	9.0
% Spread: Plus	AI	46% K	13% K	18% AI	10% BNL		17% B&W	12% ANL	10% AI	60% B&W
Minus	K-UK	48% AI	29% AI	82% BNL	5% UK-ANL	20% AI	21% UNC	6% B&W-GE	35% BNL	74% BNL

	$B^2 \times 10^4$	% δk for Removal of 50% Na		Doppler Effect ($\sim Tdk/dt$)	$\frac{\sigma_f U^{238}}{\sigma_f Pu^{239}}$	$\frac{\sigma_c U^{238}}{\sigma_c Pu^{239}}$	$\frac{\sigma_c Pu^{239}}{\sigma_f Pu^{239}}$	$\frac{\sigma_f U^{238}}{\sigma_f Pu^{239}}$	% Fissions Below:	
		Total	Spectral + Capture						25 keV	2.1 keV
BARE CORE NO. 12 9:1 Carbide with F. P. and Pu ²⁴⁰										
AI	9.38	-.11	2.02	.0097	.023	.12	.25	.18	21.0	7.3
ANL	6.22	1.56	3.13	.0063	.021	.14	.22	.20	18.7	6.5
BNL	8.1	.51	2.89	.0014	.025	.13	.19	.19	12.1	1.8
B&W	6.46	.19		.0083	.023	.14	.25	.17	19.0	11.9
France	~6.2	1.4 (900°)	1.98 (900°)	.0070	.024	.15	.22	.19	15.6	5.0
GE	7.27	.19	2.43	.0078	.023	.14	.21	.17	18.8	7.0
K	~5.3	1.8 (900°)	~3.4 (900°)	.0066	.023	.15	.21	.19	17.2	4.9
UK	~4.7		~3.4	.0086	.021	.15	.22	.18	18.9	7.4
UNC	6.03	1.40		.0040	.025	.14	.17	.18		
Westing-house	7.58	.38		.0054	.024	.13	.23	.18	17.7	4.0
Median Value	6.34	.51	2.89	.0068	.023	.14	.22	.18	18.7	6.5
% Spread: Plus	48% AI	K	18% K-UK	43% AI	9% BNL-UNC	7% France	14% AI-B&W	11% ANL	12% AI	83% B&W
Minus	26% UK	AI	31% France	79% BNL	9% ANL-UK	14% AI	23% UNC	5% B&W-GE	35% BNL	72% BNL
BARE CORE NO. 13 7:1 Carbide with F. P. and Pu ²⁴⁰										
AI	15.75	-1.67	1.38	.0064	.027	.11	.22	.20	17.0	5.10
ANL	12.48	-.86	2.32	.0039	.026	.13	.19	.23	15.2	4.65
BNL	14.40	-1.68	2.05	.0008	.030	.12	.17	.22	9.7	1.23
B&W	14.19	-1.63		.0060	.028	.14	.23	.19	15.4	8.94
GE	13.30	-1.86	1.82	.0048	.027	.13	.19	.20	15.2	4.90
Karlsruhe	11.70	-4.5 (900°)	~2.3 (900°)	.0042	.027	.14	.19	.22	14.2	3.50
UK				.0013	.021	.14	.18	.19	12.4	
WEST	13.40	-1.52		.0035					14.2	2.80
Median Value	13.4	-1.67	2.05	.0040	.027	.13	.19	.20	14.7	4.65
% Spread: Plus	17% AI	48% ANL	13% ANL	60% AI	11% BNL	8% B&W-UK	21% B&W	15% ANL	16% AI	92% B&W
Minus	13% K	K	33% AI	80% BNL	22% UK	8% AI	10% BNL	5% B&W-UK	34% BNL	73% BNL
BARE CORE NO. 14 5:1 Carbide with F. P. and Pu ²⁴⁰										
AI	25.5	-3.20	.77	.0034	.034	.11	.19	.24	11.8	2.92
ANL	21.9	-3.29	1.44	.0022	.032	.12	.16	.26	11.1	2.77
BNL	23.8	-3.76	1.23	.0004	.037	.11	.14	.26	6.8	.70
B&W	25.3	-3.49		.0028	.036	.13	.19	.23	11.4	5.22
GE	22.7	-3.99	1.15	.0023	.034	.12	.16	.23	11.0	2.8
Karlsruhe	21.3	-2.7 (900°)	~1.6 (900°)	.0017	.034	.13	.16	.25	10.6	2.1
UK	21.6		~1.7	.0024	.032	.13	.16	.24	11.2	3.2
UNC	24.5	-2.15			.036	.12	.12	.25		
Median Value	23.2	-3.29	1.34	.0023	.034	.12	.16	.24	11.1	2.8
% Spread: Plus	10% AI	35% UNC	27% UK	48% AI	9% BNL	8% UK-B&W	AI-B&W	ANL-BNL	3% AI	B&W
Minus	8% K	21% GE	43% AI	83% BNL	6% ANL	8% AI	25% UNC		39% BNL	BNL

	$B^2 \times 10^4$	$\% \delta k$ for Removal of 50% Na		Dopper Effect ($-Tdk/dt$)	$\frac{\sigma_f \text{Pu}^{233}}{\sigma_f \text{Pu}^{239}}$	$\frac{\sigma_c \text{U}^{238}}{\sigma_c \text{Pu}^{239}}$	$\frac{\sigma_f \text{Pu}^{240}}{\sigma_f \text{Pu}^{239}}$	$\frac{\sigma_f \text{Pu}^{239}}{\sigma_f \text{Pu}^{239}}$	$\% \text{ Fissions Below:}$	
		Total	Spectral + Capture						25 keV	2.1 keV
BARE CORE NO. 15 9:1 Metal with F. P. and Pu ²⁴⁰										
ANL	13.0	-1.02	2.88	.0019	.026	.11	.14	.25	6.32	.94
BNL	14.7	-1.93	2.00	.0002	.029	.11	.13	.24	3.57	.16
B&W	13.1	-.94		.0021	.028	.13	.18	.20	7.90	3.08
France	13.4	-1.1 (900°)	2.42 (900°)	.0022	.028	.12	.15	.24	5.80	.46
GE	13.1	-1.18	2.16	.0023	.027	.12	.13	.21	6.20	.90
Karlsruhe	12.4	-0.5 (900°)	~2.7 (900°)	.0017	.027	.12	.14	.24	6.00	.70
UK	12.1		2.9 (300°)	.0031	.025	.12	.14	.22	6.60	1.20
Median Value	13.1	-1.06	2.56	.0021	.027	.12	.14	.23	6.20	.90
% Spread:										
Plus	12% BNL	53% K	13% UK	48% UK	7% BNL	8% B&W	28% B&W	9% ANL	27% B&W	B&W
Minus	8% UK	82% BNL	22% BNL	90% BNL	7% UK	8% ANL-BNL	7% BNL-GE	13% B&W	42% BNL	82% BNL
BARE CORE NO. 16 Steam-Cooled, 11:1 Oxide										
B&W	neg									
GE	neg	3.14	2.64	.0193	.017	.12	.49	.11	51.9	42.6
Karlsruhe	neg									
UK	neg			.0179	.017	.12	.46	.12	51.4	42.4
BARE CORE NO. 17 Steam-Cooled, 9:1 Oixde										
B&W	neg									
GE	3.00	2.14	2.11	.0165	.020	.13	.45	.12	49.2	39.4
Karlsruhe	neg									
UK	neg			.0165	.019	.13	.43	.13	48.4	39.0
BARE CORE NO. 18 Steam-Cooled 7:1 Oxide										
B&W	5.88	3.88		.0145	.025	.13	.39	.15	42.2	36.4
GE	8.05	.57	1.05	.0131	.024	.13	.41	.15	45.3	35.0
Karlsruhe	4.05	3.36 (900°)	~3.8 (900°)	.0191	.025	.15	.42	.15	44.5	34.6
UK	~4.5	~2.5 (300°)		.0140	.023	.13	.40	.16	44.5	34.6
Median Value	5.2	2.93		.0143	.024		.405		44.5	34.8
% Spread:										
Plus	55% GE	32% B&W		34% K	4% B&W		4% K		2% GE	5% B&W
Minus	19% K	84% GE		8% GE	4% UK		4% B&W		5% B&W	0% K-UK
BARE CORE NO. 19 Steam-Cooled 5:1 Oxide										
B&W	15.0	1.31		.0108	.031	.14	.35	.18	37.0	31.0
GE	15.4	-1.05		.0090	.029	.13	.37	.18	39.9	29.2
Karlsruhe	11.6	1.2 (900°)	~2.2 (900°)	.0145	.030	.15	.37	.18	39.3	29.1
UK	12.2	~1.0 (300°)		.0102	.028	.14	.36	.19	38.9	28.7
UNC	13.3	2.17			.033	.15	.27	.20		
Median Value	13.3	1.2		.0105	.030	.14	.36	.185	39.1	29.2
% Spread:										
Plus	16% GE	UNC		38% K	7% UNC	7% UNC	3% GE-K	8% UNC	2% GE	6% B&W
Minus	8% K-UK	GE		14% GE	10% UK	7% GE	25% UNC		5% B&W	2% UK

	Critical Mass, kg.	Breeding Ratio		Doppler Effect ($-Tdk/dt$)	% δk for Removal of 50% Core Na	
		Total	Core		Total	Spectral + Capture
BLANKETED CORE NO. 8 9:1 Oxide						
AI	1591	1.37	0.95	.0129	1.48	2.65
ANL	7090	1.43	1.26	.0084	3.46	4.02
BNL	2376	1.57		.0014	2.74	
France (900°)	6546	1.51	1.32	.0097	3.26	3.32
B&W	3773	1.45		.0112	1.81	
GE	2870	1.51		.0096	2.01	
Karlsruhe (900°)	15283	1.53	1.36	.0103	3.37	~3.8
UK (300°)	13320	~1.4		.0120	2.97	3.40
UNC	6321	1.49		.0080	2.83	
Russian	4710	1.51	1.25	.0112	2.53	~3.4
BLANKETED CORE NO. 10 5:1 Oxide						
APDA	323	1.64	0.64	.0036	0.10	2.0
ANL	391	1.52	0.70	.0029	0.40	2.13
BNL	304	1.62		.0001	0.07	
B&W	318	1.55		.0040	-0.36	
GE	347	1.57		.0031	-0.28	
Karlsruhe (900°)	449	1.60	0.72	.0036	0.41	
UK (300°)	429	~1.45		.0035	0.14	1.95
Westinghouse	343	1.56	0.60	.0029	-0.17	
BLANKETED CORE NO. 12 9:1 Carbide						
AI	866	1.47	.86	.0098	.60	2.04
ANL	1817	1.62	1.14	.0065	2.02	3.11
BNL	1116	1.69		.0010	1.56	
B&W	1782	1.51		.0083	.75	
France (900°)	1884	1.68	1.22	.0074	1.82	3.16
GE	1376	1.62		.0077	1.15	
Karlsruhe (900°)	2370	1.65	1.21	.0072	2.28	~3.5
UK (300°)	2504	~1.6		.0086	2.03	3.05
UNC	2081	1.59		.0050	1.83	
Russian	1662	1.63	1.13	.0082	1.55	~3.0
Westinghouse	1188	1.57	1.01	.0056	1.28	
BLANKETED CORE NO. 15 5:1 Metal						
ANL	605	1.77	.98	.0019	1.18	2.92
BNL	469	1.86		.0002	.33	
B&W	674	1.82		.0022	.51	
France	605	1.98	1.03	.0023	.60	2.36
GE	665	1.84		.0022	.42	
Karlsruhe	676	1.88	.98	.0017	1.19	~2.1
UK (300°)	712	~1.9		.0031	1.24	2.91
BLANKETED CORE NO. 19 STEAM COOLED, 5:1 Oxide						
B&W	463	1.23		.0109	1.89	
GE	406	1.21		.0086	-.31	
Karlsruhe (900°)	654	1.23	.66	.0141	1.59	
UK (300°)	558	1.18	.58	.0082		

TWO-DIMENSIONAL CARBIDE CYLINDER

Core Diameter: 10 ft
Core Height: 2 ft
Approx. Ratio $U^{238}:Pu^{239} \approx 7$

	Critical Mass, kg. of Pu^{239}	Breeding Ratio	% δk for Removal of 50% Na	
			Total	Spectral Component
UK	1979			
GE	1802	1.48	1.02 (1100°)	2.11
B&W	1884	1.42	0.53 (1100°)	
Karlsruhe	2030	1.34	1.42 (900°)	
LASL CRAM-2Dcyl	1957		0.8 (300°)	
DDF-2Dcyl	1943	1.53	0.9 (300°)	
DTF-1Dslab	1936		0.8 (300°)	
BNL	1830		1.32	2.00
ANL	1960		1.34 (1100°)

FIG. 1. Buckling for Oxide Reactors

FIG. 2. Buckling for Carbide Reactors

FIG 3 Adjunct Flux for 9:1 Oxide without Fission Products or Pu²⁴⁰

FIG. 4 Adjoint Flux for 9:1 Oxide with Fission Products and Pu^{240}

FIG. 5. Adjoint Flux for 5:1 Oxide with Fission Products and Pu^{240}

Discussion

Mr. Chernick (BNL): I thought the slides Dr. Okrent showed were very instructive. I've just one brief bit of information to add. It is very clear that people are generally calculating these things accurately except for one or two exceptions which are noted, that is, the machines are behaving themselves these days. The other thing I would like to mention is the fact that it was clear that the Doppler-effect calculations which were made at Brookhaven were quite low, and I'd like to explain this very briefly. We've been interested primarily in very hard spectra at Brookhaven. Therefore we've stuck to the original YOM cross sections. As is well-known, these give a poor picture of the low-energy spectra from which most of the Doppler effect comes. What we saw on the slides bears this out.

Mr. Greebler (GE): I'd like to comment on a couple of inconsistencies that I noted. In the case of the sodium void coefficients, the GE value was consistently less positive than that of, for example, the Karlsruhe result. Similarly, the GE result for the steam-cooled void effect was less positive than that of B and W. Yet,

if we recall the recent Detroit technology meeting, the Karlsruhe people reported a near-zero sodium void effect with a reactor that was 3 feet high and with a composition similar to that which we had; we had to go to something like 2 feet or less to get a near-zero sodium void effect. We find a similar comparison with regard to the B and W steam-cooled reactors. I wondered what is the basis of the inconsistency? Are the same people that are calculating these results the ones who are doing the detailed design calculations?

Mr. Okrent: I cannot comment to your question. I have given the names of people signed to the submitted results. Whether they are separate from the design people, one doesn't know.

Mr. Küsters (Karlsruhe): I would like to hear you comment on the high critical masses of the Karlsruhe group. We are using the low plutonium cross sections measured by White (I suppose the UK people do this too), and therefore we got an appreciably high critical mass. The plutonium values of White are about 20% lower in the range from 10 kV to 100 kV, and therefore we got so high critical masses.

Mr. Okrent: This was one of the things that I had in mind when I said, were Argonne to make a change in its cross sections we would become less reactive. I think that the other change that we would have in mind would involve raising some of our high-energy U-238 capture cross sections; again this would make us less reactive, and we would move in your direction for values of the critical mass.

Mr. Perry (ORNL): Not all of the differences noted, of course, are equally important. The large variations in buckling in the neighborhood of $k_{\infty} = 1$, for example, are not necessarily very important. I'd like to ask Dr. Okrent which of the variations he noted seem in his opinion to be most decisive for design of large reactors.

Mr. Okrent: I would think that the large differences in the sodium void effects require the most attention. Inevitably we can find out what the critical mass is by building critical experiments. One should not, however, minimize the buckling differences that were just mentioned. Since interest in large part will eventually be on reactors of large size and many people have been trying to design such reactors to have internal breeding ratios in the vicinity of one (in other words, zero long-term reactivity effects), it's going to be important to know just what it takes to design such a reactor. Assume that you want a reactor having a given volume so that it puts out a certain power. If it turns out that this reactor needs a 7 to 1 enrichment to go critical instead of 9 to 1, this will leave quite a bit of long-term reactivity loss to be handled. So I don't think one can be too carefree about this buckling difference.

Mr. Küsters: I would like to give the answer to the question of Dr. Greebler. The results presented at the Detroit meeting and Chicago were calculated with the Russian cross-section set. The results presented here were obtained with the new Karlsruhe set. The people are the same.

Mr. Okrent: I might note that in the comparison calculations from Karlsruhe they did give results on some reactors with the Russian set and with their new set. The Russian set is certainly much more reactive.

Mr. Wolfe (GE): Isn't a large part of the differences shown, especially in the low-enriched reactors, due to the way in which the comparisons were made; that is, when the bucklings came out wrong or the reactivities came out different, the various groups calculated different sized reactors so that any spectral differences, in particular the sodium void effects, would be accentuated by this? I think that if you take the case where people calculate the same size reactors, there'd be an

enrichment change, but the various parameters might be much closer together.

Mr. Okrent: I may be wrong, but I would expect the spectral component of the sodium void effect to be relatively insensitive to this; that is why it was included, as well as the total sodium void effect, which is influenced by neutron leakage.

Mr. Greebler: Somewhere midway in the course of the calculations you suggested to us a heterogeneity calculation which would have somewhat overestimated the degree of heterogeneity. Did all of the participants

tion of the fact that there is more flux at the very very low energy, say 10 keV and below.

Mr. Okrent: In defense of my colleagues, I should note that the YOM set ended in the low-energy region where the Doppler effect pretty much begins. The set was intended to analyze the existing criticals in 1960, none of which had neutrons at such low energies.

Mr. Fenech (MIT): The errors that we have seen on the slides reflect overall errors which are due to several causes: some are due to cross sections and some are due to the matters used to establish these numbers, such as the number of groups and transport diffusion theory or S_8 , S_4 , etc. Does Dr. Okrent have an idea as to the order of magnitude due to the input into the methods or due to the methods themselves, and whether emphasis should be on the establishment of improving the cross section or on the methods which are used to determine these numbers?

Mr. Okrent: I would assume that the primary difference lies in the cross sections used. Certainly differences in methods of calculations have an effect, but I think similar methods were used for the most part. It appears that the cross-section input is the primary culprit.

Calculated Physics Parameters and Their Uncertainties in a 1000-MW(e) Fast Ceramic Reactor

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1.0 Introduction

A design study of a 1000-MW(e) fast oxide reactor was conducted to take into account new information in fast reactor technology developed since the last 1000-MW(e) study in late 1963.⁽¹⁾ In the physics area there have been several new cross-section measurements and new sets of group constants,⁽²⁻⁵⁾ dynamics calculations that more clearly defined the role of the Doppler and sodium void reactivity effects in reactor safety and stability,⁽⁶⁾ and results of scoping studies⁽⁷⁾ and other fast reactor design studies^(8, 9) that offered alternative approaches toward achieving a safe and economical fast power reactor. Lowering of fuel density and of the allowable peak fuel temperature as a result of irradiation and transient fuel test experience⁽¹⁰⁾ and reduction of clad thickness with vented fuel are new material technology factors that affect the physics properties; and they contributed to the need for updating the earlier 1000-MW(e) design.

1.1 Differences in New 1000-MW(e) Design

The 2-ft-thick core containing BeO additive of the earlier 1000-MW(e) pancake reactor had a high neutron leakage and a degraded spectrum in order to satisfy

* Work performed while on assignment with GE-APO from AEC Chicago Operations Office.

stringent safety requirements involving the sodium void and Doppler effects. Scoping and design studies on alternative core configurations showed that other core shapes, such as an annular^(7, 11) or modular,⁽¹²⁾ could achieve as good or slightly better sodium void effects than pancakes for the same core composition and core neutron leakage. These alternative configurations, however, showed important disadvantages compared with the pancake in the thermal-hydraulics area⁽¹³⁾ and in other aspects of reactor safety such as control requirements. Hence, the pancake-shaped cylindrical core is still the favored design for meeting stringent safety requirements with respect to sodium voiding. More recent considerations on design safety criteria are discussed in reference 14.

One of the designs studied in detail is even thinner than the earlier design, the core thickness having been reduced from 24 to 16 in. In this design, the sodium volume fraction was reduced from 0.46 to 0.36 and the fuel volume fraction increased from an average of 0.25 to 0.38. Because of the lower sodium content the core had to be made thinner to make the neutron leakage high enough to produce a zero (slightly negative) reactivity for total sodium loss. This change in core composition and thickness slightly improved the neutron economy without penalizing the safety reactivity coefficients.† BeO is used, as in the earlier design, to provide an adequate negative Doppler coefficient. Different BeO contents in two core zones and a BeO radial reflector are used in the new design to achieve a suitably flat power distribution. Tantalum is used for control rod material instead of boron carbide as in the earlier design.

Statics physics parameters of this thinner 1000-

† The improvement in fuel cost is not manifest in a direct comparison of the calculated breeding ratio and fissile inventory of the design (Table 1) with those reported for the earlier design.⁽¹⁾ This is because changes in cross sections used, in Pu isotopic content for the selected mode of recycle, and in fuel density and maximum operating temperature since the earlier design study have all been in the direction of reducing the neutron economy.

MW(e) design are discussed in Section 2. Discussion of safety criteria upon which the physics design is based and a more complete engineering design description of the reactor will be found in reference 14.

1.2 Evaluation of Uncertainties

Current physics predictions are highly unreliable because of the unsatisfactory status of fast reactor cross-section information and calculation methods. This is evident in the large differences in reactor physics results calculated by different organizations. For example, Karlsruhe and Cadarache report 1000-MW(e) designs that have 3- to 5-ft core thicknesses with internal breeding ratios in excess of 0.9, nearly zero reactivity for total loss of sodium, and Doppler coefficients near -0.01 (Tdk/dT) without adding any moderating material.⁽⁹⁾ General Electric, on the other hand, using cross-section data very nearly that in ANL Set 224,⁽²⁾ calculates that such Doppler and sodium void effects requires a core thickness below 2 ft with an internal breeding ratio less than 0.7 and BeO additive.⁽¹⁾ Examination of measured basic cross-section data shows large deviations for many of the material cross sections important in large fast ceramic reactors. Two examples are (1) the U-238 energy point-wise capture cross section has a 25% standard deviation over most of the energy region between 4 and 100 keV⁽¹⁵⁾ and (2) recent measurements of the Pu-239 fission cross section in the U.K. indicate that it is about 30% lower than that given by BNL-325 in the energy interval from 24 to 100 keV.⁽¹⁶⁾

Calculations were made to evaluate the effects of several cross-section and computation model uncer-

tainties on the physics parameters. The reactor model used for this evaluation was an infinite slab approximation of the 1000-MW(e) pancake design. Perturbations in calculated Doppler and sodium void reactivity effects, breeding ratios, fissile "enrichment," and neutron-spectrum indices were obtained for individual changes in important material cross sections and composites of such changes. These cross-section changes represented the estimated range of uncertainty as indicated by differences in basic point-wise cross-section data and in standard sets of group constants.⁽²⁻⁵⁾ This evaluation of uncertainties in the calculated physics parameters is described in Section 3.

2.0 Physics Parameters of the 1000-MW(e) Design

Figure 1 shows the core and blanket configuration of the 1000 MW(e) design, and the volumetric composition of each region. A very thin (16-in.) two-zone core is used with different amounts of BeO and fuel in the two regions and a thin BeO reflector around the outer core region. There is no radial blanket, resulting in a small loss of breeding ratio (~ 0.03) but an improvement in fuel cost. The fissile plutonium "enrichment" of the fresh fuel is the same for the two core zones. Each axial blanket volumetric composition is the same as that of the adjacent core zone.

2.1 Power Distribution

The core blanket-reflector configuration of Figure 1 gives a very flat power distribution. The radial peak-to-average fuel specific power (kW/kg fuel) shown by the white curve in Figure 2, is 1.22 and occurs at the center of the core. The axial peak-to-average is only

FIG. 1. Core and Blanket Arrangement and Composition

FIG. 2. Core Radial Power Distribution

FIG. 3. Comparisons of Power-flattening Methods

1.11 because of the thin core. The radial peak core power density (kW/liter) occurs in zone 2 at the interface of the two zones (see black curve in zone 2 region of Figure 2). This is because zone 2 has a higher fuel volume fraction than zone 1. A power "spike" occurs near the BeO reflector; however, the power density there is well below the peak value at the center of the core or that at the interface between zones 1 and 2. It was found that the flat radial power distribution of Figure 2 could be maintained throughout the operating cycle between refuelings by using the shim control rod pattern to contribute to the power flattening at the beginning of the cycle.

Other methods of power flattening were studied. It was found that use of three core zones did not give enough improvement over the power distribution obtainable with two zones to warrant the additional design

and refueling complexity. In Figure 3 radial power distributions for several of the two-zone approaches that were considered in preliminary scoping studies are compared. With variable fissile "enrichment" in two zones and a BeO reflector, a power distribution can be obtained that is about as satisfactory as that with variable content of BeO and fuel in the two zones and a BeO reflector. Lack of a moderating reflector gives a very sharp power gradient at the outer edge of the core that is about as unsatisfactory as the power "spike" with the BeO reflector, and the overall power distribution profile is less satisfactory, as seen in Figure 3.

2.2 Reactivity Coefficients

Table 1 lists important physics parameters of the 1000-MW(e) design. The calculated Doppler coefficient

of $-0.0072 Tdk/dT$ (-0.006 with sodium completely removed) and the negative reactivity for total loss of sodium satisfy safety criteria that are discussed in reference 14. Zone 1 has more BeO and, hence, a softer spectrum than zone 2 (21% of fissions below 10 keV in zone 1; 13% in zone 2). Accordingly zone 1 had a higher local Doppler power coefficient than zone 2, over and above that due to differences in the power and statistical weight splits between the two zones. The portion of the axial reflector near the core was calculated to make a small ($\sim 5\%$) contribution to the Doppler coefficient, the fuel there having both a lower temperature rise and a lower statistical weighting than the average core fuel.

With the 2-zone variable BeO content, the Doppler coefficient and the neutron economy are sensitive to the power split between the two zones. Table 2 shows the Doppler coefficient and the breeding ratio for the design power split and for other power splits corresponding to abnormal conditions at power or transient overpower (such as having all control rods withdrawn in one zone compensated by an excess of inserted control rods in the other zone). It is seen from Table 2 that an ample Doppler coefficient is present even for postulated transients in which an unusually high fraction of the power occurs in zone 2.

2.3 Neutron Economy

The in-pile fissile inventory of 2470 kg and reactor power of 2500 MW(t) give a fuel specific power of 1010 kWt/kg fissile material. This specific power combined with the total breeding ratio of 1.18, and a load factor of 0.8 and an out-of-pile inventory that is 30% of the in-pile fissile inventory, results in a doubling

TABLE 1. PHYSICS PARAMETERS OF 1000-MW(e) DESIGN

Fissile Inventory at Midcycle* in Core and Blankets (kg)	2470
Core Fissile "Enrichment" (a/o)	15.3
Total Breeding Ratio	1.18
Core Conversion Ratio	0.63
Doubling Time (yr)*	13.4
Doppler Coefficient (Tdk/dT) Sodium In	-0.0072
Sodium Out	-0.006
Sodium Void Δk — Total	-0.0007
Core	+0.0037
Worst	+0.0056
β_{eff}	0.0035
Power Split (MWt)—Core Zone 1	1150
Core Zone 2	950
Blankets	400

* Equilibrium fuel cycle with 6-month operation between refuelings for partial reloading. Input plutonium is recycled plutonium discharged from core and blankets and mixed. Load factor is 0.8 and out-of-pile fissile inventory is 30% of the in-pile inventory.

TABLE 2. INFLUENCE OF POWER DISTRIBUTION ON DOPPLER COEFFICIENT AND BREEDING RATIO

Case	Power Split (%)		Doppler Coefficient [$T(dk/dT)$]			Breeding Ratio
	Zone 1	Zone 2	Total	Zone 1	Zone 2	
A	55	45	-0.0072	-0.0051	-0.0021	1.18
B	64.5	35.5	-0.0083	-0.0072	-0.0011	1.19
C	51.3	48.7	-0.0069	-0.0043	-0.0026	1.17
D	57.0	43.0	-0.0063	-0.0043	-0.0020	—

Case A: Design power split, approximately uniform shim rod density.

Case B: All zone 2 shim rods inserted; zone 1 shim rods withdrawn to maintain criticality.

Case C: All zone 2 shim rods withdrawn; zone 1 shim rods inserted to maintain criticality.

Case D: Sodium completely voided from zone 1 core and blanket.

TABLE 3. EVALUATION OF PHYSICS PARAMETER UNCERTAINTIES^(a)

Cross-section Deviations Contributing to Improved Doppler and Sodium Void Effects

Description ^(b)	Doppler $T \frac{dk}{dT}$	Total Sodium Void Δk	Breeding Ratio	Fissile Core a/o	% Fissions below 9.1 keV
BASE CASE	-0.0074	+0.0010	1.20	14.3	21.7
High Fuel Inelastic	-0.0076	-0.0002	1.19	14.5	22.2
High U-238 Capture	-0.0075	+0.0003	1.24	14.8	20.4
Low Pu-239 Fission (24 to 170 keV)	-0.0075	+0.0006	1.20	15.1	22.6
Low Steel Capture	-0.0078	+0.0007	1.22	14.0	22.4
Low Pu-240 Capture (below 10 keV)	-0.0075	+0.0001	1.20	14.2	22.1
Zero Pu-239 Doppler	-0.0108				
Good Coefficient, Composite	-0.0120	-0.0015	1.25	15.4	22.8

^(a) Infinite-slab reactor; 16-in.-thick core; 37.0 v/o PuO₂-UO₂, 20.5 v/o steel, 33.0 v/o sodium, 9.1 v/o BeO, and 0.4 v/o Ta. This is a slightly higher steel and lower sodium v/o than the two-zone core (see Figure 1). Blankets are 18 in. thick. Mid-burnup condition of refueling cycle with 10⁵ MWID/t burnup of discharged core fuel.

^(b) See Sections 3.1.1 through 3.1.6.

time of 13.4 yr. Reference 14 gives the breakdown of fuel cost.

3.0 Evaluation of Uncertainties

An infinite-slab (one-dimensional) representation of the 1000-MW(e) design was used in calculating the sensitivity of physics parameters to uncertainties in cross-section data and computation methods. Footnote (a) of Table 3 gives the reactor geometry and composition.

Deviations in cross-section data were obtained from the various standard sets of group constants and also from results of recent cross-section measurements that have not yet been incorporated in these standard sets. Group-constant sets considered were the ELMOE-corrected ANL 26-group set,⁽²⁾ the new U.K. Winfrith 33-group set, the U.S.S.R. 26-group set,⁽³⁾ the new YOM 16-group set,⁽⁵⁾ and the GE 60-group set. Recent cross-section experimental information included (1) U.K. results on the fission cross section of Pu-239 between 24 and 170 keV,⁽¹⁶⁾ based on the measured ratio of σ_f of Pu-239 to that of U-235, which leads to Pu-239 σ_f values as much as 30 % lower than those in BNL-325. (2) U.K. measurements of the Pu-239 total cross section by Uttley and fission cross section by James below 20 keV that, combined with theoretical evaluation of the scattering cross sections, give high alpha values for Pu-239 below 10 keV, and (3) measurement of the absolute ν value of U-235 at low energy by Colvin and Sowerby⁽¹⁷⁾ that indicates about 2 % lower ν values for all fissionable materials.

3.1 Basis for Cross-section Changes in Sensitivity Studies

3.1.1 Base Case

The G.E. 60-group set uses basic cross-section data similar to that in the ELMOE-corrected ANL 26-group set.⁽²⁾ The main differences are slightly higher U-238 capture cross sections between 0.1 and 1 MeV in the G.E. set and independent methods (believed to be similar) used to calculate temperature-dependent

self-shielded fuel cross sections. The physics parameters of the Base Case in Tables 3 and 4 were obtained by means of the data of the G.E. 60-group set which was condensed to 26 energy groups for the multigroup one-dimensional calculations. All physics parameters and their variations (see below) were computed by use of 26-group, one-dimensional diffusion theory.

3.1.2 Inelastic and Elastic Scattering

The new U.K. Winfrith 33-group set gave the highest inelastic scattering transfer cross sections for U-238 and plutonium isotopes, both in terms of total inelastic removal and energy loss by the inelastically scattered neutrons. The ANL 26-group and the new U.K. 33-group sets have the highest elastic scattering energy transfer cross sections for oxygen and other light elements in the high-energy region. The cross-section variation in Table 3 labeled "High Fuel Inelastic" was taken from the new U.K. 33-group set. The cross section variation in Table 4 labeled "Low Elastic" appears in those sets of group constants that probably did not take into account the new values using the ELMOE code;⁽¹⁸⁾ hence, this variation may be more an uncertainty in calculation methods than one due to cross-section discrepancies.

3.1.3 U-238 Capture

"Low U-238 Capture" in Table 4 and "High U-238 Capture" in Table 3 are the envelopes of lowest and of highest U-238 σ_c for all energies obtained from the standard sets of group constants mentioned in Section 3.0. The variations representing lowest U-238 capture cross sections were obtained from the ANL 26-group set between 67 keV and 825 keV, and from the U.K. Winfrith 33-group set between 12.4 eV and 5.5 keV. The variations for highest U-238 capture cross sections were obtained from the U.S.S.R. 26-group set between 1 keV and 100 keV.*

3.1.4 Plutonium Fission and Capture

Measurements of the Pu-239 total cross section by Uttley and of the fission cross section by James below 20 keV, combined with theoretical evaluation of the Pu-239 scattering cross section in this energy region, give alpha values between 1.0 and 1.2 over most of the energy region between 100 eV to 10 keV. These alpha values are roughly 50 % higher than those deduced from earlier activation measurements.⁽¹⁹⁾ The case in Table 4 labeled "High Pu-239 Alpha" used these high-alpha values in conjunction with James' fission cross sections below 20 keV.

"Low Pu-239 Fission" in Table 3 refers to measure-

TABLE 4. EVALUATION OF PHYSICS PARAMETER UNCERTAINTIES*

Cross-section Deviations Contributing to Worse Doppler and Sodium Void Effects

Description*	Doppler $T \frac{dk}{dT}$	Total Sodium Void Δk	Breeding Ratio	Fissile Core a/o	% Fissions below 9.1 keV
BASE CASE	-0.0074	+0.0010	1.20	14.3	21.7
Low Elastic	-0.0068	+0.0036	1.23	13.9	19.4
Low U-238 Capture	-0.0078	+0.0027	1.17	14.0	22.6
High Pu-239 Alpha (below 10 keV)	-0.0062	+0.0065	1.12	15.5	19.8
High Steel Cap- ture	-0.0073	+0.0007	1.19	14.4	21.4
High Γ_γ Sodium 2.8-keV Reso- nance	-0.0073	+0.0030	1.19	14.4	21.1
Low ν Values (2.82 for Pu-239 at thermal)	-0.0072	+0.0012	1.16	15.0	21.4
Poor Coefficient, Composite	-0.0052	+0.0124	1.06	15.7	17.7

* See Footnotes a and b of Table 3.

* Cross sections for other than the indicated variations are the same as the base G.E. 60-group set.

ments by White *et al.* at Aldermaston,⁽¹⁶⁾ which indicate significantly lower Pu-239 σ_f from 24 keV to 170 keV.

“Low Pu-240 Capture” in Table 3 is based on data from the new U.K. Winfrith 33-group set between 91 eV and 5.5 keV.

3.1.5 Steel and Sodium Capture

“High Γ_γ Sodium 2.8-keV Resonance” in Table 4 is for a Γ_γ of 1.0 eV assumed on the basis of systematics of nuclides. (The new Yiftah 16-group set⁽⁵⁾ actually uses a $g\Gamma_\gamma = 1.0$, which gives even higher capture.) The value $\Gamma_\gamma = 0.35$ is used for the “Base Case” on the assumption that the 2.8-keV resonance accounts entirely for the thermal capture cross section.

The lowest steel capture cross sections (mainly due to iron and nickel) were found in the new U.K. Winfrith 33-group set for most of the energy region above 2 keV. “Low Steel Capture” in Table 3 is for the envelope of lowest iron, nickel, and chromium σ_c at all energies below 10 MeV. For iron this was obtained from the U.K. Winfrith 33-group set above 3.3 keV, and for nickel from the U.K. Winfrith 33-group set between 2.0 keV and 150 keV. Low chromium values were taken from the G.E. 60-group set.

“High Steel Capture” in Table 4 is worse than the envelope of highest iron, nickel, and chromium σ_c at energies below 1 MeV obtained from the various standard cross-section sets. This is because the calculated resonance integrals of these materials using energy point-wise cross-section data are 50 to 100 per cent lower than reported resonance integral measurements.⁽²⁰⁾ There is concern that some capture resonances may be missing in the energy point-wise cross-section data. To take this possibility into account, 25% was added to the envelope of highest iron, nickel, and chromium σ_c values below 1 MeV. The envelope of highest σ_c for iron, nickel, and chromium was obtained primarily from the G.E. 60-group set with several specific energy groups from the U.K. Winfrith 33-group set included.

3.1.6 ν Values and Pu-239 Doppler

Measurement of an absolute ν value of 2.385 ± 0.015 for U-235 by Covin and Sowerby⁽¹⁷⁾ indicates the possibility of about 2% lower ν values for all fissionable materials. Using 1.182 as the ratio of ν for Pu-239 to that for U-235⁽¹⁷⁾ gives $\nu = 2.82$ for Pu-239 at low energies. This was used for the case in Table 4 labeled “Low ν Values” along with proportionately lower ν values for Pu-239 at higher energies.

It appears that the calculated positive Doppler coefficient for Pu-239 may be highly overestimated, using a Breit-Wigner single-level resonance model and a chi-squared fission width distribution with three de-

grees of freedom. This observation is based on limited experimental information^(21, 22) in which the errors in some cases may be greater than the desired measured quantity. A one-degree-of-freedom distribution, which is supported by available resonance data, gives a lower Doppler effect, but not enough lower to agree with the near zero (or even negative) values indicated by the experiments. The case in Table 3 labeled “Zero Pu-239 Doppler” discounts the positive Pu-239 Doppler contribution currently computed.

3.2 Results of Sensitivity Studies

3.2.1 Cross-section Deviations Favorable to Reactivity Coefficients

Table 3 shows calculated Doppler coefficients, total sodium void reactivity effects, total breeding ratios, core fissile “enrichments,” and spectra in terms of fissions below about 10 keV for the Base Case and for those cross-section changes that improved overall Doppler and sodium void effects. The bottom row of Table 3 gives the composite effect of combining all of these cross-section changes.

It is noted that the cross-section deviations contributing to improved coefficients generally softened the neutron spectrum, the only exception being the High U-238 Capture case. The High Fuel Inelastic case, corresponding to the U.K. Winfrith new 33-group inelastic scattering constants for U-238 and plutonium, gave a surprisingly small improvement of the coefficients and degradation of the spectrum ($\sim 5\%$). The Low Pu-239 Fission case, corresponding to lower measured σ_f values between 24 and 170 keV,⁽¹⁶⁾ produced very little improvement in the coefficients and significantly increased the required fissile content. (Including this case in the Good Coefficient Composite perhaps imposes an unfair penalty on the combination of cross-section changes that improve the coefficients.) The most significant improvement in the Doppler effect results from neglecting the positive Pu-239 contribution. As discussed in Section 3.1.6, there is experimental evidence that supports a near-zero Pu-239 Doppler effect in a degraded spectrum similar to the calculated Base Case.

3.2.2 Cross-section Deviations Unfavorable to Reactivity Coefficients

Table 4 shows the calculated physics parameters, similar to Table 3 but for cross-section changes that worsen overall Doppler and sodium void effects. With a single exception (Low U-238 Capture) these changes tend toward a harder neutron spectrum. The most significant of these variations is that of High Pu-239 Alpha below 10 keV (which also includes slightly lower

σ_f values in this energy region) deduced from the Uttley σ_t and James σ_f Pu-239 measurements. This potential deviation in cross-section data is harmful not only to the coefficients but also to neutron economy. Therefore, it would not be attractive to overcome it with a variation in core composition or geometry that traded improved coefficients for higher fuel cost, unless other variations in the cross-section data resulted in significant improvement of the economics.

The possibility of 2% lower ν values⁽¹⁷⁾ produces mainly a decrease in neutron economy and imposes only a small penalty on the coefficients. The Low Elastic case, representing oxygen and other light-element elastic transfer cross sections that were not ELMOE-corrected, results in a significantly harder spectrum with poorer coefficients and better neutron economy. The deviations in energy-transfer group constants are probably least significant insofar as their long-term effect on fast reactor design is concerned because, when the values of these cross sections become well-established, the deviations from current values can generally be easily compensated by changes in reactor composition. Such adjustment is particularly easy when BeO or some other moderating additive has been included in the design for the purpose of degrading the spectrum and thereby improving the coefficients.

3.2.3 Cross-section Deviations that Produce Softer Spectra

Table 5 compared calculated physics parameters for Base Case, Good Coefficient, Composite taken from

Table 3, and Poor Coefficient, Composite taken from Table 4. A similar comparison is made for a reactor in which the BeO has been removed and replaced by a proportional amount of fuel and sodium. One other composite of deviations in cross-section data is also included in Table 5 for the BeO and non-BeO cores: the Soft Spectrum, Composite which is the same as same as that of Table 3, except that Low U-238 Capture replaced High U-238 Capture in calculating the composite effects; hence, Soft Spectrum, Composite represents the combination of cross-section variations that yield the most degraded neutron spectrum.

There is ample experimental evidence that the low-energy portion of the neutron spectrum is substantially underpredicted with current cross sections and calculation methods. This includes integral critical measurements, and their comparison with calculations, of neutron lifetime, boron-10 worth, and Doppler effect made in many facilities (see, for example, references 21, 22, 23, and 24). For the reactor with BeO, the Soft Spectrum, Composite has about 30% more fissions below 10 keV than the Base Case adjusted for uncorrected elastic removal (Low Elastic case of Table 4). For the reactor without BeO, the difference is slightly larger. The Low Elastic case has roughly the status of cross-section data that was used in some of the earlier calculations of integral experiments and which underpredicted lifetime and boron-10 worth by about 30% for highly dilute fast critical assemblies.

TABLE 5. EVALUATION OF PHYSICS PARAMETER UNCERTAINTIES
Comparisons of Composite Cross-section Deviations for Cores with and without BeO

Description	BeO, v/o	Doppler [-T(dk/dT)]		Total Sodium Void Δk	Breeding Ratio		Fissile Core, a/o	Fissions below 9 keV(%)	Doubling Time, yr ^(e)
		Na In	Na Out		Total	Core			
Base Case ^(a)	9	0.0074	0.0061	+0.0010	1.20	0.70	14.3	21.7	13.4
Poor Coefficient, Composite ^(b)	9	0.0056	0.0037	+0.0124	1.06	0.61	15.8	17.7	49.1
Good Coefficient, Composite ^(a)	9	0.0119	0.0098	-0.0015	1.25	0.71	15.5	22.8	11.6
Soft Spectrum, Composite ^(c)	9	0.0123	0.0101	+0.0007	1.18	0.70	14.4	25.0	15.0
Base Case ^(d)	0	0.0041	0.0029	+0.0086	1.39	0.76	12.7	10.6	6.1
Poor Coefficient, Composite ^(d)	0	0.0029	0.0018	+0.0191	1.25	0.67	13.5	8.5	10.1
Good Coefficient, Composite ^(d)	0	0.0059	0.0042	+0.0061	1.44	0.79	13.8	11.6	5.9
Soft Spectrum, Composite ^(d)	0	0.0061	0.0044	+0.0068	1.38	0.75	13.4	12.7	6.6
Zero Pu-239 Doppler ^(e)	3	0.0063	0.0049	0.0009	1.26	0.64	15.1	15.5	10.9
^(f)	3	0.0052	0.004	0.0062	1.15	0.56	17.7	13.9	22.1

^(a) Same as in Table 3.

^(b) Same as in Table 4.

^(c) Same as Good Coefficient, Composite, except Low U-238 Capture replaces High U-238 Capture (see Section 3.2.3).

^(d) Same geometry as BeO case. Volume fractions of fuel, sodium, steel and Ta are 0.429, 0.362, 0.205, and 0.004, respectively.

^(e) Volume fractions of fuel, sodium, steel, and Ta are 0.355, 0.41, 0.20, and 0.004, respectively. This case assumes a zero Pu-239 Doppler effect.

^(f) Same as (e) and, in addition, low ν , low σ_f , and high α for Pu-239.

^(g) Normalized to agree with two-zone core (see Table 1) for the 9 v/o BeO case.

The negative boron-10 reactivity worth was calculated for the core of the Soft Spectrum, Composite case without BeO. This calculation was also made for the same core composition and plutonium content in the fuel but with the cross-section values changed to correspond to a "Low Elastic" non-BeO case. The boron-10 worth obtained with the Soft Spectrum, Composite cross sections was found to be 20% higher than that obtained from the "Low Elastic" cross-section values. Neutron lifetime should show a similar dependence on the cross-section variations as boron-10 worth. This order of improvement in the agreement between predicted and measured parameters that are strongly spectrum dependent tends to make the Soft Spectrum, Composite appear as a reasonable direction for at least part of the cross-section variations that it encompasses. A definite conclusion along these lines, however, can be very hazardous because of the possibilities for compensating errors in cross-section or methods factors that are not being taken into account here (and even for systematic errors in the integral critical measurements!). In Table 5, the Soft Spectrum, Composite includes the Zero Pu-239 Doppler case since the latter also is justified mainly on the basis of integral critical experiments.

3.2.4 Heterogeneity Effects

All of the calculations reported here included a heterogeneity correction which consisted of using an equivalent potential scattering cross section. The simplified approach of a rational approximation leading to Bell's formula⁽²⁵⁾ was used. Neglecting this heterogeneity correction for the Base Case with BeO resulted in the following small changes: 5.0% higher Doppler, 0.0006 higher sodium void Δk , 1.5% increase in total breeding ratio, and 0.3% higher fissile content and 0.6% less fissions below 10 keV. These differences are so small that incorrect treatment of heterogeneity is not expected to introduce significant errors in the calculated results.

3.2.5 Influence of Uncertainties on Design Considerations

The physics parameters listed in Table 5 for the BeO and non-BeO cases include Doppler coefficients with sodium removed, core conversion ratios, and fissile doubling times in addition to the parameters listed in Tables 3 and 4. The doubling times are normalized to make the Base Case value agree with that of the 1000-MW(e) design described in Section 2.

If it be assumed that a minimum Doppler coefficient of -0.004 Tdk/dT with sodium removed and a near-zero reactivity for total sodium loss is required to satisfy safety criteria, the variations in data representing Good Coefficient, Composite or Soft Spectrum,

Composite offer considerable scope for adjusting the reactor composition and geometry to achieve these criteria with satisfactory neutron economy. For these favorable composite deviations in the cross-section data, breeding ratios around 1.3 and doubling times below 10 yr appear reasonable, using a significantly smaller amount of BeO than that in the BeO Base Case and a higher leakage than that of the Base Case without BeO. If the criterion of non-positive reactivity for total sodium void can be relaxed, then breeding ratios around 1.4 and doubling times around 6 yr appear attainable for the Good Coefficient, and Soft Spectrum, Composite deviations of the cross-section data, corresponding to the non-BeO reactor.

The Poor Coefficient, Composite deviations, on the other hand, offer a rather dismal possibility. For these cases, in order to achieve a -0.004 Tdk/dT Doppler coefficient with sodium removed, it appears necessary to have a tolerance for a significant positive total sodium void reactivity to maintain even a small breeding ratio in excess of unity.

The discussion in Section 3.2.3 of experimental evidence indicating softer neutron spectra than those calculated may provide some assurance that the Poor Coefficient, Composite situation will not arise. In addition, the Doppler measurements with the ZPR-III and ZEBRA assemblies strengthen the argument on a much smaller positive Pu-239 Doppler effect than is indicated by present calculations.

The bottom row of data in Table 5 shows results for a 3 v/o BeO case based on the Base Case cross sections except for Pu-239. The Pu-239 variations used were (1) low ν values, (2) high alpha values below 10 keV (both as in Table 4), (3) the low σ_f values between 24 and 170 keV, which gives slightly better coefficients but significantly increases the fissile inventory (Table 3), and (4) the zero Pu-239 Doppler effect as in Table 3. The case with 3 v/o BeO gives a Doppler coefficient with sodium removed that meets the -0.004 requirement. This case must be regarded as a realistic (and pessimistic) possibility until it is resolved by convincing experimental results. It is seen in Table 5 that such Pu-239 cross sections would give fuel economics significantly poorer than that of the 9 v/o BeO Base Case with tolerance for a significant positive reactivity for total sodium voidage. Without such tolerance the neutron economy resulting from this pessimistic Pu-239 cross section possibility would be much poorer than that calculated for the reference design.

The second-from-the-bottom row of data in Table 5 shows a 3 v/o BeO case in which the only variation considered is a zero Pu-239 Doppler coefficient. This case satisfies the Doppler and sodium void design

safety criteria with significantly better neutron economy than the 9 v/o BeO Base Case.

It is evident that the present status of fast reactor cross-section information is very confusing. Until this is resolved by a suitable program of energy point-wise and integral critical measurements, physics predictions of fast power reactor performance will remain highly unreliable.

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Discussion

Mr. Chernick (BNL): Now that the nominal breeding ratios of these big fast reactors are going down towards 1.2, I'd like to ask Dr. Greebler the questions that always bothered Wigner at these Fast Reactor meetings, namely, are not people being too lackadaisical about losses in processing a fuel? Might this not be just as important? With your goal of maybe 10% burnup, you have to reprocess only 10 times, but that is still a large number. What is the feeling of General Electric about this problem?

Mr. Greebler: The question of reprocessing losses is an important economic consideration not just from breeding considerations. I'd like to delay answering until tomorrow's panel session at which questions of safety and economics philosophy will be discussed.

Mr. Cohen (GE): I would like to make the following comment on this and the preceding paper: so far we have made intercomparisons without a real point of reference. I think it might be very useful, since we are about to have in ZPR-3 a fairly representative assembly, to invite the people who have taken part in the previous intercomparisons to calculate that SEFOR critical. Let's see where we are . . . before it goes critical.

Safety and Cost Analysis of a 1000-MWe Sodium-cooled Fast Power Reactor*

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(PRESENTED BY D. SMIDT)

At the end of 1964 the Karlsruhe group completed a design study of a 1000-MWe sodium-cooled fast breeder reactor.⁽¹⁾ A detailed safety and cost analysis has now been made. This takes into account good judgement as regards the safety of large fast reactors in general and for defining the criteria of a second design of a large plant and of the smaller prototype reactor as well. A number of interesting conclusions can be drawn.

1. Main Features of the Present 1000-MWe Design

Table 1 shows some of the important design parameters of the core. The main features are a cylindrical shape, oxide fuel of 87 % theoretical density, no moderator like BeO, and a moderate flattening with a height-to-diameter ratio of 1/3. This results in a relatively large negative Doppler coefficient and an internal breeding ratio (BR_{in}) close to unity. The void coefficient will then be somewhat larger than in several other designs. Its effect on overall safety will be one of the topics of this paper.

The coolant volume fraction of 50 % is relatively large, resulting in a small pressure drop and pump size. Special spacers with a small amount of structural materials can be used. The decision not to use BeO or similar moderators is purely economical. The loss in total breeding gain is evident, whereas the small internal breeding ratio contributes some additional operational difficulties.

The core has 2 zones of equal volume and different enrichment. The 229 subassemblies are of hexagonal shape; the inner radial blanket is of oxide, the outer one of metal. The axial blanket is 40 cm on either side. A fission gas plenum of 80 cm is below the core. In another paper during this Conference⁽²⁾ we give an evaluation of the vented fuel concept.

The characteristics of the plant design will be discussed in detail in our panel paper (see Session IV), so

* This paper has been prepared within the framework of the association EURATOM-Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

† EURATOM, Brussels, delegated to the Karlsruhe Fast Reactor Project.

that we mention only a few important items: The primary circuit is of the loop type with some features of the pool type (mainly rigid connection ducts between reactor vessel and heat exchangers; pumps and heat exchangers are sliding). There are two intermediate heat exchangers and four pumps, as well as four secondary loops. A new type of refueling system has been proposed.

The capital costs, based on studies from industry, have been calculated to be \$115/kW.

2. Influence of Group Constants

The effect of the cross-section set used for calculation of breeding ratio, Doppler effect, and void coefficient defines the reliability of any safety and cost analysis. The results of a world-wide comparison and their interpretation have been presented by Okrent (see first paper of this Session). We have made calculations for our specific reactor with three sets of cross sections:

- a) The Russian 26-group set ABN;
- b) A 60-group set;
- c) The new Karlsruhe 26-group set (of J. J. Schmidt and coworkers).

Table 2 shows the results.

3. Dynamics and Safety of the Reference Reactor

3.1 Dynamic Programs

The dynamic behaviour of our proposed reactor has been studied with the use of 3 programs:

- a) An analog program of the core. It included for the simulation:
 - 1) Space-independent neutron kinetics;
 - 2) Fuel heat generation and transfer through fuel and can, and heat removal by the coolant;
 - 3) Reactivity feedback caused by the Doppler effect, structural material, and coolant.

The fuel element was divided into 30 segments. For each segment the heat balance equation was set up; density, specific heat, and thermal conductivity were considered to be constant.

Fuel melting, the temperature dependence of the

Doppler coefficient, and a temperature-dependent heat transfer coefficient between fuel and can were included in the study.

b) An analog program of the total primary circuit, which included:

- 1) Similarly to the first program, neutron kinetics, heat generation, transfer, and removal, and the reactivity feedback (15 segments of the fuel element);
- 2) The heat exchange in the intermediate heat exchanger (16 segments);
- 3) The flow coast-down in the main coolant pumps;
- 4) The time delay of the pipes between reactor and heat exchanger;
- 5) The mixing process in the reactor inlet and outlet plenum.

c) A revised version of the digital code FORGE, developed by GE. The Karlsruhe version included, in addition, the temperature dependence of the heat transfer coefficient between fuel and can.

3.2 General Safety Criteria

It is impossible to define one general safety criterion for a fast reactor. We have already pointed out⁽²⁾ that one has to study each possible *actual* accident or incident more or less separately and evaluate the consequences. Since there is always some uncertainty whether one knows each possible *actual* cause of an accident, it is nevertheless helpful to study the general behaviour of a system under extraordinary conditions, such as fast reactivity ramps of infinite height, even if the results are not directly applicable to a real case.

TABLE 1. SOME DESIGN PARAMETERS OF A 1000-MWe SODIUM-COOLED FAST REACTOR

Core Height H:	95.5 cm
Core Diameter D:	286 cm
H/D: $\frac{1}{3}$	
Number of Core Zones:	2
Max Rod Power χ :	560 W/cm
Rod Diameter d :	6.7 mm
Coolant Fraction α :	50%
Structural Fraction β :	17.3%
Pressure Drop (Core and Axial Blanket) Δp :	3.2 kg/cm ²
Mean Power/Max Power = $\varphi = \varphi_{rad}\varphi_{ax}$:	0.66
Internal breeding ratio BR_{in} :	0.89
Total breeding ratio BR_{tot} :	1.385
Mean specific power q_{spec} :	1.2 MW/kg
Mean power density q_m :	0.599 MW/l
Δk_{void} maximum:	\$3.07
Δk_{void} totalCore:	\$1.04
Coolant inlet temperature ϑ_1 :	430°C
Coolant exit temperature ϑ_2 :	580°C
Maximum fuel center temperature ϑ_{max} :	2412°C

80-cm fission gas plenum at the bottom; 229 hexagonal subassemblies; 45-cm radial blanket; 40-cm axial blanket.

TABLE 2. CALCULATIONS WITH 3 CROSS-SECTION SETS FOR A 1000-MWe BREEDER*

	26 Groups KFK	26 Groups ABN	60 Groups
Critical Mass, kg	2168	2048	2010
Internal Breeding Ratio	0.95	0.94	0.91
Doppler Coefficient ($\times 10^6$)	-5.97	-8.32	-6.58
Δk_{max} for Sodium Loss	0.024	0.013	0.008

* Slightly revised version which has been used as a reference point for the systems analysis.

FIG. 1. Reactivity versus time for safety system

The *first* general safety criterion is the time t_m between the beginning of an infinite reactivity ramp input and the starting of melting of the hottest fuel. This gives a reference scale for the possibility of any counteractions by the scram system.

Since any reactivity input above \$0.25 will lead to fuel melting, and under certain conditions after sufficient time to destruction of the reactor, the reaction of the safety system should be included into the analysis. So our *second* safety criterion gives the *maximum* rate of the *reactivity ramp*, which can be counteracted by the safety system without starting fuel melting. We choose a conventional, spring-driven system with 10 safety rods.

Figure 1 shows the space-time relationship of the safety system, including the specific effects of inertia and friction. Naturally, this criterion is less general, since it depends on a specific safety system, but it is also more practical.

Also the delay time τ between the onset of the excursion and the beginning of rod movement has to be taken into account.

A third criterion should be the maximum hypothetical accident (MHA) and its consequences. This depends not only on the core design, but also on the containment and shielding properties and is even less general. The MHA will be discussed in Sect. 6.

3.3 Results with Safety Criterion 1, the Time t_m to Reach Start of Fuel Melting

In Table 3 the input data for the dynamic calculation of the reference reactor are given. In Fig. 2 t_m is plotted as a function of the Doppler and the structural expansion coefficients. The influence of the Doppler effect is distinct, but not too important. The structural coefficient acts only in the case of slow ramp rates.

In this connection it is interesting to compare t_m with t_{bc} , the time to reach coolant boiling (at channel exit), and t_{bf} , the time to reach fuel boiling. Here t_{bf} may be taken as an equivalent for core decomposition and termination of the excursion.

In Fig. 3 the three time intervals are plotted as a function of the ramp rate r_s . For large ramp rates always $t_{bc} > t_{bf} > t_m$. This is equivalent to the fact that it is impossible in a highly rated oxide core to get coolant boiling before reactor decomposition. Okrent, Cohen, and Loewenstein⁽⁸⁾ indicate the same results. Only for small r_s is $t_{bc} < t_{bf}$. Then coolant boiling may happen. In connection with safety criterion 2 we shall prove that these slow ramp disturbances can easily be governed by the safety system.

Figure 4 shows the effect of finite ramps. Here t_m is plotted as a function of the ramp height for different ramp rates. For $t_m \rightarrow \infty$ all curves converge to the same value: $r_h = \$0.25$, which is defined by static conditions. For this reactivity input the reactor does not need any safety system.

TABLE 3. BASIC CONSTANTS FOR DYNAMIC CALCULATIONS OF REFERENCE REACTOR

<i>Thermal Conductivity</i> (W/cm-°C)	
Fuel (1450°C)	0.03
Can (600°C)	0.21
<i>Specific Heat</i> (W-sec/g-°C)	
Fuel (1450°C)	0.343
Can (600°C)	0.503
Coolant (500°C)	1.264
<i>Density</i> (g/cm ³)	
Fuel (1450°C)	9.9
Can (600°C)	8.0
<i>Melting Temperature</i> (°C)	
Fuel	2800
<i>Heat Transfer Coefficient</i> (W/cm ² -°C)	
Fuel → Can (1450°C)	0.75
Can → Coolant (500°C)	14.5

FIG. 2. Time t_m to reach fuel melting versus Doppler coefficient α_D and structure coefficient α_S for reference reactor.

FIG. 3. Time t_m to reach fuel melting, time t_{bc} to reach coolant boiling (core exit), and time t_{bf} to reach fuel boiling versus disturbance reactivity ramp rate.

3.4 Safety Criterion 2, Maximum Ramp Rate

Figure 5 shows, for different Doppler coefficients, the maximum ramp rate which can be counteracted without fuel melting by the safety system as a function of the delay time τ before beginning of rod movement. The safety system consisted of 10 scram rods, each with a 250-kp/cm spring and a weight of 100 kg. The total reactivity value of the rods was \$15. (The space-time dependence of this system has been given in Fig. 1.)

In Fig. 6 is plotted the maximum ramp rate which

FIG. 4. Time t_m to reach fuel melting versus ramp height for different disturbance reactivity ramp rates for the reference reactor.

FIG. 5. Maximum allowable disturbance ramp rate r_s for different Doppler coefficients versus delay time for the reference reactor.

FIG. 6. Maximum disturbance reactivity ramp rate r_s for $\tau = 30$ msec versus Doppler coefficient. (Dotted line: r_s for $t_{bc} = t_{bf}$).

can be controlled by the above safety system without fuel melting, for a delay time of 30 msec, as a function of the Doppler coefficient. By consideration of some of the information of Fig. 3, we get the dotted line; it resembles the ramp rate, for which $t_{bc} = t_{bf}$. For ramp rates above this dotted curve the reactor disassembles before the coolant can boil. The ramp rates for coolant boiling are below the ramp rates, which can be governed by the safety system.

With respect to coolant boiling and void effect, therefore, it is an important fact that *it is impossible to generate sodium boiling by any ramp reactivity input as long as the normal conventional safety system is working*. It is much easier to destroy the reactor by fuel boiling.

Moreover, in principle the safety action can be accelerated by an additional electromagnetic force according to a proposal of Dosch.⁽⁴⁾

Necessarily the values of fuel conductivity, melting temperature, and gap conductivity after some irradiation are not too well known. The influence of these uncertainties is shown in Fig. 7, which shows calculated allowable ramp rates as a function of τ for different values of the fuel melting temperature.

3.5 Actual Accidents

So far we have studied the effect of hypothetical ramp excursions. The actual ramps, i.e., control rod

runaways, will definitely be kept to the order of $\$0.01/\text{sec}$ and below.

Another actual possibility is a *loading accident*. Suppose the central fuel element, with $\$0.7$, is dropped into the cold critical reactor in 500 msec. The effect of the resulting perturbation is shown in Fig. 8. All temperatures can be kept below dangerous values.

In Fig. 9 can be seen the result of a simultaneous failure of all 4 primary pumps. The can temperature will reach the boiling point of sodium (about 960°C) after 90 sec.

As is shown in Fig. 10, the structural expansion coefficient will reduce the reactor power to about 75% during this time. The effect of the uncertainty in this coefficient can also be seen from this figure.

3.6 Stability

It can be expected that a reactor system with a fast negative and a slow positive coefficient will be stable in most cases. This has been proved by a detailed analysis. According to the Nyquist diagram, instability may oc-

FIG. 7. Maximum allowable disturbance ramp rate r_s for different fuel-melting temperature versus delay time for the reference reactor (Doppler coefficient = $6 \times 10^{-6}/^\circ\text{C}$).

FIG. 8. Loading Accident: reactivity, power, max fuel center temperature, and coolant temperature (core exit) versus time.

FIG. 9. Loss of pumping power: coolant flow Q , core inlet temperature T_{inlet} , core exit temperature T_{exit} , and can temperature T_{can} versus time t .

FIG. 10. Loss of pumping power: reactor power N versus time for different structure coefficients α_{str} .

cur if the Doppler coefficient is smaller than $1/50$ of its actual value or if the coolant coefficient is 50 times its actual value.

It has been shown that instability may arise also from higher-order delay terms. But there is no reason to assume those.

4. Parametric Studies; Influence on Cost and Safety

4.1 Survey of Variations

It has been the central point of our analysis to define the influence of the important design parameters on cost and safety and by this to learn where our concept has to be changed. To do this, we systematically varied many values and calculated the effect on safety and

costs. Table 4 summarizes the parametric variations, as well as their expected advantages and disadvantages.

In comparison with other parametric surveys, ours refers to a real system. Therefore many detailed effects of an extraordinary importance are included. For example, if the coolant fraction α is lowered (see case 1 in Table 4), the pressure drop rises and therefore more structural material is needed. Moreover, other types of spacers may be needed for geometrical reasons.

Also, for a real system with given heat exchanger, temperatures, pressures, etc., the effect of parametric variations on fuel and capital costs will be more realistic.

For our investigations the following programs were used:

- 1) One-dimensional multigroup-diffusion program "MGP";⁽¹²⁾

- 2) Two-dimensional diffusion program "Twenty Grand";⁽¹³⁾
- 3) Nuclear program system Karlsruhe "NUSYS" [not published];
- 4) Two-dimensional perturbation Code "2 D-Pert." ⁽¹⁴⁾

The enrichment of the first and second core zones was determined by one-dimensional diffusions calculations ("MGP") with the 26 KFK-group set.⁽¹⁵⁾ From these results were generated the macroscopic 6-group cross sections by "NUSYS." Then the breeding ratios, critical masses, reactivity coefficients, and power distributions were calculated by the "Twenty Grand" and the two-dimensional perturbation code "2 D-Pert." For the calculation of the Doppler coefficient the one-dimensional perturbation code contained in "NUSYS" were used.

The determination of the core region in which removal of the sodium caused the maximum $\Delta k_{\text{rod loss}}$ effect, was performed by two-dimensional perturbation calculations. The $\Delta k_{\text{rod loss}}$ effect itself was determined by one- and two-dimensional diffusion calculations.

4.2 Discussion of Nuclear Results

In Table 5 the main results are given. In Fig. 11a the most important results are represented graphically.

Case 1: The reduction of the coolant fraction α from 50 to 40% should result in a larger internal breeding ratio. However, the coolant pressure drop is raised to 13.7 atm, and the larger requirement in structural material cancels the gain in the internal and total breeding ratios.

Case 1a: If $\Delta\theta$ is raised to 200°C and α changed in such a way that the pressure drop is the same as for the reference reactor, the internal and total breeding ratios are somewhat higher. The Doppler coefficient stays practically unchanged during these variations, whereas the void effect is somewhat reduced for the smaller values of α .

The larger value of $\Delta\theta$ also allows for a smaller effective temperature difference between the primary and secondary sodium circuit, and reduces the danger of thermal shocks in the case of a break in the intermediate heat exchanger (IHE). On the other hand, the IHE must be larger or the thermal plant efficiency will be reduced. This will be discussed in our cost considerations.

Case 2: A smaller ratio H/D must be paid for in the internal breeding ratio and Doppler coefficient. The reduction of the void effect is favorable compared with the reference reactor, but does not pay in comparison with Cases 1 and 1a.

Case 3: By addition of 5% BeO the Doppler constant is nearly doubled, whereas the void effect is reduced by about \$1. The penalty is purely economical. The advan-

TABLE 4. LIST OF PARAMETRIC VARIATIONS

0) Original Reactor	Expected Advantages	Expected Disadvantages
1) Smaller coolant fraction α	Internal BR ; Total BR	More Δp ; More structural material
1a) Smaller α and larger $\Delta\theta$	Less structural material than 1a)	Larger heat exchange
2) Smaller H/D	Smaller void effect	Smaller Doppler; Smaller BR_{in} ; Capital costs?
3) 5% BeO added	Larger Doppler	Smaller BR
4) 10% Mo in fuel	Low fuel temperature	Smaller Doppler; Smaller BR
5a) UC, same geometry as oxide	Low fuel temperature; Large internal and total BR	Larger void effect
5b) UC compact core	Compared to 5a): capital costs?	Smaller BR_{in} ; Larger Δp
6) Vented fuel	Larger BR ; Less pumping power	Gas-purification plant

tages are not too important for operational stability and safety, but are in terms of the maximum hypothetical accident.

Case 4: The addition of molybdenum for a better fuel conductivity is plainly disadvantageous. The breeding ratio is low, the Doppler constant is low, and the void effect is high.

Case 5a: In this carbide core the internal and total breeding ratios are very large, the Doppler coefficient nearly as large as for the oxide cores, but there is a tremendous void effect. The advantage of the low fuel temperature will be discussed in connection with the dynamic behaviour.

Case 5b: Here the breeding is strongly reduced, especially internally. The Doppler effect is smaller, but this is true also for the void effect.

Case 6: The main advantage of the vented fuel concept is the low amount of canning material with a subsequent gain in breeding. It might still be possible that strong gas pressures develop during certain transients and that the can has to withstand them. This might change the evaluation of the vented fuel.

4.3 Discussion of Economical Results

In Table 6 and Fig. 11b the results of the comparative cost calculation are given. All calculations are based on the same assumptions as for the reference reactor.⁽¹⁾

TABLE 5. CHARACTERISTIC DATA OF DIFFERENT REACTORS

	Reactor Number								
	0	1	1a	2	3	4	5a	5b	6
	Characteristic Data								
	Reference Reactor	Smaller Coolant Fraction α	Smaller Coolant Fraction α and Larger $\Delta\theta$	Smaller H/D	5% BeO added	10% Mo in Fuel	UC Fuel; Same Geometry as Oxide	UC Fuel; Compact Core	Vented Fuel
Core Volume (m ³)	5.9	5.3	5.2	6.0	6.6	5.9	5.9	3.2	5.9
Core Height (m)	94	91	90.5	78	90	94	94	77	94
Height/Diameter: H/D	1/3	1/3	1/3	1/4	1/3	1/3	1/3	1/3	1/3
Pressure Drop (atm.)	4.6	13.7	4.6	2.8	5.1	4.6	4.6	12.3	3.3
Sodium (v/o)	50	40	44	50	50	50	50	50	50
Fuel (v/o)	32.2	34.4	35.0	33.0	27.2	29.3	32.2	28.8	36.8
Steel (v/o)	17.8	25.6	21.0	17.0	17.8	17.8	17.8	21.2	13.2
Coolant Temp Rise $\Delta\theta$ (°C)	150	150	200	150	150	150	150	150	150
Volume Ratio of UO ₂ /PuO ₂ (UC/PuC):									
1 Core Zone	8.2	8.1	8.4	7.9	7.3	7.3	9.7	8.2	9.3
2 Core Zone	6.0	6.0	6.1	5.8	5.3	5.2	7.3	5.6	6.7
Critical Mass (kg)	2168	2093	2046	2320	2295	2220	2370	1416	2245
Breeding Ratios:									
Core (int.)	0.95	0.95	0.97	0.91	0.89	0.84	1.12	0.91	1.04
Blanket	0.47	0.44	0.48	0.52	0.39	0.47	0.50	0.62	0.55
Total	1.42	1.39	1.45	1.43	1.28	1.31	1.62	1.53	1.59
Doppler Coefficient	$-5.97 \cdot 10^{-6}$	$-5.99 \cdot 10^{-6}$	$-5.99 \cdot 10^{-6}$	$-5.30 \cdot 10^{-6}$	$-1.05 \cdot 10^{-5}$	$-4.61 \cdot 10^{-6}$	$-5.76 \cdot 10^{-6}$	$-4.72 \cdot 10^{-6}$	$-6.08 \cdot 10^{-6}$
Max Sodium-loss Effect									
$\Delta k_{\text{Sod-loss}}$	0.0244	0.0224	0.0221	0.0223	0.0201	0.0271	0.0291	0.0201	0.0263
Total Additional Cost (10 ⁶ \$)	0	+3.00	-2.11	+1.39	+5.36	+3.98	-4.24	-6.05	-2.98

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FIG. 11. a) Internal breeding ratio BR_i , total breeding ratio BR , Doppler constant D , and reactivity for complete core sodium loss V , for different reactors. b) Total additional costs T , and differences in capital costs C and fuel costs F .

The main changes of capital costs result from the calculated pressure drop and the corresponding pump size. A relatively moderate power law with an exponent 0.6 has been assumed for the cost dependence on pumping power. Other main cost variations come from the fuel and the breeding ratio. These values are capitalized over 15 years for a load factor of 0.8.

Case 1: Additional costs mainly from pumping power.

Case 1a: Cost savings mainly because of better breeding ratio.

Case 2: Savings in pump size are more than compensated by fuel costs (large number of fuel rods).

Case 3: Somewhat larger capital costs (pressure drop) and much larger fuel costs (breeding). Additional costs of nearly 5% of the total plant costs.

Case 4: Additional costs mainly by bad breeding.

Case 5a: Considerable cost savings by favourable breeding ratio.

Case 5b: Even with some additional capital costs for pumping power and less breeding ratio than for Case 5a the total net savings are largest (5% of total plant).

This results mainly from lower fuel costs because much less fuel is needed.

Case 6: Savings in pumping power and breeding ratio are partially compensated by gas-purification plant (see Ref. 5). Savings of the order of 2% of total costs.

4.4 Discussion of Dynamical Results

Considerable differences in the dynamical behaviour compared to the reference reactor are found only if other fuels are used. As can be expected, moderate changes in coolant temperature and geometry are of minor importance.

In Fig. 12 t_m is shown for Cases 0, 4, 5a, and 5b as a function of ramp rate. The two low-temperature cores with molybdenum and carbide have values of t_m somewhat above those for the reference core and for the highly rated carbide.

Figure 13 shows the effect of ramp height. The low-temperature cores naturally can withstand considerable reactivity inputs without any action of the safety system.

TABLE 6. CAPITAL AND FUEL COSTS

	Reactor Number								
	0	1	1a	2	3	4	5a	5b	6
	Additional Costs (\$)·10 ⁴								
	Reference Reactor	Smaller Coolant Fraction α	Smaller Coolant Fraction α and larger $\Delta\theta$	Smaller H/D	5% BeO added	10% Mo in Fuel	UC-Fuel Same Geometry as Oxide	UC-Fuel Compact Core	Vented Fuel
a) Capital Costs									
Core Shroud	0	-0.18	-0.20	+0.23	+0.23	0	0	-0.95	-1.00
Thermal Barrier	0	-0.33	-0.38	+0.35	+0.28	0	0	-1.80	-2.25
Inner Vessel + Shielding	0	-0.98	-1.13	+1.25	+1.30	0	0	-5.55	-6.00
Reactor Vessel	0	-0.43	-0.58	+0.63	+0.65	0	0	-2.55	-2.75
Shielding Assemblies	0	-0.70	-0.75	+0.20	+0.90	0	0	-3.68	-6.00
Refueling Equipment	0	-0.13	-0.13	-0.60	+0.15	0	0	-0.65	-3.00
Reactor Vessel + Internal Inventory	0	-2.75	-3.17	+2.06	+3.51	0	0	-15.18	-21.00
Intermediate Heat Exchanger + Containment	0	0	+84.50	0	0	0	0	0	0
Pumps	0	+450.00	-110.00	-120.00	+30.00	0	0	+380.00	-50.00
Gas-purification System	0	0	0	0	0	0	0	0	+230.00
Capital Costs (\$)·10 ⁶	0	+4.47	-0.29	-1.18	+0.34	0	0	+3.65	+1.84
b) Fuel Costs									
Gross Fuel Costs (mills/kWhe)	+0.89	+0.88	+0.88	+0.94	+0.90	+0.89	+0.95	+0.77	+0.85
Plutonium Gain (mills/kWhe)	-0.13	-0.11	-0.15	-0.13	-0.04	-0.05	-0.26	-0.19	-0.17
Fuel Costs (mills/kWhe)	+0.76	+0.77	+0.73	+0.81	+0.86	+0.84	+0.69	+0.58	+0.68
Fuel Cost Difference (mills/kWhe)	0	+0.01	-0.03	+0.05	+0.10	+0.08	-0.07	-0.18	-0.08
Annual Addit. Cost (\$)·10 ⁶	0	+0.03	-0.23	+0.33	+0.65	+0.51	-0.55	-1.25	-0.62
Equivalent Capital Costs (Present Value) (\$)·10 ⁶	0	+0.26	-1.82	+2.57	+5.02	+3.98	-4.25	-9.70	-4.82
Capital Costs (\$)·10 ⁶	0	+4.42	-0.29	-1.18	+0.34	0	0	+3.65	+1.84
Fuel Costs (\$)·10 ⁶	0	+0.26	-1.82	+2.57	+5.02	+3.98	-4.25	-9.70	-4.82
Total Additional Costs (\$)·10 ⁶	0	+4.73	-2.11	+1.39	+5.36	+3.98	-4.25	-6.05	-2.98

In Fig. 14 is plotted the maximum allowable ramp rate as a function of the delay time of the safety system. The low-temperature cores allow for ramps faster by about 1.5 times.

5. The Importance of Coolant Boiling

Partial or complete voiding of the total core may have two causes:

- loss of coolant flow;
- a slow excursion.

Both occurrences are improbable to a very high degree. As has been shown, a) requires the simultaneous failure of all primary pumps, of all emergency drives despite a time reserve of about 90 sec, and of the safety system (a

break of the double-wall primary circuit is excluded). b) requires the simultaneous runaway of several control rods and the failure of the safety system or the runaway of a control rod and the simultaneous failure of all independent safety rods.

It should really be discussed whether a maximum accident beyond this is credible. But if we assume credibility, then one of these accidents would certainly destroy the core. Ramps of rates up to \$100/sec may be generated by coolant evaporation, depending on the initial conditions. The total energy of the excursion is strongly dependent on the Doppler coefficient.

It is only for this type of hypothetical accident, that the void coefficient is of real importance. The dynamic studies

have shown, that under other conditions boiling in larger parts of the core ($\Delta k > \$1$) cannot occur. (Boiling in a single subassembly will be discussed later.) However, in the light of these considerations two remarks have to be made:

First Remark. The boiling starts at the upper end of the core channels. In this region the void coefficient is negative. Therefore it has been said that an overall positive void coefficient might not be too dangerous under these conditions. This opinion is too optimistic. Because of the large specific volume of sodium vapor, a boiling channel will be blocked very easily by the large pressure drop of the two-phase mixture. For our reactor, boiling begins at 28% of normal coolant flow, whereas the channel is already blocked at 22%.

If boiling starts at all, chances are very large that a complete flow blockage with subsequent complete voiding occurs.

Second Remark. Some designs aim at a $\Delta k \leq 0$ for total voiding, but a positive maximum Δk . If voiding occurs according to the above-mentioned mechanism of channel blockage it will gradually spread from the core center and at some time the maximum Δk will be effective. Therefore, $\Delta k \leq 0$ for total voiding should not be mistaken for a criterion of a qualitative nature. It might

FIG. 12. Time t_m to reach fuel melting versus disturbance reactivity ramp rate for different fuel materials.

FIG. 13. Time t_m to reach fuel melting versus ramp height for a ramp rate of 2 $\$/\text{sec}$ for different fuels.

FIG. 14. Maximum allowable disturbance ramp rate r_s versus delay time for different fuels.

be helpful in a quantitative way only insofar as the maximum Δk will also be smaller compared to the case in which Δk for total voiding is positive.

Although we consider these types of boiling events to be very improbable, the *blockage of a single fuel subassembly*, for example by something in the sodium flow, is much more probable. The reactivity effect of this can be governed easily by the temperature coefficient (for the central subassembly it amounts to $+\$0.08$). But it might be disastrous if the sodium superheats to a larger degree.

Experimental results on sodium superheat have been reported.⁽⁷⁻⁹⁾ The bulk superheat certainly depends on the heat flux and on the surface conditions. Superheating of the order of 200°C and even more might be possible. Taking as an example a maximum superheat of 200°C and taking into account the temperature distribution in the coolant under reduced flow conditions (21 % of normal flow), a superheating energy of about 70 kcal will be stored in the upper quarter of the subassembly.

If, finally, the superheated liquid is flashed, everything depends on the mechanism as to how the energy is released in space and time. We have calculated the effect of the worst case, i.e., an instantaneous release of the whole energy and its impact on the subassembly wall, by using a one-dimensional model of Symonds and Mentel.⁽¹⁰⁾ Then apparently the subassembly box will be badly deformed and the neighbouring subassemblies will be affected. If the combination of flow reduction and superheat now occurs in the neighbour assemblies, the damage may spread over the core like a chain reaction.

Accordingly, we concentrate an important part of our effort on three subjects:

- a) Theoretical and experimental studies of the dynamics of two-phase flow and of the release of superheat in space and time. A first step in this direction is reported by Fischer and Häfele.⁽¹¹⁾
- b) Development of methods to keep the superheat in a fast power reactor small.
- c) Design of the subassembly boxes so that the superheating energy can be dispersed at a maximum rate.

6. Some Remarks on the Maximum Hypothetical Accident

The MHA is a fast excursion with the effect of a positive void coefficient. There are two possibilities for the MHA:

- a) The core is disassembled by the fastest possible reactivity input rate (first excursion).
- b) After the first excursion, the molten core material

gathers somewhere in the reactor containment (second excursion).

The maximum reactivity input rate for the first excursion is caused by voiding of the core center. This again can be caused by a slow primary excursion or by a loss of coolant flow. The other possibility for a fast reactivity increase is core meltdown or, at least, a certain stage of fuel slumping.

The consequences of the second excursion may be influenced by the shape of the containment.

Both the first and second excursions are strongly influenced by the Doppler coefficient. In our panel paper (see Session IV) we shall discuss this point in more detail. The released energy has to be contained in the concrete structure surrounding the reactor vessel.

7. Conclusions

7.1. According to criteria 1 and 2, our proposed 1000-MWe reactor is very safe.

7.2. Infinite ramps of the order of \$10/sec can be counteracted by a spring-driven safety system, much more than will actually occur.

7.3. Coolant boiling in larger regions of the core cannot occur as long as the safety system is operating.

7.4. The positive void effect is of importance for the maximum hypothetical accident only. It occurs only if several independent improbable conditions are fulfilled simultaneously.

7.5. Coolant boiling in a single subassembly may be dangerous in combination with sodium superheat.

7.6. Parametric studies show advantages in cost and safety in the direction of lower coolant fractions, larger $\Delta\theta$, and a not-too-compact carbide core. The addition of molybdenum to the fuel oxide proves to be very unfavourable.

7.7. Further studies on the MHA and on sodium superheat and sodium two-phase flow are required.

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Discussion

Mr. Gibson (AI): I noted that your vented fuel elements showed the greatest economic advantage. We performed some studies which showed that the advantage of a vented fuel element depended upon a lack of swelling in the fuel itself to the extent that the fuel swelling did not impose upon the cladding an excessive strain. If one presupposes this condition then, indeed, the vented concept offers the advantage of a reduction in the cladding stress of the order of a third. If, however, the fuel is allowed to expand or one finds that it does until it imposes a cladding strain upon it, then the possible advantage of reduction in cladding thickness is pretty marginal. Did you consider these effects?

Mr. Smidt: First, the vented fuel is not the most economic on this display. Savings as compared to the reference reactor amount to only about 2% of the total costs, whereas, for instance, the compact carbide core saves about 5%. In comparison with a more optimized reactor, the savings are even less. As you remark, any swelling would make the use of a vented fuel less effective.

Mr. Leitz (GE): Did your reference fuel show a fission gas plenum at the bottom end of the fuel pin?

Mr. Smidt: Yes; the reference design has the fission gas volume plenum at the bottom to have less stress in the fuel rods.

Mr. Leitz: Did you consider the hazard associated with possible blanketing of adjoining fuel pins in the event of failure of the clad and release of gas from that region? If the fuel pin should fail down in this plenum region and release the fission gas, which is large in inventory and large in pressure, does this not threaten to blanket the adjoining fuel pins and wipe out the subassembly?

Mr. Smidt: This is a danger we are now discussing. The overall reactivity effect when the total subassembly is full of gas, which would be about the worst type of accident, is about 7/100 of a dollar.

Mr. Hall (LASL): You spoke of the danger of fuel-element failure propagating from one subassembly to another and said you were going to take care of this by design consideration. Are you planning any work to substantiate this worry?

Mr. Smidt: Yes. We are working together with EURATOM-ISPRA. We are doing some work ourselves on the effect of two-phase flow and the effect of shock waves. The last paper of this conference, by Fischer and Häfele, will give a review of the theoretical approach.

Mr. Häfele (Karlsruhe): If I may additionally comment on that. Investigation of the full mechanism of two-phase flow is parallel to the investigation of the nuclear aspects insofar as safety analysis is concerned. We are paying particular attention to the superheat and plugging accident, as well as to the mechanism of desuperheat with its energy release. More experimental work on desuperheat is needed.

Mr. Bump (ANL): In your vented fuel, does the sodium come in contact with the fuel itself?

Mr. Smidt: Our most optimistic vented fuel is one with a porous plug, the simplest kind of closure. This produces a gain of about 2% in cost. So we didn't make any detailed experimental design studies.

Mr. Bump: With the porous plug could you keep the sodium away from the fuel then? Is that your assumption?

Mr. Smidt: Yes.

An Analysis of Fast Reactor Transient Response and Safety in Selected Accidents

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INTRODUCTION

In the development of large fast reactors, attainment of the optimum balance between safety and economics considerations is one of the most important problems to be resolved. Conceptual designs which have been developed⁽¹⁻⁴⁾ do reflect little consensus with regard to necessary values for parameters important to safety, such as the Doppler and sodium reactivity coefficients.

A study was therefore made of large fast reactor behavior under accident conditions to provide information which would be helpful in assessing the importance of various parameters to safety, and perhaps also in determining desirable value for these parameters. The FORE computer program⁽⁵⁾ was used to calculate the course of accidents in typical large fast reactor cores under a variety of assumptions. Accidents analyzed were nonterminating ramp reactivity insertions at \$0.20/sec and \$2/sec, and loss of pumping power with resultant flow coastdown.

The inherent safety of the reactor is independent of the system of reactor control, although it has considerable effect on the control-system design. Therefore, although most serious accidents will require a scram to terminate them before core damage occurs, no scram was assumed in this analysis. Instead, the length of time available before a given type of damage or failure occurred was considered to be a measure of the inherent safety of the reactor. A decrease in this time interval requires greater reliance and places greater requirements on the speed and reliability of the reactor-control system. For accidents caused by continuous, constant-rate reactivity insertions, such as were considered in these calculations, the length of time elapsed before core damage is also an indication of the capability of a reactor system to absorb reactivity insertions.

Consideration was given to three principal types of inherent reactivity coefficients which could be present: a prompt coefficient (normally negative) due to changes in fuel temperature, a delayed negative coefficient due to expansion of the reactor structure, and

a delayed coefficient due to coolant density change, which may be either negative or positive, depending on core design. The fuel temperature coefficient was assumed to be due essentially to the Doppler effect. In the case of metal fuel, a fuel expansion effect was also assumed, although it may not be dependable if significant burnups are to be achieved. The significance of the coolant reactivity effect is, of course, greatly increased if actual coolant voiding occurs; the reactivity feedback is then a function of both the size and location of the voids,^(1, 6) and the value of the coefficient for small density changes is not applicable.

The values chosen for the Doppler coefficient range from a small value, $T(dk/dt) = -0.0015$, which might be expected to occur without effort on the part of the reactor designer, to a large value, $T(dk/dt) = -0.01$, which probably requires deliberate spectrum softening to achieve.

The radial structure expansion coefficient represents the combined effects of increasing core size and decreasing atom densities (except for coolant) which result from thermal expansion of the core structure. The value given, $-3.7 \times 10^{-6}/^{\circ}\text{F}$, is the same as that given for two quite different 1000-MWe designs, a pancake and an annular core.^(1, 2) In the axial direction, the height of the core is defined by the height of the fuel column. Feedback from this source was assumed only for those cases with metal fuel. The sodium coefficient and the axial expansion coefficient are the same as those in Ref. 1, namely, $+0.15 \times 10^{-6}/^{\circ}\text{F}$ and $-1.3 \times 10^{-6}/^{\circ}\text{F}$, respectively.

ASSUMED CORE DESIGN

Core designs investigated were intended to be representative of typical cylindrical, 1000-MWe, oxide-, metal-, and carbide-fueled cores, the metal atoms being mixed uranium and plutonium. On the assumption that carbide has somewhat greater flexibility with respect to its specific power, because of its high conductivity and a sodium-filled fuel-clad gap, both a low (150 kW/kg of carbide) and a high (300 kW/kg)

TABLE 1. CORE DESIGNS

Core Power: 2200 MWt; Axial Blankets: 300 MWt
 Coolant Inlet: 800°F
 Coolant ΔT : 250°F
 Coolant Velocity: 20 ft/sec
 Radial Peaking Factor: 1.7
 Axial Reflector Savings: 20 cm

Core	Fuel Pin	kW/kg of Oxide, Carbide, Metal	Core Height, ft	Core Diameter, ft	Sodium/Fuel/ Structure, Volume Percentages	Peak Fuel Operating Temp, °F
Oxide, 150 kW/kg	0.242-in.-OD; 15-mil stainless steel	150	2.84	8.81	50/30/20	~4900
Carbide	0.250-in.-OD; 15-mil stainless steel clad, sodium bonded	150	2.84	8.38	55/27/18	~2400
Carbide	0.250-in.-OD; 15-mil vanadium clad	300	2.84	7.38	71/17/12	~3700
Metal	0.140-in.-OD; 10-mil vanadium clad	150	2.7	7.9	62/22/16	~1300

specific power were used for the carbide. For the oxide and metal cases specific power was not a variable. Table 1 summarizes the core designs.

The fact that these cores are somewhat idealized should be kept in mind when interpreting the results. For example, it should be noted that the radial peaking factor of 1.7 assumed is equivalent to assuming little or no purposeful power flattening in the core, and that local peaking factors were not taken into account. An actual core design might tend to have a lower value of the radial peaking factor due to power flattening, but local peaking factors would offset this. The location of the hottest channel or fuel pin would then not necessarily be in the core center, as it was in this analysis.

Core compositions were picked in terms of volume percentages of sodium, fuel, and structure. The idealization was made that all sodium in the core was effective as a heat transport agent, and space for control was neglected. Thus, specification of core composition combined with a choice of specific power (in kW/kg of fuel meat) was sufficient to specify core volume and height. In all cases the volume ratio of fuel to total structure (including clad) was held close to 3/2, a value which should allow enough structure for a good mechanical design. Fuel-pin designs were chosen to be typical of the fuel material (oxide, carbide, or metal) under consideration and consistent with the specific power. Clad was 15 mils thick, and fuel pins were approximately 0.25 in. in diameter, except for the metal fuel, which had a 0.14-in. diameter with 10-mil clad. All fuel except the oxide was sodium bonded. The oxide and lower-specific-power carbide had stainless steel clad, other cores vanadium clad.

Any of the cores used in the analysis could have been designed with a smaller height-to-diameter ratio if desired, by assuming a lower coolant temperature rise and/or a lower coolant velocity. On the other hand, increasing the height-to-diameter ratio would require higher velocity or larger temperature rises than those chosen, and revisions in this direction are therefore limited.

FAILURE MECHANISMS

The analysis of the results consists in large part of identifying the failure mechanisms in each transient, the time elapsed before failure, and its location in the core. Failure in this case is intended to mean essentially voiding of coolant, or occurrences which lead very shortly to voiding of coolant, since such voiding can theoretically result in large reactivity insertions, and will in any case cause clad melting and possible core compaction.

Three mechanisms were considered for failure:

1. Fuel clad fails due to pressure from within, possibly causing complete or partial flow blockage and subsequent coolant boiling.
2. Coolant boils due to overheating.
3. Burnout or vapor blanketing of the clad occurs, resulting shortly in clad failure and possible blockage of the coolant channel.

To identify when these might occur is difficult except for the mechanism of coolant boiling because the failure of the clad due to internal pressures is a function of fuel design (smeared density, vented to coolant or not, etc.), and the condition of the clad (neutron irradiation), as well as fuel and clad temperatures. Also,

the burnout condition for sodium under conditions of interest is not well-known. Consequently, a simplified approach was employed. Clad failure due to internal pressure was considered possible over a range of conditions, and the lower and upper bounds of these conditions were identified respectively with

1. a fuel center temperature *above* melting, and
2. a fuel center temperature equal to the fuel boiling point at atmospheric pressure.

The first postulated indication, a fuel temperature above the melting point, could cause failure due to fuel volume changes that could not be accommodated within the clad. For example, radial expansion of oxide fuel in going from room temperature to the melting point would be approximately 4%,^(7, 8) whereas stainless steel clad operating at an average volumetric temperature of 1200°F would expand only about 1.2%. In addition, there is reported to be a volume change in oxide fuel due to melting of 9.6%.⁽⁹⁾ Inspection of the radial temperature profiles from computer problems run for this study indicate that 25% of the fuel is molten or partially molten when the peak fuel temperature begins to rise above the melting point, giving another 1-2% volume increase in relation to the overall fuel volume. If the clad should have lost its ductility due to irradiation, these volume changes in the fuel could conceivably cause failure in a fuel pin containing high-density fuel. Similar behavior could presumably occur in the carbide and metal fuel, although the actual percentage volume increases would be different. If, on the other hand, ample space exists within the clad for fuel volume changes and clad temperatures are not excessive, an upper bound may be placed on the fuel element transient capability by the vapor pressure of the fuel itself.

Although a high temperature is required for the vapor pressure to become significant, relatively small further increases in temperature will produce very high vapor pressures which would burst the clad. For UO_2 , an extrapolation of Ackermann's data⁽¹⁰⁾ indicates a vapor pressure of 10 atm at 6500°F and 100 atm at 7250°F. Since this is subject to great uncertainty at such temperatures, a fuel center temperature of 6500°F was assumed to be a failure point due to fuel vapor pressure in the mixed oxide fuel. Experimental confirmation exists that oxide fuel pins will withstand temperatures of this magnitude.⁽¹¹⁾

The boiling point of uranium carbide at atmospheric pressure has been reported to be 4100-4370°C.⁽¹²⁾ The lower temperature, 7400°F, was assumed to represent failure due to excessive vapor pressure for the carbide fuels. Tests have been made in which sodium-bonded carbide pins withstood transient fuel temperatures of about 4200°F.⁽¹³⁾

For the metal fuel a melting point of 2070°F was taken.⁽¹⁴⁾ An extrapolation of available correlations for plutonium would indicate a boiling point of 5850°F.⁽¹⁴⁾ The steady-state temperatures with metal fuel are less than 1500°F, and the analyses were not carried far enough to observe a temperature of 5850°F in the metal fuel. In the accidents analyzed, failure due to high vapor pressure in the metal-fueled cases would have occurred after all other modes of failure and was therefore considered to be of minor significance.

Bulk boiling of the coolant was assumed to take place if a temperature of 1620°F, the atmospheric pressure boiling point, was reached. Normally it would be expected that the pumps would pressurize the core slightly and the boiling temperature might be 100°F or more higher than was assumed.

The third failure mechanism is burnout. Experimental data on critical heat fluxes at the pressures corresponding to full flow (3 atm or more) are limited. A simple model was used wherein the critical heat flux was the sum of the pool-boiling heat flux and the forced-convection heat flux due to subcooling, and the pool-boiling critical heat flux was assumed to be 1,000,000 Btu/hr/ft².⁽¹⁵⁾ This assumption may be quite conservative, depending on the pressure dependence of the critical heat flux. However, increasing the assumed heat flux required for burnout would have little effect on the conclusions to be drawn from the analysis. Since the FORE code has no provision for two-phase flow or boiling heat transfer, the burnout condition was identified by a clad surface temperature of 1720°F, with 1620°F being the saturation temperature at atmosphere pressure and 100°F the film drop equivalent to a heat flux of 10⁶ Btu/hr/ft².

LOSS OF FLOW ACCIDENTS

Figure 1 shows the coolant temperature as a function of time for a loss of flow in oxide fuel cores with different Doppler coefficients. The temperature levels off and even starts to fall after the flow reaches its assumed minimum of 13% of full flow at 10 sec. All of these cases had the normal radial structure expansion, and it can be seen that the Doppler coefficient has very little effect on the accident. What effect it does have is a slight disadvantage because fuel temperatures drop in these accidents, giving a positive Doppler feedback. The large rise in coolant temperature causes a large radial expansion of the core, and the resultant negative feedback is the dominant factor in the accident, causing a drop in power and in fuel temperature.

Similar calculations for the carbide and metal cores give very similar results, with maximum coolant temperature somewhat lower, particularly for the cases of

metal and low-specific-power carbide. However, the coolant boiling point is exceeded in all cases.

In Figure 2 the effect of radial expansion of the core in the loss-of-flow accident is demonstrated. These cases are all for a Doppler $T(dk/dt)$ of -0.004 . The maximum coolant temperature reached is severely affected if no effective radial expansion feedback exists. If the radial expansion coefficient were large enough, coolant boiling might be prevented.

Figure 3 shows the average fuel temperatures for the same three cases. In the situation with no radial expansion feedback, core power is not reduced significantly, and fuel temperatures rise.

In all loss-of-flow accidents, coolant boiling was the first failure mechanism. Heat fluxes generally were not high enough to cause burnout, except for carbide fuel operating at 300 kW/kg.

Two other points should be noted. First, the coolant

FIG. 1. Coolant Temperature for Loss of Flow in the Case of Oxide Fuel.

FIG. 2. Effect of Structural Expansion Feedback on Coolant Temperature in Case of Loss of Flow.

FIG. 3. Effect of Structural Expansion Feedback on Average Fuel Temperature in Case of Loss of Flow.

voiding patterns that would result from coolant voiding are relatively favorable from the viewpoint of reactivity effect, since the boiling always begins near the outlet of the core. More basic is the fact that the times required to cause any failure for these accidents are long, of the order of 5 sec or more in every case. Many scram signals due to temperature and flow should be received, and it is difficult to imagine that a control system properly designed to operate in fractions of a second will fail to scram the reactor. This conclusion is, of course, dependent on the length of time required for flow coastdown, but the designer can provide assurance of a sufficient time by the use of several primary loops and by adding inertia to the system if necessary.

REACTIVITY INSERTION ACCIDENTS

I will not discuss the \$0.20/sec insertion, which was calculated for the same cores, because the results appear very similar to the \$2/sec case, but with the time scale expanded by a factor of ten. As for the loss of coolant, the \$0.20/sec accidents allow several seconds for detection and corrective action.

Figure 4 shows clad and coolant temperatures in both the peak and average fuel channels for a reactivity insertion of \$2/sec in oxide cores. The effect of the large Doppler coefficient in increasing the time before boiling or burnout occurs is important in these cases.

Figure 5 shows the peak fuel temperatures for the same cases. The time required to reach even the upper limit assumed to indicate failure is shorter than the times required to produce boiling or burnout. Cases

FIG. 4. Clad and Coolant Temperatures of Oxide Fuel for Reactivity Insertions at \$2/sec.

FIG. 5. Peak Fuel Temperatures in Oxide Fuel for Reactivity Insertions at \$2/sec.

were run to examine the effect of reducing the radial core expansion to zero for both rates of reactivity insertion, and it was found to be small.

Inspection of the magnitudes of the various feedback reactivities experienced in these accidents showed that in all the reactivity-insertion accidents, the positive sodium coefficient assumed for the analysis had negligible effect.

Table 2 summarizes most of the results for reactivity insertions at \$2/sec. The beneficial effect of the Doppler coefficient is apparent in most cases. The gain in available time due to the Doppler effect appears to be small in some instances, as between the first two oxide cases in the hot fuel pins. However, the advantage is greater than it might appear, for the following reasons:

1. As the time available for corrective action de-

creases, small differences in this time become more important.

2. The relative difference between the first two cases is increased if one subtracts the constant delay time required to produce a scram signal. For example, if the scram signal was due to overpower at 120% of full power, about 0.1-sec delay would result in either of the two oxide cases.
3. Since the hot fuel pin actually represents an unusual combination of hot spots and peaking factors, there may be few or none of them in the core. The average fuel behavior, which shows a greater effect of Doppler, may therefore be as significant as the hottest fuel behavior in evaluating the incentives for a large Doppler coefficient.

It can be seen that failure times for the high-specific-power carbide cases are very similar to those for oxide. In both the oxide and the 300-kW/kg carbide the first failure indication is to be found in the fuel temperature. The 150-kW/kg carbide has considerably increased times before failure, due to its low operating temperature. Furthermore, the failure mechanism for the 150-kW/kg carbide cases may be coolant boiling or clad burnout, at least for the average fuel pin. For the metal fuel, within the accuracy of this analysis, all the failure mechanisms appear to be virtually simultaneous. In interpreting these results, it should be noted that in these cases flow was maintained, causing some pressurization of the core, which would delay both coolant boiling and clad burnout by perhaps a tenth of a second beyond the times shown in the table. Also, a change in the critical heat flux correlation might well result in boiling always occurring before burnout, but this would have little effect on the time of first failure, since boiling generally occurs before or shortly after burnout under the current assumption.

Although it is not shown in Table 2, an oxide case considered was one with a Doppler $T(dk/dt)$ of -0.004 , and a plot of failure time (for any given type of failure) as a function of $T(dk/dt)$ resulted in a straight line.

If failure is caused either wholly or partially by fuel temperature, it may be expected to occur in the core axial center, due to the axial power peaking factor, whereas coolant boiling and clad burnout occur near or at the outlet. If the assumption is made that resultant sodium void patterns will tend to progress from the point of initial failure, the reactivity effect of voids caused by coolant boiling or burnout might be expected to be less than if caused by failures related to high fuel temperature. The metal and lower-

TABLE 2. TWO DOLLAR/SEC ACCIDENTS

Core	$[T(dk/dt)]_{\text{Dop}}$	Hot Fuel Rods		Average Fuel Rods		Coolant Boiling, sec
		Fuel Temp Limits, sec	Burnout, sec	Fuel Temp Limits, sec	Burnout, sec	
Oxide, 150 kW/kg	-0.01	0.47-0.83	1.45	1.06-1.37	1.53	1.45
	-0.0015	0.40-0.55	0.71	0.63-0.71	0.76	0.73
Carbide, 150 kW/kg	-0.01	1.24-1.59	1.23	1.67-2.2	1.39	1.36
	-0.0015	0.61-0.76	0.61	0.76-0.94	0.67	0.67
Carbide, 300 kW/kg	-0.004	0.54-0.74	0.63	0.75-1.06	0.78	0.93
	-0.0015	0.39-0.58	0.51	0.59-0.74	0.61	0.68
Metal, 150 kW/kg	-0.004	0.85-	0.77	0.91-	0.85	0.80
	-0.0015	0.69-	0.64	0.74-	0.71	0.67

specific-power carbide cores may thus have some advantages in this respect. The safety advantages of maintaining a low fuel operation temperature in this particular type of accident might provide additional incentive for development of sodium-bonded oxide fuel; all other fuels examined in this analysis were sodium-bonded.

The magnitude and significance of the difference in void patterns are difficult or impossible to estimate at this time. The patterns of sodium voiding will depend not only on the initial failure location but also on the core radial power shape and on the rate of expulsion. The rate of expulsion is not known with confidence. With regard to the rate of radial progression, a power-flattened core with substantial local peaking factors will tend to produce a somewhat incoherent void pattern regardless of failure mechanisms. For any power shape, the lower-power channels may fail by different mechanisms (for example, coolant boiling in the oxide cores) than do the high-power channels, which further complicates the void pattern.

In all of these \$2/sec reactivity ramps, the times before failure are of the same order of magnitude as those required from the control system to operate. Although in every case it might be expected that a high power level of period scram would prevent any damage, there is little margin for failure of sensor, or other unforeseen delays. However, the credibility of a \$2/sec ramp at full-power condition should be questioned if proper design measures are taken to avoid mechanisms that might produce such a rate.

If it could be established that credible ramp rates are much less than \$2/sec, the incentive for a large Doppler to increase the time available for scram would be reduced. Other incentives, such as the role of the Doppler in the meltdown accident, might remain. If,

on the other hand, ramp rates much greater than \$2/sec are credible, the design of the control system may become very difficult, and the incentives for a large Doppler are, if anything, increased.

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Discussion

Mr. Smidt (Karlsruhe): I think in general our results agreed quite well. I only found a difference in the times to arrive at coolant boiling in the loss-of-flow accident. What assumptions did you make as regards the inertia of the whole system? Are the pumps still running with all probability, or what did you assume?

Mr. Sherer: The assumptions were not specific with regard to the inertia of the system, but we assumed it takes approximately 10 seconds for the flow to decrease to its steady state, that is, to a new steady-state value.

Mr. Lellouche (BNL): What sort of a dynamics model was this? Was it space-dependent, and fully nonlinear or linear, or what?

Mr. Sherer: It was not space-dependent; it was a point kinetics model and nonlinear.

Mr. Lellouche: You discussed failure of your elements in terms of the melting point, but in the oxides and carbides there will be mobility of fission gases, and a pressure buildup will occur as the temperature increases, which may cause pressure failure at much lower temperatures than the 4900°F or 4300°F that you indicate for melting. Did you consider this a possible means of failure?

Mr. Sherer: In the first place, these cores were operating for our specific project with the oxide or carbide close to the melting point, so that the amount of fission gas still in the fuel would not be too large. We also have some experimental evidence which indicates that for fuel which is initially operating close to the melting point the sudden release of fission gases should not cause failure as you go through the melting point.

Mr. Häfele (Karlsruhe): I have a more general remark. In connection with evaluation of the importance of the size of power coefficients, I think one finally should arrive at a point where the two consequences are envisaged; that means the power coefficient should be evaluated in a certain light. The first would be the reactivity value which could be withstood without the action of a safety system. And then one has to ask: to what extent is that amount of reactivity influenced by that and that coefficient? This is of a greater importance as regards design characteristics of a safety system, because finally the safety system has to act. Therefore, the meaning of such a power coefficient can be only that insofar as it influences the design charac-

teristics of the safety system. If one does that, one finally arrives at the point where at the reactivity rate of \$2/sec, for example, the impact of the Doppler coefficient of the design characteristics of the safety system is not too large. I think you also mentioned that, but I think it should be more explicitly the target of such an investigation. Do you tend to agree with that or do you disagree?

Mr. Sherer: I think I tend to agree with you.

Mr. Gibson (AI): As regards your high power-density carbide core, I presume you had a sodium bond. If this is true, did you have any switching mechanism to account for the possible boiling of the bond as you went through the transient with the changing in the transfer coefficients?

Mr. Sherer: No; we did not expect the temperatures of the bond at the sodium-fuel interface to exceed 3000°F. We more or less made the assumption that there might be some sodium vaporization, but the fuel element would pressurize itself to prevent boiling. I don't exactly know what sodium vapor pressure would be required to cause failure.

Mr. Campise (AI): Did you consider pressurization of your sodium system? It might be implied from your curve that if you were to pressurize the system upwards of 100 psi, you would be able to lengthen the time to boiling considerably.

Mr. Sherer: I think that's right. In fact, by use of 100 psi you can prevent boiling altogether in almost every case, but I think to pressurize to 100 psi is not an easy thing in this system. Also, as regards coolant-flow accidents, there will be some pressurization on the sodium due to the pumping, that due to the pressure drop to the core, which was not taken into account in the ramp reactivity insertion during which flow was maintained. This would increase the time required to get to the boiling point slightly.

Mr. Gast (ANL): Following the lines of Mr. Häfele's comments, do you believe that possibly this kind of a calculation could be inverted, so that if one started from some criteria for a safety system, it would be possible, in some way, to arrive at a criterion as to what the acceptable magnitude of the sodium void coefficient might be in a loss-of-coolant accident?

Mr. Sherer: I think that one could arrive at a magnitude for the Doppler coefficient to avoid the occurrence of sodium voiding. I think that the acceptable magnitude for the sodium void coefficient is very difficult to fix, because to void sodium on a scale big enough to get a significant reactivity insertion, you are probably going to melt the core anyway. So it's questionable what kind of significance you can attach to that.

Mr. Sofer (UNC): Do you happen to know the kilo-

watts per foot rating of the two carbide cases that you considered?

Mr. Sherer: I think that they would correspond to approximately 20 and 40 kW/ft.

Mr. Persiani (ANL): Did you consider the possible feedback resulting for collapse of core following melting, or did you stop at melting?

Mr. Sherer: Oh, certainly. The analysis was taken only to the point at which core failure began. No consideration was given to what the reactivity rate might be after that point. The value of \$2/sec is to be considered as related to things such as control rod runout or shootout or things of that sort.

Mr. Häfele: I would like to come back to the question asked by Mr. Gast and partly to my first remark. If one accepts the situation in which the safety system works, there will be no limit on the sodium void coefficient, and you can accept virtually all void coefficients. Therefore, from that kind of an analysis, virtually a stability analysis or an incident analysis, accepting or assuming the situation in which the safety system works, we at Karlsruhe believe that there will be no lower or upper limit for the sodium void coefficient. That will only come up in the case that the

safety system fails. In our panel discussion tomorrow we will come to that point more extensively.

Mr. Fenech (MIT): I wonder if you have studied the situation in which you lose coolant flow and the sodium superheat was at boiling; after a certain amount of superheat it would flash into vapor and introduce a step reactivity in your core.

Mr. Sherer: That would occur after either the coolant boiling or the burnout that is analyzed here. Again, therefore, the conclusion would be the same, that is, if the safety system doesn't work....

Mr. Fenech: We have done some experiments on sodium boiling which show that the sodium does not readily boil but rather flashes, and this is a rather violent effect. The work is part of the doctoral thesis of Mr. Marto, who hasn't published it yet. This is work on pool boiling, not forced circulation, but I think it would apply to forced circulation, too.

Mr. Dalle-Donne (Karlsruhe): Referring to this sodium flashing, Oak Ridge has conducted experiments, not with sodium, but with potassium. This is for forced convection, and the flashing was very violent. Not only that, my calculations indicate that the situation with sodium is even worse.

Program Proposed for Safety Studies of the Fast Neutron Project of the EURATOM-CEA Association

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I. General: Object, Limits, and Philosophy of the Program

Safety itself is no goal for research. It is sought after and achieved by a combination of well-known means such as simplicity of design, reliability of elementary components, choice of a suitable staff with good instruction, and adequate administrative procedures, whatever may be the difficulties to obtain them at the same time; and more generally by a reasonable awe with respect to accidents, common to everybody, at all stages of design, construction, or operation.

Actually, safety is nothing else but the lack of accidents. These are the real objects of the investigations. But these studies in turn would have only little interest without the positive care for safety which we dealt with and which is its true basis.

An accident may be defined as any nonqualitative or widely quantitative and nonreversible modification of some of the values which are typical of the normal operation of the reactor.

The widening of the ranges of the values involved is highly interesting, and project teams are permanently on the job. The limits of technology are ever increasingly extended. Power densities or outlet temperatures are increased. But one remains in a qualitatively unchanged field.

It is the part of the accident studies to pass over these ridges separating the normal from the inadmissible. The basic knowledge necessary for these studies is not the same as that used by project teams. In the former case the numerical results can be less accurate; conclusions must not and cannot depend that much on construction details. The accidents studied must deal with the group of reactors and not only with one of them. If the analysis would be too "sharp pointed," it would cease to be convincing. It is, by the way, very good that the safety studies may legitimately be applied to a conceptual design rather than to a Title II. Actually, the acquisition of the basic knowledge necessary to the study of accidents requires resources and a continuity in time which is difficult to

conciliate with the financial requirements and the planning of one isolated reactor.

The safety study program of the "Fast Neutron" project nowadays being examined in France illustrates well the preceding arguments.

Most of the accidents we can think of involve actually sodium boiling and/or the vaporization of the fuel. These phenomena themselves are of no interest whatsoever to calculations for the project. Their laws are complex and still badly known. On the other hand, the reasonable, although qualitative, description of the chains of accidental events is still in a very embryonic state. Everybody is harping on Bethe-Tait, that's all.

Thus a long-term double effort has to be pursued:

(i) first of all in imagination, a realistic imagination cleared up by the experience of accidents which have happened elsewhere;

(ii) the numerical knowledge of laws ruling accidental situations.

The program proposed hereafter concerns large fast-neutron reactors with sodium cooling and mixed $\text{UO}_2\text{-PuO}_2$ oxide fuel. Whatever may be the parameter changes which can be expected following the details of the design and which are important to safety, it seems to us that these reactors have enough points in common to belong to one group:

(i) considerable fuel inventories at high melt-down and vaporization temperature;

(ii) coolant whose nuclear effects are variable from one point to the other and of small amplitude;

(iii) important ratio between specific volumes of the liquid and vapor phases of the coolant;

(iv) existence of a prompt Doppler negative temperature-dependent reactivity effect;

(v) high specific power per cm^3 coolant.

The experience gathered with the safety studies of water or gas-graphite reactors is not without interest for these reactors. But it has to be kept in mind that the imaginable sequence of events is certainly qualitatively different here.

II. Investigation of Accidents

II.1. Bethe-Tait Model

A model of an accident, symbolized by the names of Bethe and Tait,⁽¹⁾ has been widely used in connection with the reactors of the first generation. It is an accident characterized by a fast and unique insertion of reactivity which ends with core disassembly under the pressures which are developed inside. It must be emphasized that the sequence of events considered in the model, in order to lead to the fuel collapse which creates the reactivity insertion, becomes more and more unrealistic when large-dimensioned reactors with flattened core are studied.⁽²⁾ Because of the essentially improbable nature of the associated events, the model has to be presented as a limiting case for all possible accidents.

But an inconceivable accident cannot be the most important.

It is, on the other hand, not at all certain that the Bethe and Tait model is not optimistic in several points: the core disassembly is a phenomenon which stops the accidental sequence in a very convenient way. Is it even sure that the most serious accident which one can imagine is that which involves the highest power?

The most valuable point of the Bethe-Tait analysis is basically the fact that no serious accident can be conceived which does not involve a partial or total core meltdown.

More detailed pictures of what happens are necessary. Three examples are given below. The events anticipated are related to the different phases: origin, evolution, and effects of the accident, but we do not claim to give a complete description of the latter in placing them side by side.

II.2. First Example: Propagation of a Fuel-pin Explosion

The fission gases kept under pressure inside the fuel pin are a potential source of energy. It may be asked under which form this energy would be liberated in the course of an ill-timed rupture of the cladding.

It has been suggested that there might exist a mechanism following which the rupture of a pin propagates to the other pins of the subassembly and even to the pins of the neighbouring subassemblies. This would have troublesome consequences on the circulation of the fuel-cooling sodium.

The study of the consequences of a cladding rupture is being tackled experimentally. The rupture could have different kinds of origins:

(a) localized flaws in the cladding, such as an already existing metal defect which might have es-

caped manufacturing control or a defect resulting from cladding corrosion in the reaction;

(b) brittleness produced by irradiation of the cladding steel;

(c) defective pin cooling, giving rise to an excessive overheating of the metal.

The tests carried out so far have dealt with RAP-SODIE-type pins. The rupture due to a localized defect a) has been simulated by weakening the cladding mechanically and by starting the rupture with the help of an electrical arc (see Fig. 1). This kind of flaw never led to an explosion at normal pressure and temperature conditions (80 bars and 550°C) (see Fig. 2). Bursting has been recorded only at clearly higher pressures (130 bars and 550°C) (see Fig. 3).

These first results lead to the belief that the available energy would be liberated progressively rather than brutally by a bursting. The study is still under way.

II.3. Second Example: Meltdown Propagation in a Fuel Subassembly

(a) One cannot guarantee that no fuel element will ever melt. In fact, it is pretty sure that some fuel subassembly will melt some day. Flow blockage between fuel pins or plugging of the fuel subassembly inlet are possible events despite any preventive measures.

(b) Such an accident is likely to remain undetected for some time. Noise-analysis detection methods are not yet of practical use. Core outlet thermocouples are not reliable enough to be sure that their readings won't be overlooked. Reactivity effects of sodium voidage and fissile material motion give reliable reactivity indications. Unfortunately they can be of too small amplitude to be detected in time.

(c) One can imagine a mechanism of propagation as follows: the melting fuel gathers over the lower axial blanket of the defective subassembly or even on the support grid of the fuel pins. The molten fuel, serving as an important source of heat, penetrates through the walls which separate it from one of the neighbouring subassemblies. Lumps, solidified by the contact with sodium circulating in the peripheral channels of the neighbouring subassembly, lead to a cooling failure of the adjacent pins, the same process leading step by step to the meltdown at the adjacent element.

This propagation mechanism may not be considered alone. One can imagine that the subassembly meltdown leads also to a plugging of its upper part and that the hexagonal cladding becomes the center of high pressures deforming it. Besides, the pressures origi-

FIG. 1 Experimental Tube for Cladding Rupture Test

nating in the molten fuel itself can take part in the propagation mechanism. It is not proved that this type of incident is no real danger even with scrambled rods.

II.4. Third Example: After-heat Cooling of a Molten Core

After-heat cooling of a subcritical molten core is in itself a big problem because of the high fission product power of large reactors. In the case of a total loss of heat sink it could lead to sodium boiling in the primary tank and to subsequent loss of tightness of the primary circuit.

Suppose that following a nuclear excursion during a meltdown the primary structure has been able to contain it within its bounds; is it clear that there won't be any fuel reassembly leading to mild power bursts? Is it clear in turn that a mild power burst cannot give rise to a slow disassembly and then to another power burst? In short, can no mechanism be conceived by which or through which energy is exchanged between fissile material and sodium, not in a steady state but in more or less repetitive transients?

III. Sodium Boiling

Future reactors will certainly operate under conditions far removed from those at which sodium boiling occurs, and some doubts may be expressed as to the necessity to assign such important funds to researches on boiling. It must, however, be admitted that a safety review of these reactors will lead to a survey of incidents apt to cause such a temperature rise that the boiling risk does exist. Although sodium boiling is not itself a primary event, it is difficult to imagine a serious accident in which this phenomenon does not take place. When it does, its consequences will always be troublesome.

Once we are convinced that safety requires sodium boiling research, it is not advisable to lay down too strictly utilitarian limitations. We better let the physicists do their work as they are used to doing it. What we lack is a basic quantitative understanding of the phenomenon. For instance, neither exclusively nor mainly do we need the laws governing sodium boiling in a coolant channel between fuel pins. Subassembly meltdown propagation requires knowledge of burnout heat flux in slab geometry or in pool boiling.

We are not so much confident in digital computer codes already written to describe two-phase flow in various geometries because their input data do not seem that reliable.

It is suggested that investigations be carried out on the following points:

- (a) Incipient local boiling in free or forced convection. The laws governing Δt_{sat} values have to be studied for various speeds, pressures, surface states, etc.
- (b) Two-phase pressure drops. S-shaped curve char-

FIG 2 Cladding Rupture Tests Weakening Brought about with an Electrical Arc The Swelling of the Pin Went Down Slowly (Test Conditions $P = 80 \text{ kg/cm}^2$ $T = 550^\circ\text{C}$)

FIG 3 Cladding Rupture Tests Bursted Pin View (Test Conditions $P = 130 \text{ kg/cm}^2$, $T = 544^\circ\text{C}$)

acteristics of the flow excursion phenomenon in different geometries have to be determined

(c) Burnout Study of heat exchange through the walls in contact with vaporizing sodium A better understanding of the flow patterns following a flow

excursion and of heat transfer before burnout is required

(d) Detection of boiling This investigation will be continued despite the fears that one may have no practicable methods Water-reactor experience is not so

FIG. 4. Induction Heated Tube (500 kHz)

FIG. 5. High-flux, High-temperature Heating Tube

FIG. 6. Pirana, a Simplified Diagram

encouraging in this field. To sum up, this program is very much inspired by the one followed in water-reactor research, but it is clear that difficulties are much greater.

Preliminary work on studies with sodium started at the end of 1963. It has been dedicated to instrumentation and to the study of heating sources. Different ways have been proposed to obtain the necessary flux:

(a) A heating device using a high-frequency current generator of 500 kHz has been tested. Already a heat flux of several hundred watts/cm² at the wall of a nickel tube of 4-mm inner diameter and a length of 40 cm (see Fig. 4) has been reached.

(b) A heating pin able to dissipate by the Joule effect a flux of 2000 W/cm² is now under construction (see Fig. 5).

(c) Electronic bombardment is being studied.

IV. Physical Data for Ceramic Fuel in Liquid and Gaseous Phases

Physical data about ceramic fuels outside the field of normal operating conditions are needed; however, no serious accident is foreseeable which would not involve meltdown and even vaporization of the fuel.

The lack of physical data hinders especially the correct evaluation of the feedback which results from the Doppler effect and shiftings of fuel under the impact of pressures of which it is the center.

1) Since the value of the Doppler coefficient in the liquid phase is not known, it has been taken as zero for accident calculations. Basic physics studies are to be undertaken.

2) The system formed by a partially vaporized core cannot be studied without a better knowledge of basic data: heat capacity, thermal conductivity, etc., even ignoring the part played by sodium under such conditions.

The possibility of using new experimental techniques for measurements of thermodynamic characteristics of uranium oxide at high temperatures is foreseen. Dynamic methods using heating by direct conduction or by laser impact in high-pressure molten systems are considered.

Whatever be the accuracy of the equation of state and of the basic data, it will remain difficult to describe mathematically the meltdown of a fuel sub-assembly owing to its complicated geometry. Experiments seem here almost unavoidable. The possibility of in-pile study of fuel meltdown should be contemplated. Nuclear heating seems indeed the only suitable way of achieving high-enough power densities to simulate correctly the meltdown phenomenon. The meltdown experiments which are taking place in TREAT reactor are being followed with interest, but it must be understood that our approach is somewhat different. We are not looking so much for power transients as for high power densities.

V. Miscellaneous Studies

Though the accurate description of an accidental sequence appears to be very difficult, a large variety of its consequences, such as involving contamination and fire, is fairly well-known. Thus it is wise to study the best way of dealing with these consequences independently of their origins. Indeed, if the reactor itself is presumed to be perfectly safe, many auxiliary and ancillary systems give rise to safety hazards that are not negligible.

V.1. The Problem of the Containment Shell

The problem of containment shells is highly explosive. The fight is hard between no-containment people and high-pressure-vapor-suppressor people. Our position is moderate.

(a) We are convinced that the primary shielding of the reactor can be designed in order to contain grossly all core accidents.

(b) It does follow necessarily that the strict requirements regarding containment shell tightness

FIG. 7. General View of Pirana

should be dropped. Actually, accidents and minor incidents may entail a release of radioactivity which would be harmful, as, for instance, in meltdown accidents during fuel handling.

(c) With respect to point a) we no longer need to design containment shells to withstand dangerously large internal pressures arising from sodium fires.

(d) Does this mean that the containment shell has to be not resistant at all? In no case whatsoever! Indeed, the simple necessities of the construction: overhead bridge crane, and shelter against snow and wind, lead to buildings which inherently offer some resistance to pressure. Furthermore, it is our experience that an appropriate tightness has a sense only if it can be periodically checked and the best inspec-

tions consist in an overpressure at a level distinctly higher than the random background due to wind and temperature. We believe that the price supplement which is paid with respect to a classical building is very low, as soon as the overpressure required is low, e.g., 50 cm of water. This solution is very close to the "control leakage" version, but it looks somewhat safer.

V.2. Radiological Safety Studies—Pirana and Fanal experiments

The decision to carry out experiments which would clarify the behavior of fission products in case of an accident with one of the reactors of the group was

FIG 8 Fanal, a Simplified Diagram

FIG 9 View in the "Fanal" Vessel Showing the Sodium Meltdown Device

E: Rupture device
M: Safety membrane
P: Pressure capture device

T: Thermocouple
V: Pneumatical valve

FIG. 10. NaK-Water Reaction Test Circuit (50 kW)

FIG. 11. General View of the Test Circuit for NaK-Water Reaction

taken at the end of 1963. Two series of studies are conducted.

(a) *Pirana Tests* (see Figures 6 and 7). These aim at knowing the nature and quantity of fission products which can pass through the sodium and escape into the upper inert gas blanket. An irradiated sample of metallic uranium is melted at the bottom of a 240-liter vessel in direct contact with the sodium contained therein. The fuel sample is a hollow cylinder with an outer diameter of 10 mm and a height of 20 mm; it is placed in a titanium melting pot and induction heated. On the vessel, devices to sample sodium are fixed at different levels for analysis. Fission gases, escaping above the free sodium level into the argon cover gas, are collected as well.

The tests already carried out with static sodium will be followed by circulating sodium experiments; the metallic uranium sample will also be replaced by a ceramic sample.

(b) *Fanal Tests* (see Figures 8 and 9). These aim at solving the problem of the escape of fission products of burning sodium, condensing of these fission products on cold surfaces, and the efficiency of the filtering.

The contaminated sodium is preheated and allowed to flow into a container where it is ignited. Sampling will take place inside the sodium, in the atmosphere, after the filters, and on the walls. Tests have been carried out so far only with uncontaminated sodium.

Sodium-Water or NaK-Water Reaction

Experiments have been conducted with single-wall steam-generators in order to check the efficiency of shutdown valves and pressure-relief devices. The test loop (see Figures 10 and 11) includes a single-tube, hair-pin, 50-kW once-through steam-generator. First tests were made with a NaK alloy because this fluid is now in use at the test station at Grand-Quevilly.⁽³⁾

A known steam or water leak in the NaK loop is created by an electric arc. At first, steam characteristics and hole cross section (about 10 mm²) are kept constant. Leak position in the generator is altered (the influence of the steam quality is thus studied). Further experiments will study the influence of geometrical parameters (type of generator, NaK cross section, group, etc . . .)

Conclusion

1. The proposed program does not intend to look into all the problems which arise from a safety review of the fast breeder conceptual design, but only to those which look to us most important. We expect that world-wide programs will be coordinated enough to cover the whole field.

2. The program is proposed for a four-year period.

3. Financial evaluation is under way. Two million dollars is not too bad a figure.

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Discussion

Mr. Stachura (AI): I would like to comment on some of the items you have brought up. You had mentioned that the Doppler of the liquid phase is perhaps not established or that sometimes it was assumed to be zero. I think the normal procedure is to assume that the Doppler is simply temperature dependent, and it doesn't matter what the phase is. For example, in the GE studies if the fuel goes beyond melting, I believe that the code simply looks at the changes in T and that $T(dk/dT)$ contribution goes in. Similarly in the Bethe-Tait-type analysis where the fuel gets up to critical temperatures and pressures, I believe the Doppler again, even though there exists a liquid or whatever phase you want to call it, is simply based on the temperature dependence. There might be some value in looking whether there is a Doppler effect on melting. If it really is simply temperature dependent, one would assume that there is none. Secondly, the problem of what happens to the fuel melting and reassembling I think is the general intention of the Bethe-Tait analysis; namely, that the fuel can reassemble at as rapid a rate as it would like. We have insertions as large as 100 or 200 dollars per second. The only mechanisms that then take place involve the equation of state to disassemble this critical mass and the Doppler effect. However, we have noticed in some recent TREAT studies another factor which could be of extreme importance. In one case where we melted uranium carbide, we found that upon cladding failure an exposure of this molten uranium carbide to the liquid sodium generated large local sodium pressures which causes gross dispersion of the fuel material, so that this may be another mechanism for fuel motion. Presumably if melting occurred at the peak portions of the core, the reactivity effects would tend to be negative under those conditions.

Mr. Denielou: In answer to the first part of your question, I must say that the Doppler effect is assumed to be zero in accident studies as soon as we are in liquid phase.

Mr. Campise (AI): In mentioning containment philosophy you implied low-pressure containment. Does this mean that you are going to a large building that is leak tight or are you essentially ignoring a

possible sodium fire in the containment building as one of your accidents?

Mr. Denielou: At present we believe that we can avoid a large escape of sodium from the primary containment. Thus we are not going to a very large building. You are right in saying that we assume we are not going to have what we call a "lake of flame" type of accident, in which the sodium burns and generates very high pressures.

Mr. Burton (Risley): Concerning the experiments in which you looked at the possibility of a propagation of a subassembly blockage by fission gas release, were you able to reach your confident position without doing experiments in sodium at the right temperature? And if so, what criterion of failure did you use?

Mr. Denielou: Are you asking whether or not we have performed in-pile experiments?

Mr. Burton: The experiments could be out of pile, but did you do them, for example, in water as one possibility or did you do them at temperature?

Mr. Denielou: Yes, with the right pressure and temperature. The next step would be to make them in sodium, but I think the latter might be avoided because the conclusion will be that a violent explosion of the pin is highly improbable by itself. If it's improbable by itself, it cannot propagate because of the difference in different situations in the pins.

Mr. Burton: Another possibility is that you'll get heat transfer effects locally when you release the fission gas.

Mr. Denielou: Local loss of cooling may result. We believe that this will have a loss-of-coolant effect if it propagates. If it's a factor of 10, we hope that this will not have bad loss-of-coolant effect. Anyway, it will be included in the overall possible causes for coolant loss, which, I say, we don't see how we can avoid. One pin exploding will surely cause some local coolant defect, but many other causes can provoke the difficulty.

Mr. Wolfe (GE): Can you tell us what accident your containment is designed for?

Mr. Denielou: Not now, because our containment is not designed.

Mr. Wolfe: Well, you mentioned studies of containment. How do you study containment if you don't know the accident or the criteria for the accident?

Mr. Denielou: We do not proceed in a very logical way. We believe that we will be able to demonstrate that the accident is contained within the primary circuit. Again, we believe that we will never be able to demonstrate that there is no fission product release or no small sodium fire. Thus we must have a tight

building first and then one that does not need to withstand a very high pressure.

Mr. Wolfe: I didn't understand whether you, for instance, postulated the Bethe-Tait accident or melt-down accident, or were going to design for it.

Mr. Denielou: No, I don't postulate it. I expostulate against the misuse of Bethe-Tait, which has people looking into the detail of every accident. We believe that some part of Bethe-Tait accidents can be contained, for instance, equivalent to 500 kg of TNT; this can be safely contained within the primary circuit with some extra care.

Mr. Charak (ANL): What is the primary circuit? Is it piping or building?

Mr. Denielou: It is concrete and in the inside you've got the piping, but it is concrete which withstands the pressure.

Mr. Charak: There is no air?

Mr. Denielou: There is inert gas within the containment we are speaking about, but there is air in the building itself.

Mr. Charak: And you preclude fires in the containment but not in the building?

Mr. Denielou: Yes.

Loss-of-Coolant Accidents in a Large Sodium-Cooled Fast Reactor

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(PRESENTED BY E. HICKS)

1. INTRODUCTION

This paper will explore the possibilities of analysing fast reactor accidents more realistically than has been attempted before.

The safety of a large sodium-cooled ceramic-fuelled fast reactor against overheating accidents depends on guarding against three potentially dangerous factors. Firstly, as in all fast reactors, the fuel is not arranged in its most reactive configuration, so that in an accident involving collapse of the core structure, the fuel could be rearranged to become prompt critical. Secondly, the sodium void coefficient of reactivity is positive over the central region of the core, so that it may be possible to achieve prompt criticality by losing coolant from part of the core, for example by local boiling. Thirdly, the fuel operates at very high temperature, which in the case of oxide may exceed 2000°C, so that if the core does collapse, allowing hot fuel and coolant to come in contact, there is a possibility of explosive vapourisation.

In addition, safety may be affected by the details of the design of the specific reactor. For instance, the fuel cans may be pressurised by the buildup of fission-product gas so that if a can is broken, this gas is released into the coolant stream to cause overheating and bursting of other cans.

During the course of its life the reactor may be subject to unscheduled perturbations of various degrees of severity, such as reductions or blockages of coolant flow, affecting all or part of the core, bursting of faulty pressurised fuel pins, or accidental reactivity additions. It is necessary to know whether such an incident can bring into play one of the more dangerous mechanisms mentioned above, and so turn into a major accident.

It is relatively simple to calculate the effects of a flow or reactivity perturbation if the integrity of the fuel is maintained, and there are no phase changes (i.e., the fuel or cans do not melt and the coolant does not boil). At the other end of the scale, it is possible to do explosion calculations to estimate the severity of a prompt-critical excursion when the whole of the core structure collapses. This paper attempts to fill the

gap between these two types of calculations by following specific accidents as they develop beyond the point at which the coolant boils or the fuel collapses, and to predict in a realistic way the consequences of these accidents.

Work has been done on many of the individual mechanisms which are important in the development of such an accident. In this paper we try to gather these results together to present as coherent a picture as possible of the safety of the reactor, within the limitations of our method. Of course, we are unable to predict exactly what happens in every accident, but the exercise does enable us to see more clearly what are the important points in the design for the safety of the reactor, and also to see where our knowledge is most seriously lacking.

2. SYNOPSIS

Summary of Method

Basically the approach used here is to consider a series of fault conditions which might arise in a typical oxide-fuelled, sodium-cooled fast reactor. The accidents resulting from these faults are then analysed step-by-step, usually under some pessimistic assumptions, such as the failure of one or more reactor trip circuits.

There are, however, some mechanisms which are important for several of the accidents and require detailed consideration. Section three therefore is devoted to general discussions of certain aspects of failure, including:

- coolant and fuel reactivity worth;
- the behaviour of the coolant as it boils, and particularly the effects of nucleate boiling;
- the release of fission-product gas from a burst fuel can.

In Section four these results are applied to the analysis of various accidents which are initiated by faults affecting either a single subassembly or the whole core. Because of the greater difficulty in detecting a single subassembly failure, these accidents are considered in greater detail. The initial faults are:

TABLE 1. REACTOR MODEL

	Dimensions (cm)		
Core Height	90 cm		
Core Diameter	140 cm		
Diameter of Inner Enrichment Zone	92 cm		
Blanket Thickness:			
Axial	23 cm		
Radial	55 cm		
	Composition (v/o)		
	Fuel	Stainless Steel	Coolant
Core and Axial Blankets	36	21	43
Radial Blanket	57	22	21

Core Fuel is UO_2/PuO_2 , Overall Density: 8.8 g/cm³
 Enrichment $[\text{Pu}/(\text{U} + \text{Pu})]$ { Inner Zone: 15.3%
 Outer Zone: 20.4%
 Plutonium Isotopic Composition:
 239 80%
 240 16.8%
 241 2.8%
 242 0.4%

restriction of coolant flow in one subassembly;
 sudden complete failure of coolant flow in one subassembly;
 local coolant blockage affecting a small amount of the fuel in one subassembly.
 We also consider whole-core accidents initiated by: coolant-circulating pump failure, and sudden complete coolant flow failure occurring while the reactor is operating at power.

As a result of these analyses, certain problems stand out as being particularly important for the safety of the reactor, and these are discussed in Section five. They include the possibility of a single subassembly accident spreading to the whole core, and the time available for a trip mechanism to operate. Suggestions for future experimental and theoretical work are then made.

Limitations of Method

It is important to be quite clear as to exactly what can be accomplished by this approach. As a result of the limitations imposed by the use of the present method (which is to follow the development of specific accidents), we can expect to reach conclusions of the following types:

(i) We can place bounds on the severity of various transients (e.g., we can say that a certain accident cannot give more than a certain reactivity increase, or

give rise to pressure pulses of more than a certain severity or duration).

(ii) We can pinpoint sensitive features of the design for safety (e.g., we may be able to show that if the rate of release of fission-product gas from a burst fuel can is prevented from exceeding certain value, failure of other cans is impossible).

(iii) We may be able to suggest areas where the present design is overcautious (e.g., we may be able to show that the margin between operating coolant temperature and can-failure temperature is unnecessarily wide).

(iv) We can gain increased confidence that no important dangers have been overlooked.

It is equally important to be clear as to what cannot be achieved. The two main limitations are:

(i) An accident is so complicated that we cannot hope to calculate exactly what happens. Instead, we have to concentrate on refining the limits we place on its severity.

(ii) We cannot *prove* that we have overlooked nothing, so there is always the possibility of a dangerous failure mechanism going unnoticed. Nevertheless, the more we study the problem, the greater is our confidence.

3. PRELIMINARY CALCULATIONS

In this section we discuss in general terms certain aspects of reactor failure to establish results for use in the analysis of specific accidents.

Reactivity Worth of Coolant and Fuel

Reactor Model

The reactor model which was used for reactivity calculations is described in outline in Table 1. It is a typical mixed oxide-fuelled fast reactor producing 600 MW (thermal), in the design of which no special steps have been taken to reduce the positive sodium void coefficient of reactivity. There are two radial fuel-enrichment zones to flatten the power distribution, so that the rating of the fuel at the inner edge of the outer zone is the same as that at the centre of the core.

The core is taken to consist of hexagonal subassemblies, each about 14 cm across flats. The inner and outer enrichment zones are of roughly equal volumes. Six subassembly positions in the outer part of the inner enrichment zone are occupied by control rods. For the purpose of this study, we ignore the differences in radius from the core axis between different subassemblies in one hexagonal row, so that all the subassemblies of a row are taken to have the same power rating and reactivity worths. The central seven subassemblies

TABLE 2. REACTIVITY CHANGES DUE TO LOSS OF SODIUM FROM SINGLE SUBASSEMBLIES

Subassembly row	1	2	3	4	5
Number of core subassemblies in row	7	12	12	24	24
Power developed relative to central subassembly	1.00	0.95	0.88	0.99	0.90
Reactivity worth of complete subassembly (Δk , %)	0.87	0.78	0.68	0.81	0.55
Reactivity change due to loss of sodium from core section of one subassembly (%)	0.0198	0.0161	0.0092	-0.0005	-0.0199
Reactivity change due to loss of sodium from one axial blanket section (%)	-0.0026	-0.0024	-0.0024	-0.0023	-0.0018
Maximum positive reactivity due to loss of sodium from centre part of subassembly (%)	0.0218	0.0182	0.0118	0.0041	0

We take the delayed-neutron fraction to be 0.3%

FIG. 1. Sodium Void Coefficient of Reactivity

are treated together as row 1. The number of subassemblies in each row and their power rating are shown in Table 2.

Sodium Void Coefficient

The reactivity worth of loss of sodium was calculated by means of the CRAM diffusion-theory code⁽¹⁾ with the FD 1 data set.⁽²⁾ Perturbation theory was used to find the worth of sodium at each mesh point, the mesh having been chosen to coincide with the rows of subassemblies. The results are given in Figure 1, which shows the increase in reactivity due to the loss of 1 cm³ of sodium of density 0.833 g/cm³ (correspond-

ing to a temperature of 500°C) from a point on the axis of a subassembly of each row, as a function of axial position. Table 2 also shows the reactivity increase due to loss of sodium from the core and axial blanket parts of one subassembly from each row, and also the maximum positive reactivity obtained by loss of sodium from the central part of a subassembly where the void coefficient is positive.

Fuel Movement

The reactivity effect of fuel movement in one subassembly has been estimated by first-order perturbation theory. If the fuel in a central subassembly melts,

FIG. 2. Flow/Pressure Drop Characteristics for a Subassembly of Row 1

moves to the centre of the subassembly, and is compacted at its maximum theoretical density of 11.0 g/cm^3 , there is a reactivity increase of 0.16%, or about 56 cents, assuming that the axial blanket does not move. This estimate is not valid if the core undergoes a gross perturbation and can only be used for single subassembly accidents. For whole-core accidents in which the fuel in several subassemblies collapses, we have confined our attention to reactivity changes due to coolant loss, leaving a complete study for more detailed treatment.

Doppler Effect

The Doppler coefficient for the whole core is $-4.6 \times 10^{-6} \Delta k/^\circ\text{C}$ at a mean fuel temperature of 1000°C , while the coefficient for heating row 1 alone is $-0.72 \times 10^{-6} \Delta k/^\circ\text{C}$. We assume the coefficient for heating the central subassembly is one-seventh of this, i.e., $-0.10 \times 10^{-6} \Delta k/^\circ\text{C}$. The coefficient is taken to be inversely proportional to the mean fuel temperature.

Coolant Behaviour

Steady-state Pressure Drop

The relationship between the coolant flowrate in the central subassembly and the steady-state pressure drop across it is shown in Figure 2, taking the core inlet temperature to be 400°C and the temperature rise to be 200°C . We have assumed that vapour is formed as soon as the sodium reaches its saturation temperature (the effects of superheating and nucleate boiling are discussed below).

While the coolant at the subassembly outlet is sub-cooled, the pressure drop is substantially independent of the heating rate, and decreases as the flow decreases and the outlet temperature rises. When the outlet temperature reaches the saturation temperature corresponding to the outlet pressure, boiling starts in the top of the subassembly. The large volume change on boiling gives high velocities in the boiling region, and a correspondingly large pressure drop due to friction and momentum increase, so that the pressure drop needed to maintain the flow increases rapidly as the flow decreases. The curves of Figure 2 were derived by the method described in detail by Stein,⁽³⁾ which is based on empirical correlations of two-phase flow data from experiments using water.

The effect of a partial blockage in the subassembly is to introduce an extra flow-dependent pressure drop which reduces the pressure drop available to maintain the flow in the manner shown in Figure 3. The reduced flow is now given by the intersection of the subassembly flow characteristic curve and the reduced pressure-drop curve (for example, *X* in Figure 3). As the blockage increases, the flow decreases until a point *Y* is reached. Here, boiling has started at the top of the subassembly in the axial blanket region.

The condition represented by *Y* is, however, unstable, and a slight increase in the blockage gives a large change in flow as conditions jump to *Z*, where the flow is very small and most of the subassembly is filled with a mixture of liquid and vapour. This jump is known variously as a "flow excursion" or "choking." The nature of this flow excursion is not certain: it

FIG. 3. Flow Excursions in a Subassembly of Row 1

cannot be instantaneous, as it is accompanied by a considerable rise in temperature of the fuel, and it may not be a smooth transition, especially if significant superheating of the liquid sodium takes place.

Nucleate Boiling

When a liquid boils on a heated surface, vapourisation is prevented by surface tension until the liquid temperature exceeds the saturation temperature corresponding to the ambient pressure. Vapour bubbles grow from nucleation sites, which may be envisaged as being small pits in the surface containing nuclei of trapped gas or vapour. Such a nucleus can only grow when the liquid temperature exceeds the saturation temperature by an amount (the superheat) depending on the tension in the curved surface of the trapped bubbles (and so on the radius of the site) and also on the ambient temperature. Water normally requires a superheat of 5–10°C when boiling on a normal engineering surface, but there is experimental evidence to suggest that liquid metals require superheats as high as 400°C to initiate nucleation.

Figure 4 shows the superheat required to make a bubble grow as a function of the nucleation site radius for sodium at a pressure of 1.7 atm, at which the saturation temperature is 944°C. It is based on the assumption that the surface tension falls linearly with temperature to zero at the critical temperature, which is taken as 2000°C for sodium. This value may be in error by 100°C or more, so that the high-temperature

part of the curve for nucleation site radius less than 10^{-6} cm may be in error.

The actual superheat which occurs depends on the radius of the largest pits in the surface which are retaining bubbles of gas or vapour and so acting as nucleation sites. This, in turn, depends on the previous history of the surface and the liquid, and also on the cleanliness of the system. If the system is very clean, bubbles are retained only by the smaller pits, so that while water boils typically with a superheat of up to 10°C, corresponding to a nucleation site radius of about 2×10^{-4} cm, superheats in excess of 100°C have been observed under very clean conditions, corresponding to radii of 10^{-5} cm or less. Also, if the system is particularly free from dissolved or adsorbed gas (as may be the case in a reactor after long periods of operation), few trapped gas bubbles will survive to form nucleation sites. Hoffman and Krakoviak⁽⁴⁾ report superheats in the range from 150 to 390°C, depending on pressure, for potassium boiling in "as received" type 347 stainless steel tube. These results correspond to a site radius of about 4×10^{-5} cm. Thus it is likely that site radii in the range from 10^{-4} to 10^{-5} cm are appropriate for sodium boiling on normal engineering surfaces, corresponding to superheats of up to 300°C. Moreover, making the system cleaner tends to increase the superheat.

The expansion of a bubble growing from a nucleation site is governed by the distribution of temperature and pressure in the moving liquid surrounding it. The problem cannot be solved analytically, and so far no

FIG. 4. Superheat and Nucleation Site Radius for Sodium Boiling at 1.7 Atm

accurate numerical solution has been attempted, but an approximate estimate of the pressure transient suggests that the pressure at a point 0.2 cm from the centre of an expanding bubble reaches a peak of about 10 atm in a pulse lasting for about 100 μ s, if the nucleation site radius is 10^{-5} cm (i.e., the liquid sodium is superheated by 354° to about 1300°C). For a nucleation site radius of 10^{-6} cm (sodium at 1670°C), the peak pressure is about 40 atm, and the pulse lasts about 40 μ s. These pressures are only rough estimates, but show that mechanical damage due to rapid expansion of bubbles in superheated liquid is unlikely.

Coolant Ejection and Re-entry

If the coolant flow in the central subassembly is suddenly stopped by complete blockage of the inlet, it is shown below that saturation is first reached just above the centre about 0.6 sec after the flow stops. If the liquid sodium in the subassembly above this point is assumed to move as a rigid body, the forces acting on it and the rate at which it is ejected can be found. The motion of the liquid-vapor interface is shown in Figure 5, assuming various amounts of superheating occur before vapour is produced. The top of the subassembly is voided in about 0.16 sec if there is no superheat. This is an oversimplified picture of what takes place, but it serves to indicate the time scale for coolant expulsion by boiling.

The liquid expelled from the top of the subassembly is followed by a stream of mixed vapour and liquid, possibly containing fission-product gas as well, which

continues until most of the sodium has been lost from the subassembly. After this, the liquid sodium falls back into the subassembly and comes into contact again with the fuel which is now above the saturation temperature. Vapour is generated and the liquid blown out again. This process may be repeated in a cyclic manner, as observed for instance by Huebner⁽⁵⁾ in experiments on a water model of part of the Enrico Fermi core. He reports a cyclic "chugging" motion with a period in the range from 0.2 to 0.5 sec, but does not give details of the pressures generated.

It is not clear how much sodium enters the subassembly in a "chugging" cycle of this type, but a rough estimate based on the correlation of limiting countercurrent liquid and gas flows due to Wallis⁽⁶⁾ and assuming coolant and fuel exchange heat through a 0.01-cm vapour layer, suggests that the coolant falls back in to occupy about 30% of the core section of the subassembly before it is ejected again.

Fuel-pin Behaviour

Temperature Distribution in the Fuel

If the coolant flow in a subassembly is suddenly stopped by a blockage of the inlet, the temperature of the coolant and fuel rises. The temperature distribution in a single fuel pin and its associated coolant can be calculated analytically as a function of time after the flow stops. The variation of temperature in a fuel pin at the centre of the reactor is shown in Figure 6, assuming constant reactor power, for an

FIG. 5. Expulsion of Sodium from a Blocked Subassembly

oxide fuel pin of 0.25-cm radius, clad in stainless steel 0.04 cm. thick, with a fuel heat rating of 200 W/g. It will be seen that the coolant reaches its boiling point after 0.65 sec, and the can starts to melt after a further 0.51 sec.

Explosive Vapourisation of the Coolant

Interest in explosive release of energy by rapid vapourisation of the coolant as it comes into intimate contact with very hot fuel stems from the last of a series of transient tests with the SPERT-1 reactor, which are described by Miller *et al.*⁽⁷⁾ In this test, a light-water-moderated thermal reactor with highly enriched uranium-aluminum alloy fuel in the form of plates was subjected to a severe super-prompt-critical excursion. The nuclear excursion was terminated by expansion and boiling of the moderator, but 15 ms later an explosion occurred, giving pressures of 3–4000 psi, destroying the reactor. The evidence suggests that the nuclear power was negligible when the explosion occurred.

It is suggested, although it is not certain, that the explosion was due to water coming in contact with molten fuel at a high temperature and in a very finely divided form. Heat was transferred very rapidly to the water, giving rise to high vapour pressures before the pressure could be relieved by expansion and the heat transfer rate reduced by the formation of a vapour blanket.

Until the SPERT results are adequately explained, it is impossible to say whether a similar explosion

FIG. 6. Temperature Distribution in a Fuel Pin and Coolant after a Sudden Complete Flow Failure.

could take place in a different type of reactor. Nevertheless, even if the mechanism outlined above is not a full explanation of the SPERT results, it might in a

FIG. 7. Discharge of Fission-product Gas from Burst Fuel Pin

modified form give the rapid energy release in an oxide-fuelled fast reactor.

The situation envisaged is one where a subassembly, say, has been voided of coolant long enough for most of the fuel to reach a temperature near vapourisation, and also the fuel has broken into small pieces. The sodium then falls back in, becomes intimately mixed with the hot fuel, is heated very rapidly, and expands explosively. For this to happen, a large fraction of the fuel must be in a finely divided form, the sodium must be mixed with it intimately and very rapidly in droplets which are small enough for their temperature to rise close to that of the fuel before they have time to expand and relieve the pressure, and mixing must take place in the right proportion to maximise the amount of work available.

Further consideration of this process is beyond the scope of this paper; a more detailed study is presented by Hicks.⁽⁸⁾ It must be noted that although such an incident appears to be very unlikely, it cannot at this stage be ruled out as impossible for an explosion to occur in this manner, which could seriously distort the rest of the core and prevent shut-down of the reactor.

Release of Fission-product Gas

On irradiation to 10% burnup, each gram of fuel can release up to about 12 mg of fission-product gas. If

the fuel pin is pressurised, and a volume equal to the fuel volume is available to accommodate this gas at the sodium inlet temperature of 400°C, it raises the pressure in the fuel pin to 45 atm (assuming it has the properties of xenon).

If for some reason the can is punctured, this gas escapes at a rate limited by sonic choking of the flow through the hole, until the pressure in the pin falls to about 3 atm. Figure 7 shows the initial rate at which gas is released as a function of the area of the hole, and also the time taken for the pressure to fall to 3 atm, assuming the fuel in the can offers no resistance to the flow of gas from the storage space to the hole. Two values for the temperature of the gas as it reaches the hole, 400°C and 2000°C, were taken, corresponding to the gas either taking up the maximum fuel temperature, or receiving no additional heat as it flows from the storage space.

Cascade Failure

It is possible that gas released from one broken pin might cause overheating of an adjacent pin, so that it bursts and releases more gas, causing failure of a third pin, and so on through the subassembly. If a fuel pin is completely blanketed by gas, so that it loses no heat, it takes the can at least 0.6 sec to reach the melting point of 1250°C. Thus it is clear from Figure 7 that for a failure to spread in this way, bursting of the first pin must give a hole of area less than $5.4 \times 10^{-3} \text{ cm}^2$ (at 400°C) or of 10^{-2} cm^2 (at 2000°C) if the discharge is to last long enough to cause failure of another pin.

In addition to this upper limit on the size of the hole, it is possible to estimate a lower limit. It is difficult to see how the stream of gas can cause significant overheating of the adjacent pin if it occupies less than about 0.01 cm² of the coolant flow area, which is equivalent to a stream of bubbles of about 0.1-cm radius. This bubble stream cannot move more slowly than the bulk coolant velocity of about 1000 cm/sec, which implies that the minimum rate of release of gas to cause damage to an adjacent pin is about 0.04 g/sec. Figure 7 shows that this requires a hole of area greater than $2.5 \times 10^{-5} \text{ cm}^2$ (at 400°C) or of $4.7 \times 10^{-5} \text{ cm}^2$ (at 2000°C). The details of this argument may be open to criticism, but it is clear that some sort of lower limit can be arrived at in this way.

Thus, although we cannot rule out the possibility of a cascade failure of this type, it seems to be very unlikely. It needs all the fuel pins to fail, giving holes of which the areas fall in a narrow range, and it is inhibited by the fact that when one pin bursts, the jet of gas coming from it will be directed mainly towards the pins which have already failed.

4. ANALYSIS OF SPECIFIC ACCIDENTS

Single-subassembly Accidents

Conditions to Cause Accidents

An overheating accident affecting initially a single subassembly can only be caused by a reduction of the coolant flow. To cover the range of possibilities we consider the following situations. First, we take the case of a reduction of the coolant flow due to a blockage at the subassembly inlet, which gradually increases in severity due to, say, accumulation of some sort of debris. Secondly, we consider sudden complete blockage of the coolant inlet, so that the flow stops instantaneously. The inlet would be designed to prevent this, but it does represent a worst possible flow failure. Thirdly, we have a local blockage, caused, for example, by accumulation of debris between the fuel pins, causing local flow reduction severe enough to make one or two pins fail.

In all cases we assume the accident happens to the central subassembly, as the reactivity changes due to fuel and coolant movement are most severe, and the power density highest, at the core centre.

Partial Blockage of the Central Subassembly

As the coolant flow is restricted, the subassembly outlet temperature rises until boiling starts in the axial blanket region. The bubbles of vapour released collapse violently in the subcooled sodium above the core. The flow in the subassembly in this situation is represented by the point Y in Figure 3.

Further increase of the blockage causes a flow excursion to Z in Figure 3, as explained above. The distribution of vapour and the temperature profile before and after the excursion are shown in Figure 8. The reactivity increase due to loss of sodium in the excursion is 5.5 cents. The nature of the transition is not certain, and it may not take place smoothly, especially if significant superheating of the liquid sodium takes place. As pointed out above, however, the pressures due to bubble growth in superheated sodium are unlikely to be severe enough to cause mechanical damage. There may be rapid reactivity fluctuations as the coolant is thrown about, but a maximum positive reactivity of 7.3 cents cannot be exceeded. The efforts of such reactivity changes on the reactor power on the time scale of the accident is negligible.

If the fuel pins are pressurised and near the end of their life, they may be weakened by irradiation and fail due to the fission-product gas pressure before the sodium boils. The consequent release of fission-product gas may interrupt the coolant flow, but this is unlikely to have a significant effect. If the coolant flow is to

FIG. 8. Vapour and Temperature Distribution in a Subassembly before and after a Flow Excursion.

be stopped, the gas must raise the pressure at the top of the subassembly above the inlet pressure, in which case the gas itself will be blown out of the subassembly at the sonic velocity of about 3×10^4 cm/sec. This would allow all the gas stored in the fuel pins to be discharged in less than 0.1 sec, so this is the maximum time for which the coolant flow can be interrupted. During this time the can surface temperature could only increase by 80°C .

Within a second of the flow excursion taking place, the fuel cans at the top of the core will melt, as indicated in Figure 8, allowing the pins to collapse, and releasing any remaining fission-product gas. The reactivity change due to the Doppler effect up to the time the fuel pins collapse is about -0.5 cent. It is impossible to follow the progress of the accident in detail beyond this point. The maximum reactivity increase if all the fuel should collapse to the centre of the subassembly is 56 cents. It may be, however, that the debris of broken fuel and molten cans will be blown upwards by the sodium vapour stream to block the subassembly outlet. This, in turn, may cause expulsion of vapour downwards below the core.

If all the subassembly power goes into boiling so-

Fig. 9. Reactivity Changes due to Coolant Movement and Doppler Effect on Sudden Complete Blockage of a Single Subassembly

dium, enough vapour is produced to fill the subassembly in 7 ms. Such a large rate of vapour production can only continue for a few seconds, and a "chugging" motion, with liquid coolant entering the core region and being blown rapidly out again in a cyclic manner (as described above), must soon supervene.

If vapour gets below the core, there is a danger that it will enter other hitherto unaffected subassemblies, causing a positive reactivity transient, and maybe more melting. Against this, due to the high conductivity of sodium, any vapour entering the cold region below the core will condense quickly. It is not possible to analyse this situation without knowing how rapidly vapour can be generated in a "chugging" state. If vapour fills the six subassemblies adjacent to the one initially affected from the bottom upwards, the reactivity increases by a further 36 cents.

Sudden Complete Blockage of the Central Subassembly

After the coolant flow stops, the temperature in all the fuel pins except those nearest the wrapper rise, as shown in Figure 6. The coolant reaches saturation 0.6 sec after the blockage occurs, and the liquid sodium is expelled from the top of the subassembly in a further 0.16 sec. Loss of sodium from the top of a central subassembly gives a peak positive reactivity of 3.6 cents and a net increase (when the upper axial blanket is voided) of 2.4 cents.

After this, most of the rest of the coolant in the core region of the subassembly is ejected and then "chug-

ging" takes place as described above, probably with a period of 0.2–0.5 sec and with the coolant re-occupying about a third of the subassembly at most on each cycle. The maximum possible reactivity increase due to sodium loss from the centre part of the subassembly is 7.3 cents. The reactivity change due to the Doppler effect up to the time the cans melt is –0.3 cent.

This cyclic motion continues until the fuel pins collapse when the cans melt about 1.2 sec after the initial flow failure. The maximum possible reactivity increase if the fuel melts and collapses to the centre of the subassembly is 56 cents. The stream of sodium vapour leaving the subassembly may, however, blow the fuel away from the core centre until it is stopped by the cooler axial blanket which does not collapse so readily.

Figure 9 shows the reactivity changes due to coolant movement and Doppler effect for this accident. It will be seen that these are small compared with the possible effect of fuel movement.

The subsequent course of the accident is similar to that of the partial-blockage accident described above. It is impossible for a high pressure to be maintained by reaction against material being ejected from the top of the subassembly for long enough to force vapour downwards against the inlet pressure, and so to get beneath the core and enter other subassemblies. Only if the top of the subassembly becomes blocked with fuel pin debris would this be possible.

Local Flow Blockage

In the case of blockage of a small number of the coolant flow channels, can failure may be caused either

by coolant boiling or by bursting under fission-product gas pressure.

The possibility of the latter type of failure spreading to the other fuel pins purely by fission product gas blanketing is discussed above: it is only possible if the burst can release gas at a rate which falls in a limited range. Cascade failure of a large number of pins is only possible if each succeeding pin releases gas at a correspondingly limited rate. Gross melting of the fuel pins in the subassembly can only result from such a cascade failure if the cans are deprived of coolant for about 0.6 sec. Although such a cascade failure appears to be very unlikely because it requires the pins to fail in a very restricted way, it cannot be ruled out as impossible.

If the cans are not pressurised, or do not burst at low temperature, failure may be started if the initial blockage is large enough to cause the coolant to boil. If this causes the cans to melt, fuel at a temperature above 1400°C will be exposed, and vapour will be generated rapidly. It seems unlikely that this vapour will cause failure of other pins, however, as the bulk of the sodium flow in the subassembly is still at least 350°C below the saturation temperature, so that any vapour bubbles which leave the overheated channels—that is, the channels affected by the blockage—will collapse very rapidly. If the blockage is so severe that vapour is generated fast enough to interrupt the whole subassembly coolant flow, “chugging” will take place, as described above.

Whole-core Accidents

Conditions to Cause Accidents

A whole-core overheating accident which is of some interest is that caused by failure of the electrical supply to the coolant-circulating pumps. We consider this accident below, assuming that the trip mechanisms do not function and the reactor power remains constant. Also, to represent a worst possible whole-core coolant accident, we consider the case of the inlet pressure falling suddenly to zero, due, for example, to a sudden large breach in the bottom of the reactor vessel.

A whole-core overheating accident can also be caused by an increase in reactor power. This could be due, for instance, to the insertion of reactivity at a steady rate by a faulty control rod mechanism, giving a slow, uncontrolled power increase. The consequences of this are very similar to those of the pump-failure accident, and we do not consider it in detail.

As explained above, we do not consider here the reactivity changes in whole-core accidents due to fuel movement, as these cannot be regarded as being given by small perturbations of the reactor. We confine our attention to the reactivity effects of coolant loss.

Coolant-pump Failure

If the coolant pumps fail and the reactor is not tripped, the coolant flow falls as the inlet pressure falls until the highest rated subassemblies reach a point corresponding to the minimum of the full-power curve in Figure 3. A flow excursion now takes place in these subassemblies, but as the inlet pressure is low, the flow after the excursion is very low, corresponding to the point *Z'* in Figure 3, and almost all the core and upper axial blanket are filled with vapour.

If we assume that the flow halves in 10 sec, in the manner indicated in Figure 10, we can find the time at which each row of subassemblies is subject to a flow excursion. This happens in the central subassembly after 17.2 sec, and so on as indicated in Figure 10, until the whole core and upper axial blanket are voided of sodium. We assume the flow decay characteristics are unaffected by the accumulation of vapor in the coolant circuit.

We estimate the reactivity effect of coolant vapourisation in a simple way as follows. We assume that a flow excursion involves loss of coolant from the top axial blanket region of the subassembly in 0.1 sec and from the core region in a further 1.0 sec, and that the reactivity changes given by these stages are made as smooth ramps. Using now the reactivity values given in Table 2, we can obtain the overall reactivity change due to coolant vapourisation. This is shown in Figure 10. It will be seen that after the flow excursion occurs in row 2, a peak reactivity of more than 50 cents is reached for half a second, but after the excursion in row 5, the reactivity stays negative. It is important to note that the time scale for this accident is much longer than for any of the other accidents we have considered.

Sudden Complete Whole-core Flow Failure

The consequences of this accident for each individual subassembly are very similar to those of the sudden complete blockage discussed above. The coolant temperature rises as shown in Figure 6 and is expelled in the manner indicated in Figure 5, only in this case, as the inlet pressure is low, the coolant is expelled in both directions.

Figure 5 applies to the central subassembly, but we assume that it can be used for other subassemblies by increasing the time scale in inverse proportion to the power rating. Making this assumption and using the values of sodium void coefficient of Figure 1, we can find the reactivity transient due to sodium loss in this accident. This is shown in Figure 11. It will be seen that the reactivity reaches a positive peak of 70 cents and stays positive for less than 0.2 sec, after which it is strongly negative.

FIG. 10. Reactivity Changes due to Loss of Coolant in a Pump-Failure Accident

FIG. 11. Reactivity Changes due to Loss of Coolant after a Sudden Complete Flow Failure.

In this case, as can melting and collapse of the fuel cannot occur until 1.2 sec after the flow failure, there is no possibility of reactivity loss due to fuel movement reducing the peak reactivity due to sodium loss.

5. CONCLUSIONS

Positive Sodium Void Coefficient

Maximum Reactivity due to Loss of Sodium

For the reactor described here, the maximum positive reactivity that can be reached by voiding sodium from the centre of one subassembly is 7 cents (see Table 2). Even allowing for inaccuracy in the data used in our calculation of the void coefficient, it is clear that loss of sodium will not in itself give a serious reactivity increase in an accident which is confined to a single subassembly.

The maximum positive reactivity that can be reached by voiding just the part of the whole core for which the void coefficient is positive, is nearly \$2. It might be suggested that for this reason such a reactor design is undesirable and should be altered to reduce this maximum reactivity. This may be economically undesirable, as making the sodium void coefficient more negative may impair the breeding properties of the system and may also increase its engineering complexity.

Our analysis of specific accidents suggests, however, that it is not necessary to consider this maximum possible reactivity as a criterion for assessing the significance of a positive sodium void coefficient. The greatest positive reactivity due to sodium loss which can be reached in the more realistic, but still very pessimistic, accident conditions we have considered is \$0.7. We suggest that an analysis of this type gives a

less restrictive, and yet still adequately cautious, criterion for assessing a positive sodium void coefficient. It also has the advantage of showing clearly the conditions necessary for such an accident to take place, so giving a better idea of its probability.

Control of the Maximum Reactivity

The severity of the reactivity addition in these accidents is reduced in two ways. Firstly, when boiling occurs, coolant is lost very rapidly from the whole length of a subassembly, so that the positive reactivity contribution from the centre is rapidly followed by a negative contribution from the ends. Secondly, the high power density in the outer enrichment zone of the core ensures that a negative effect due to voiding the outer subassemblies comes very soon after the positive effect from those near the core centre.

This may afford a useful means of controlling the effect of the positive sodium void coefficient, instead of altering the design of the reactor to reduce the coefficient and incurring economic penalties. If the power density in the outer zone is made such that in a whole-core accident boiling occurs first in the outer region of the core, the reactivity transient due to loss of sodium could be made to start with a negative change, and the positive reactivity peak reduced or eliminated.

It should be noted that these conclusions are based on the above analysis of whole-core accidents, which take no account of reactivity changes due to movement of coolant after the first expulsion from a subassembly or due to collapse of the fuel. We are not able to assess the reactivity effect of, for instance, the re-entry of sodium into the core, and our conclusions may be radically changed if this is found to be important.

Pressurised Fuel Pins

If the fuel pins are sealed to prevent the escape of fission-product gas, the increasing pressure in the pins and the decreasing strength of the cans under irradiation will cause the cans to burst before the melting point of steel is reached, and possibly even before the sodium boils. Only in the latter case, however, is there a possibility of a significant effect on the safety of the reactor, because if the coolant boils, the fuel will almost certainly collapse by melting if not bursting of the cans.

The possibility of the bursting of a single faulty pin causing cascade failure of all the pins in a subassembly has been discussed above. Although this cannot be dismissed as being impossible, it is very unlikely, as it depends on all the pins bursting in a restricted manner.

Apart from this, qualitatively the effects of an

accident in which the cans burst and release fission-product gas rapidly are very similar to those of an accident in which the cans are intact until the coolant boils. In either case the subsequent course of the accident depends on the interruption of the coolant flow and the expulsion of coolant from the subassembly. The main difference is that failure occurs earlier if the cans burst below the coolant boiling point, so that, for example, a partial blockage of a single subassembly has to be less severe to cause a flow excursion, and in the case of the coolant-pump failure less time elapses before flow excursions occur in the highest rated subassemblies.

Superheating of Liquid Sodium

It is not clear whether liquid sodium can be superheated well above the saturation temperature in the manner described above in an operating reactor or not. If it can, there are two effects on the safety of the system. Firstly, the formation of vapour is delayed until a higher temperature is reached. This is an advantage, as for example it may enable a subassembly to survive a transient partial blockage of the coolant flow which otherwise would cause melting.

Secondly, when vapour is formed under conditions of high superheat, the bubbles will grow much more rapidly, which may give high pressure pulses and rapid reactivity changes. As pointed out above, the pressure transient due to an expanding bubble is unlikely to be severe enough, even for superheating to 1300°C, to do significant mechanical damage.

In the accidents considered above, the possible rapid addition of reactivity is unimportant, as the reactor we have considered is always well below prompt critical while it is being subject to reactivity changes due to coolant movement, but in a reactor with a much more positive sodium void coefficient, it might be possible for the violent expulsion of superheated sodium from a subassembly to give a high rate of addition of reactivity at prompt critical, so increasing the energy yield of the consequent excursion.

Effect of a Single-subassembly Accident on the Rest of the Core

An accident which is initially confined to a single subassembly can affect other subassemblies by changes in reactivity, temperature, or pressure, or by interruption of the coolant flow.

The reactivity changes due to fuel and coolant movement in a single-subassembly accident cannot approach a dollar, so the resulting power transient will take place slowly. Damage to the rest of the reactor will be prevented by a high-power trip system.

Temperature changes in one subassembly are in-

sulated from the rest of the core by two wrappers, the combined thickness of which would be about 0.5 cm, cooled on one side by flowing sodium. If the fuel in the damaged subassembly is spread in a layer 0.2 cm thick on the inside of the wrapper, and the inside of this layer is at 3500°C (approximately the vapourisation temperature), it takes about 10 sec to melt through the two wrappers. It should be possible to shut down the reactor in a time of this order.

The pressures generated in expelling liquid coolant from a subassembly or in the growth of vapour bubbles in superheated coolant are too small to do significant mechanical damage. It might be possible, however, for an accident to spread to unaffected parts of the core by means of a pressure pulse produced by explosive vapourisation of the sodium as described above. This is outside the scope of this paper, but must be noted as a possibility.

If the outlet of a subassembly were blocked with molten fuel and all the available fission-product gas were ejected downwards, it would occupy a considerable volume below the core. It would be necessary to design the sodium inlets so that the volume of gas which could be released if all the fuel pins in one subassembly were broken would be trapped and not allowed to rise into adjacent subassemblies and cause overheating.

Under the worst possible conditions, sodium vapour could be generated so fast in a blocked subassembly that no trapping system could cope with it if it were expelled downwards. In this case, however, the vapour would condense very rapidly, and it seems unlikely that bubbles would last long enough to interrupt the sodium flow in other subassemblies.

The Time Available to Trip the Reactor

There is little difficulty in shutting down the reactor in a whole-core accident, as monitoring coolant temperature or flow or neutron flux, for example, afford several independent means of detection. Only in an accident as violent as a sudden complete flow failure is there any difficulty in shutting down quickly enough.

Detection of a single-subassembly accident may be much more difficult. It may be impossible to monitor the outlet temperature from every subassembly, and, in addition, in an accident involving complete blockage of the coolant flow the outlet temperature will not rise until the coolant is being expelled from the subassembly as it boils.

Even if it is possible to detect the onset of vapourisation it is unlikely that can melting and collapse of the fuel in the affected subassembly can be prevented, as this may follow within 0.6 sec. This is probably too short a time for a trip mechanism to act.

Provided it can be shown, however, that expulsion of vapour below the core cannot affect other subassemblies, there is no possibility of damage spreading to the rest of the core until the hot fuel melts through the wrappers in about 10 sec. Thus, although gross damage to the affected subassembly cannot be prevented, it is possible to prevent major damage to the rest of the core.

Once again we have not considered the problem of preventing explosive vapourisation of the coolant, and this may present a more acute detection problem.

Summary of Conclusions

1. For the reactor considered here, the maximum positive reactivity due to loss of coolant actually reached in a whole-core accident is 70 cents, compared with a maximum of \$2 on voiding the centre part only of the core.

2. The method outlined here gives a realistic means of assessing the importance of a positive sodium void coefficient of reactivity.

3. The peak reactivity due to loss of sodium in a whole-core accident can be reduced by increasing the relative power density in the outer part of the core.

4. Cascade failure of all the fuel pins in a subassembly precipitated by release of fission-product gas from a single faulty pin is unlikely, but has not yet been shown to be impossible.

5. Apart from 4 above, the only effect of a pressurised fuel-pin design on the safety of the reactor is a possible reduction of the coolant temperature at which the fuel suffers damage.

6. Superheating of liquid sodium probably has little effect on the safety of the reactor, except in a design with a more positive sodium void coefficient, for which it might provide a means of increasing reactivity rapidly at prompt critical.

7. Accidents can only spread from a single subassembly to the whole core by expulsion of vapour below the core (which seems to be very unlikely), by melting through the subassembly wrapper (which takes of the order of 10 sec), or by explosive vapourisation of the coolant.

8. In the event of a flow blockage in one subassembly, it is impossible to prevent collapse of the fuel in that subassembly, but it appears to be possible to prevent damage to the rest of the core.

As a result it seems that experimental and theoretical work should continue on:

- the evaluation of the sodium void coefficient;
- the possibility of explosive vapourisation of the coolant;
- the possibility of cascade failure due to release of gas from a faulty fuel pin (see 4 above);

the expansion and collapse of vapour bubbles in superheated and subcooled sodium (see 7 above).

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Discussion

Mr. Noyes (AI): I'd like to make a remark on the question of sodium superheat during boiling, which has been raised a number of times this morning. We have made quite a large number of experiments on the boiling of sodium, both with pool boiling and for a forced-convection situation. We have never observed the degree of superheat which has been mentioned this morning. The most that we have ever observed is 20–30°F. This is not to say that such superheat is not possible, because it has been observed in experiments in other systems, but it is to say that it's not necessary, and we need to study, I think, mostly what causes the high degree of superheat and what can be done to prevent it.

Mr. Hicks: Even if the superheat were to occur, I'm far from sure that it would be a serious problem.

Mr. Zebroski (GE): I believe your calculations assume that the reactor continued running at full power after the channel blockage. In such a case you

should be getting a variety of signals from different sensors that something is happening. Have you evaluated the different sorts of signals you might get, such as temperature rise, noise, pressure changes, and so on? Do you have any judgment as to which of these might be the most effective in preventing going on to the later stages of the accident?

Mr. Hicks: This is a very big question. It hasn't been considered from that point of view in this paper itself, but a lot of work is going on in other establishments and from other points of view to try and determine the answer to this type of question.

Mr. Anderson (LASL): Dr. Chernick mentioned his concern that designers were sacrificing economics or, frequently, breeding ratio, for safety. Frequently this is probably due to a desire to consider the worst accident without looking at its credibility. Do you think that your approach of trying to specify accidents without presupposition as to the severity could lead to downgrading of the safety criterion and therefore upgrading the economics of the system?

Mr. Hicks: You are almost asking a philosophical question. I think that one has to examine accidents without regard to their credibility to some extent, but I think at one stage one has simply got to make a considered judgment about credibility and its consequences.

Mr. Häfele (Karlsruhe): In bringing up the time scale of the sodium expulsion in your considerations, to what extent did you employ as an input the velocity of sound, or was that calculation independent of that consideration?

Mr. Hicks: It was independent of that; it was simply a Newtonian-type dynamical calculation of a column of liquid sodium being expelled as a rigid column by gas pressure.

Mr. Häfele: Therefore, I assume that you did not look into the phenomena of shock short waves there.

Mr. Hicks: No. The time scale of 0.1 sec or more is very, very long in comparison with sonic velocities.

Mr. Häfele: No.

Session II
October 11, 1965

Fast Reactor Concepts

Chairman: W. H. ZINN
Combustion Engineering

Secretary: D. K. Butler
Argonne National Laboratory

A Large Metal-fueled Fast Reactor Study

Part I. Neutronics Calculations for a 1,000-MWe Plutonium-Uranium, Metal-fueled, Fast Breeder Reactor

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Introduction

An evaluation study directed toward the design of a 1,000-MWe plutonium-uranium, metal-fueled, fast breeder reactor has been carried out at Argonne National Laboratory.⁽¹⁾ The effects of various design considerations as they relate to the physics, safety, engineering, and economic aspects of the system were evaluated. The study also served to assist in evaluating the relative merits of metal- versus ceramic-fueled fast breeders in this power range.

The general design evolved was that of a ring of six cylindrical core modules separated by and surrounded by fertile blanket. Each module is equivalent to about a 800-liter core loosely coupled to the neighboring modules. A simplified schematic is shown in Fig. 1.

The modular design reduces the magnitude of positive sodium voiding effects. To improve the negative Doppler coefficient and to effect core-module radial power flattening, a beryllium-containing region has been inserted at the core-blanket interfaces of the modules. To further improve the radial power flattening, the core modules have been radially two-zone enriched.

The inner and outer radial peripheries of the fertile blankets of the composite ring of the six modules are surrounded by beryllium regions. These are surrounded by the steel inner and outer radial reflectors. The external beryllium regions reduce fertile inventory and improve blanket breeding.

Use of fission alloy fuel (containing fission products) and low-capture alloy fuels, such as titanium or zirconium alloys, has been studied.

This paper describes results of some neutronics calculations for single-enriched and zoned-enriched modules carried out in support of this fast reactor study.

Single-enrichment Module

Calculations have been directed primarily toward understanding the neutronics of a module consisting of its core, blanket, and reflector regions. The effective thickness of a radial blanket associated with a module

is dependent upon the radial direction from the module center. Furthermore, the overall outer beryllium region and steel reflector are not present in directions between neighboring modules. Cylindrical calculations for a module therefore included a range of radial blanket thicknesses with outer radial boundary conditions of zero current (without outer radial beryllium and reflector regions) and of zero flux (with outer radial beryllium and reflector regions).

Dimensions of the single-enrichment core module, associated blankets, beryllium regions, and reflectors are listed in Table 1. Atom densities of the materials in the various regions are listed in Table 2. A modification of the Argonne 26-group-cross section set⁽²⁾ was used. In the core these atomic densities represent: 45 v/o fuel element, 40 v/o sodium, 9 v/o steel structure, and 6 v/o control mechanism. The fuel element composition is 30 v/o Ti-V (20 w/o Ti, 80 w/o V) clad and 70 v/o of (U + Pu + 4 w/o Fizzium + F.P.). Using 5.5 heavy-atom percent burnup in the core as a basis, the fission product pairs are to represent an average of 2.75 percent heavy-atom burnup, i.e., 2.75% of the U + Pu atoms are fission product pairs. The expanded (U + Pu + Fizzium + F.P.) density has been assumed to be 12 gm/cc. The following atomic density ratios of plutonium isotopes to total plutonium atomic density have been assumed: 0.751, 0.210, 0.0295, and 0.0085 for Pu²³⁹, Pu²⁴⁰, Pu²⁴¹, and Pu²⁴², respectively. The 6 v/o of control has been replaced by 4 v/o additional sodium and 2 v/o additional steel for most of the analyses to be described. This corresponds to the case of no poison control inserted in the core. The atom density of zirconium listed in Table 2 represents the addition in the analyses of zirconium to simulate the noncapture properties of the listed fission capture material.

The radial beryllium-containing zone surrounding the module consists of 5 v/o sodium, 12 v/o steel, and 83 v/o beryllium.

The atomic densities of the inner radial blanket

region represent 47 v/o fuel element, 10 v/o steel structure, and 43 v/o sodium. The 47 v/o fuel element consists of about 70% U^{238} and 30 v/o titanium-vanadium cladding alloy. 2.4 w/o of the uranium fuel is Pu^{239} , which represents the average fissile concentration at midlife.

The outer radial blanket region instead contains

FIG. 1. Simplified Schematic of Horizontal Section of Reactor at Midplane of Core Modules.

TABLE 1. DIMENSIONS (cm) OF MODULE REGIONS

Core—840 liters; 92-cm height, 53.9-cm radius		
Inner Beryllium		5.3
Radial Blankets	Region 1	6.1
	Region 2	~17 → 38
Outer Beryllium		15
Radial Reflector		37
Axial Blanket		33
Axial Reflector		55

68 v/o fuel element, 7 v/o steel structure, and 25 v/o sodium. The 68 v/o fuel element consists of about 74% U^{238} and 26% titanium-vanadium alloy. The average fissile concentration at midlife is represented by Pu^{239} present as one weight percent of the uranium fuel. The steel reflector is 95 v/o steel and 5 v/o sodium. The composition of the outer beryllium region is taken to be the same as that of the core-blanket interface region described previously.

The atomic densities in the axial blanket correspond to 45 v/o fuel element, 40 v/o sodium coolant, 9 v/o steel structural material, and 6 v/o for control movement. The latter has been represented by 4 v/o additional sodium and 2 v/o additional steel. The fuel element consists of about 70 v/o U^{238} and 30 v/o of the titanium-vanadium cladding alloy. One weight percent of the uranium fuel is assumed to be Pu^{239} , which represents the average fissile concentration at midlife of the core. The expanded fuel density, i.e., ($U^{238} + Pu^{239}$), is assumed to be 15 gm/cc in the blankets.

The atomic composition of the axial reflector corresponds to 60 v/o sodium, 26 v/o steel, and 14 v/o titanium-vanadium cladding alloy.

Corresponding quantities for the two-zoned enriched module are given in a subsequent section. Calculations were by one-dimensional diffusion theory. Calculated results for sodium void include considerations of the change in axial reflector savings upon removal of core sodium.

Effects of Radial Beryllium-containing Regions and Variations of Radial Blanket Thickness

The effects of the radial beryllium-containing regions at the core-blanket and/or blanket-reflector interfaces

TABLE 2. REGIONAL ATOMIC DENSITIES (10^{24} UNITS) USED IN THE CALCULATIONS

Material	Region						
	Core	Axial Blanket	Axial Reflector	Radial Beryllium Region	Radial Blanket		Radial Reflector
					Region 1	Region 2	
Pu^{239}	0.00893*	0.000119	—	—	0.000299	0.000191	—
U^{238} **		0.011824	—	—	0.012172	0.018902	—
Stainless Steel	Fe	0.006765	0.01599	0.00738	0.00615	0.004305	0.058425
	Cr	0.001782	0.004212	0.001944	0.00162	0.001134	0.01539
	Ni	0.000847	0.002002	0.000924	0.00077	0.000539	0.007315
Na	0.00968	0.00968	0.0132	0.00110	0.00946	0.00550	0.0011
Fizzium	0.000892	—	—	—	—	—	—
(+ Zr)	(0.000892)	—	—	—	—	—	—
Fission Product Pairs	0.000256	—	—	—	—	—	—
V	0.007272	0.007272	0.007541	—	0.007595	0.009523	—
Ti	0.001933	0.001933	0.002005	—	0.002019	0.002532	—
Be	—	—	—	0.103	—	—	—

* For core, this quantity includes also the higher plutonium isotopes of masses 240, 241, and 242, as well as Pu^{239} and U^{238} .

** 1500°K U^{238} cross-section parameters used for Doppler calculations.

upon critical enrichment, breeding and core conversion ratio, sodium void effects, and Doppler effects of isolated modules are listed in Table 3.

The beryllium-containing region at the blanket-reflector interface was assumed to have the identical composition as that listed for the core-blanket interface. The thickness, however, was 15 cm rather than 5.3 cm.

The effects of the beryllium at the core-blanket interface is to augment the negative Doppler by greater than a factor of two.

Calculations show that the beryllium region can also augment the negative reactivity due to uniform voiding of core sodium.

The addition also of an outer beryllium region in the case of the thinner (~ 29 cm) radial blanket results in a breeding ratio of 1.42, which is comparable to 1.43 obtained with the thicker blanket.

The presence of the beryllium-containing region at the radial interface of core and blanket results in radial power flattening. This is evident in Fig. 2, in which a comparison is made between the radial source distribution with and without the beryllium region. The spectral degradation resulting from the presence of the beryllium region results in preferential lowering of the spectrum in the radial peripheral region of the core. This is advantageous in that centrally located sodium voiding of the core does not diminish the peripherally weighted Doppler effect.

Low-capture Alloy Fuel Compared with Fizzium-alloy Fuel

The neutronics effects of the presence of fizzium in the fuel material of the core has been studied by substitution of an equal number of titanium atoms for the

TABLE 3. EFFECTS OF BERYLLIUM REGIONS AND BLANKET THICKNESS UPON ISOLATED, REFLECTED MODULES

Beryllium Region	Calculated Quantities	Radial Blanket Thickness	
		29.1 cm	44.1 cm
No Beryllium Regions	Critical concentration ⁽¹⁾	0.190	0.190
	Core conversion ratio ⁽²⁾	0.60	0.60
	Breeding ratio ⁽³⁾	1.46 ^(a)	1.58 ^(b)
	Sodium void effect ⁽⁴⁾	-0.06%k	-0.07%k
	Doppler effect ⁽⁵⁾	-0.16	-0.16 (-0.11)
Beryllium at Core-Blanket Interface	Critical concentration ⁽¹⁾	0.189	0.189
	Core conversion ratio ⁽²⁾	0.63	0.63
	Breeding ratio ⁽³⁾	1.40 ^(c)	1.43 ^(c)
	Sodium void effect ⁽⁴⁾	-0.17%k	-0.17%k
	Doppler effect ⁽⁵⁾	-0.36 (-0.33)	-0.35 (-0.32)
Beryllium at Core-Blanket and Blanket-Reflector Interfaces	Critical concentration ⁽¹⁾	0.189	
	Core conversion ratio ⁽²⁾	0.63	
	Breeding ratio ⁽³⁾	1.42 ^(c)	
	Sodium void effect ⁽⁴⁾	-0.18%k	
	Doppler effect ⁽⁵⁾	-0.35 (-0.32)	

(a) Includes contributions of 0.33 from axial blankets.

(b) Includes contributions of 0.38 from axial blankets.

(c) Includes contributions of 0.31 from axial blankets.

$$^{(1)} \text{ Critical concentration} = \frac{(\text{total Pu})_{\text{in core}}}{(\text{total Pu} + \text{U}^{238})_{\text{in core}}}$$

$$^{(2)} \text{ Core conversion ratio} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242})_{\text{in core}}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241})_{\text{in core}}}$$

$$^{(3)} \text{ Breeding ratio} = \frac{(\text{captures in U}^{238}, \text{Pu}^{240}, \text{and Pu}^{242})_{\text{in core and blanket}}}{(\text{absorptions in Pu}^{239} \text{ and in Pu}^{241})_{\text{in core and blanket}}}$$

(4) Reactivity resulting from a 40% reduction of core sodium.

(5) Based on reactivity resulting from U^{238} Doppler effect in core for temperature change from 750°K to 1500°K with constant sodium composition and (with 40% less sodium in core). The Δk have been converted to $T(\partial k/\partial T)_{\text{Doppler}} 10^2$, assuming this quantity independent of temperature.

fizzium atoms. This corresponds to an 8.9 a/o titanium concentration in the fuel meat.

Results of the calculations are given in Table 4 for systems in cylindrical geometry, having ~ 29 cm of radial blanket, surrounded by 15 cm of beryllium-containing region, in turn surrounded by a 37-cm reflector. The use of titanium rather than fizzium results in a breeding increase of about $+0.06$.

By comparison with the sodium voiding results given in Table 3 the removal of fizzium results in a substantial improvement in the magnitude of the overall negative sodium void effect. It is noted, however, that the presence of beryllium at the core-blanket interface does not affect the sodium void reactivity as significantly with the titanium alloy fuel as with the fizzium alloy fuel.

Fig. 2. Comparison of Radial Source Densities with and without Beryllium Region.

TABLE 4. COMPARISON OF TITANIUM AND FIZZIUM ALLOY FUELS

Beryllium Region	Calculated Quantities*	With Titanium	With Fizzium
No Beryllium at Core-Blanket Interface; Beryllium at Blanket-Reflector Interface	(1)	0.182	0.190
	(2)	0.64	0.60
	(3)	1.62 ^(a)	1.56 ^(a)
	(4)	-0.28% <i>k</i>	-0.08% <i>k</i>
	(5)	-0.23 (-0.17)	-0.16 (-0.12)
Beryllium at Core-Blanket Interface and at Blanket-Reflector Interface	(1)	0.179	0.189
	(2)	0.68	0.63
	(3)	1.49 ^(b)	1.42 ^(b)
	(4)	-0.32% <i>k</i>	-0.18% <i>k</i>
	(5)	-0.52 (-0.47)	-0.35 (-0.32)

* Identical footnotes as in Table 3.

^(a) Includes contribution of 0.38 from axial blankets.

^(b) Includes contribution of 0.31 from axial blankets.

TABLE 5. NEUTRONICS CHARACTERISTICS COMPARED FOR DIFFERENT FUEL ALLOYS*

	Alloy					
	None	Fizzium, 4 w/o	Titanium		Zirconium, 22 a/o	Carbon, 1:1
			8.9 a/o	30 a/o		
(1)	0.16	0.18	0.17	0.17	0.17	0.17
(2)	0.73	0.62	0.67	0.68	0.68	0.72
(3)	1.67	1.57	1.64	1.61	1.62	1.54
(4)	-0.17	-0.08	-0.30	-0.26	-0.18	-0.16
(5)	-0.16	-0.12	-0.17	-0.20	-0.17	-0.35

* Footnotes as in Table 3 (when applicable to spherical geometry).

Doppler reactivity effects are improved by removal of the fizzium. These values are approximately 40% more negative.

Relative results of employing various other fuel alloys are given in Table 5, based on calculations in spherical geometry with surrounding blankets and reflectors corresponding to those of the previous cylindrical radial values. The core-blanket beryllium was not present. In these cases the density of Pu + U atoms was held constant. For example, the 8.9 a/o titanium concentration in the fuel was increased to 30 a/o at the expense of void space. Analogously, the 8.9 a/o titanium was replaced by 22 a/o zirconium. The last column gives the results of adding carbon atoms in 1:1 ratio to the heavy atoms.

Intermodule Reactivity Coupling

Because of the ring configuration, the intermodule coupling of a given module is dependent upon the radial direction. An estimate has been made of the intermodule reactivity coupling. Fizzium-alloy and titanium-alloy, single-enriched module cases were calculated without the outer beryllium and steel radial reflectors but with zero-current boundary conditions to simulate blanket regions between modules. A thick-blanket (38 cm) case corresponds essentially to an uncoupled module. A thin-blanket (23 cm) case corresponds approximately to one-half the average blanket thickness separating the modules. Less than one-third of the radial blanket surrounding a module is positioned between core regions of neighboring modules.

One-third of the reactivity differences between the thick- and thin-blanket cases are given in Table 6. Analogous values, similarly obtained, assuming 50% less of the Pu²³⁹ in the radial blankets, are indicated in parentheses.

The reactivity coupling of a given module is seen to be quite small, especially when the inner core-

TABLE 6. ESTIMATED MAXIMUM REACTIVITY COUPLING OF A MODULE TO THE RING OF MODULES

Beryllium Region	Fuel Alloy	
	Titanium	Fizzium
None	0.48% (0.40%)*	0.45% (0.38%)
At Core-Blanket Interface	0.15% (0.11%)	0.13% (0.10%)

* With 50% less Pu²³⁹ in radial blankets.

blanket beryllium region surrounds each module. Fifty percent variation of Pu²³⁹ content in the radial blankets varies the reactivity coupling by about a 0.1% *k* increment, without the inner core-blanket beryllium regions present.

Two-zoned Core Enrichments

Use of two-zone enriched core modules together with a beryllium interface between core and radial blanket has been considered in order to improve power flattening. In the zoned-core case the module dimensions differ from those listed in Table 1. These dimensions are given in Table 7.

The overall core volume in this case is thus about 784 liters. Analyses were by cylindrical one-dimensional geometry and zero-flux outer boundary conditions. The same axial reflector savings and variation of axial reflector savings used in the previous single-zoned-core cases also containing titanium fuel alloy were used.

In the two core zones the volume fractions are, respectively, 45.79 and 49.69 v/o fuel element, 39.42

TABLE 7. DIMENSIONS (cm) OF MODULE REGIONS

Core	Region 1	511.2 liters; 92-cm height; 42.2-cm radius
	Region 2	272.4 liters; 92-cm height; 10.1 cm thick
Inner Beryllium		4.2
Radial Blankets	Region 1	9.7
	Region 2	24
Outer Beryllium		12
Radial Reflector		36
Axial Blanket		33
Axial Reflector		55

and 42.40 sodium, and 8.80 and 7.91 steel structure. The inner core zone in addition contains 6 v/o control made up of about 58 v/o sodium, 35 v/o steel, and an assumed 7 v/o B₄^{nat}C to represent poison present during operation. The fuel-element composition is 31.3 v/o TiV (20 w/o Ti and 80 w/o V) clad and 68.7 v/o of (U + Pu + Ti) alloy plus fission products pairs. If we take an average heavy atom burnup of 6%, the assumed fission product pairs are taken to represent a time-averaged value in burnup cycle of 3% burnup, i.e., 3% of the U + Pu atoms are fission product pairs. Expanded (U + Pu + Ti + F.P.P.) density has been retained at 12 gm/cc. The fuel alloy has been assumed to contain about 8.9 a/o titanium.

Atom densities used in the calculations for the various regions are listed in Table 8.

Results for the zoned core with and without the core-blanket beryllium region are given in Table 9 for three enrichment ratios in region 2, namely, [Pu/(Pu + U)]_{core} = 0.20, 0.21, and 0.22. Criticality in all cases was by variation of [Pu/(Pu + U)]_{core} in region 1.

TABLE 8. REGIONAL ATOM DENSITIES (10²⁴ UNITS) USED IN ZONED-CORE CALCULATIONS

Material	Region						
	Core		Radial Beryllium Region	Radial Blanket		Outer Radial Beryllium Region	Radial Reflector
	Region 1	Region 2		Region 1	Region 2		
Pu ²³⁹	0.009088*	0.009862*	—	0.000311	0.000194	—	—
U ²³⁸	—	—	—	0.012647	0.019216	—	—
Stainless Steel {	Fe	0.006701	0.004865	0.004865	0.004287	0.008610	0.057810
	Cr	0.001765	0.001281	0.001281	0.001129	0.002268	0.015228
	Ni	0.000839	0.000609	0.000609	0.000537	0.001078	0.007238
Na	0.009436	0.009328	0.002200	0.009328	0.005326	0.001320	0.001320
F. P. Pairs	0.000281	0.000305	—	—	—	—	—
V	0.00772	0.008378	—	0.008378	0.009527	—	—
Ti	0.002961	0.003213	—	0.002052	0.002334	—	—
Be	—	—	0.08034	—	—	0.09888	—
B ^{nat}	0.000342	—	—	—	—	—	—
C	0.000086	—	—	—	—	—	—

* For core regions, these quantities include also the higher plutonium isotopes of masses 240, 241, and 242, as well as Pu²³⁹ and U²³⁸.

TABLE 9. RESULTS OF CYLINDRICAL CALCULATIONS OF TWO-ZONED CORE MODULES

Beryllium Region	$\left(\frac{\text{Pu}}{\text{Pu} + \text{U}}\right)_{\text{core}}$		$\left(\frac{\text{Fissile PU}}{\text{PU} + \text{U}}\right)_{\text{core}}$		Core Conversion Ratio	Breeding Ratio	40% Na Void in Core	Doppler ^(c)		Source Peak to Average at Cylindrical Midplane
	Region 1	Region 2	Region 1	Region 2				Na In	Na Out ^(d)	
No Beryllium at Core-Blanket Interface; Beryllium at Blanket-Reflector Interface	0.178	0.200	0.139	0.156	0.63	1.63 ^(a)	+0.02% _k	-0.21	-0.16	1.35
	0.176	0.210	0.137	0.164	0.63	1.63	+0.06	-0.21	-0.16	1.32
	0.173	0.220	0.135	0.172	0.62	1.63	+0.10	-0.21	-0.16	1.29
Beryllium at Core-Blanket Interface; Beryllium at Blanket-Reflector Interface	0.176	0.200	0.137	0.156	0.66	1.50 ^(b)	-0.05% _k	-0.41	-0.36	1.25
	0.173	0.210	0.135	0.164	0.65	1.50	0.00	-0.41	-0.36	1.20
	0.170	0.220	0.132	0.172	0.65	1.50	+0.04	-0.41	-0.36	1.16

(a) Includes contribution of 0.38 from axial blankets.

(b) Includes contribution of 0.31 from axial blankets.

(c) $T(\partial k/\partial T)_{\text{Doppler}} 10^2$.

(d) For case of 40% less sodium in core.

FIG. 3. Radial Source Density Distribution in a Zoned-core Module.

Comparisons of these two-zoned results with those of the analogous one-zoned titanium alloy-fueled cases in Table 4 show that the resulting breeding ratios are effectively unchanged even though in the central zone B_4C is present in the case listed in Table 9. Doppler effects are not changed by the use of zone enrichment. The somewhat lesser optimistic Doppler in the two-

zoned (relative to the one-zoned) beryllium interface case is instead largely due to the smaller effective thickness of this beryllium region. Zoned loading does, however, sizably decrease the previously very optimistic sodium void effect. This becomes more evident with increase in zone enrichment in the outer core. The combined effects of zoned loading and moderate-sized beryllium-containing region do, however, considerably improve the peak-to-average source value. Radial source distributions for one of the zoned cases containing the beryllium region is shown in Fig. 3.

Integrated radial-dependent sodium removal from the two-zoned, beryllium-interfaced core having an outer core enrichment of 21% plutonium has also been calculated. Removals of 40% sodium internal to cylindrical radii 29.8 cm, 42.2 cm, and 52.3 cm result in multiplication changes of +0.24% k , +0.23% k , and 0% k , respectively (the latter value is for 40% sodium removal from the entire core).

Equilibrium Fuel Cycle and Reactivity Loss from Burnup

Two properties of the 1000-MWe system connected with long-term operation have been studied: the equilibrium fuel composition, and the change in reactivity to be expected between refuelings. The CYCLE code⁽³⁾ was used. CYCLE is a one-dimensional code that calculates the equilibrium fuel composition for a specified burnup and refueling scheme. It also calculates the isotopic changes and changes in reactivity which must be made to maintain criticality over the cycle. Because a single dimension is assumed, the code in cylindrical geometry cannot take into account changes in the axial reflector savings that would occur in the actual system. On the other hand, if spherical geometry is used, the code assumes a uniform blanket

all around. It is therefore necessary to estimate the characteristics of the proposed system on the basis of a series of problems with appropriately varied compositions and geometries.

In calculating the equilibrium composition, an additional restriction is that the core loading be uniform. Therefore, the properties of the reference design with the two enrichment zones were estimated subject to the assumption of a single enrichment with the same volume fraction of fuel, clad, structure, and coolant. It was also necessary to use a "single batch" core in which the whole core is reprocessed at once. To approximate the actual scheme proposed, it was assumed that one-third of the fission products and 2% of the heavy atoms corresponding to reprocessing losses were removed from the core fuel. The remainder was returned, combined with sufficient fresh U^{238} , Pu^{239} , and boron to restore the core to its initial uniform composition. The code adjusts the feed enrichment so that the system is critical at midcycle, but allows the reactivity to vary during the cycle as the composition changes with burnup.

Problems to determine equilibrium composition were run in spherical geometry with both the axial and radial blankets of the reference design. The equilibrium atom densities thus obtained were very close to those used in the reference design.

The code also computes the changes in isotopic composition in the blanket over the cycle. These were found to be in satisfactory agreement with those estimated for the reference design.

The reactivity change due to burnup during a cycle was determined in a separate calculation in which a desired burnup expressed as the fraction of fissions of the heavy atoms during the cycle was specified. The burnup was taken to be 2% in the core. Like that of equilibrium composition, the reactivity change for the actual two-dimensional cylinder was estimated by means of a series of one-dimensional calculations. The reactivity change for the reference systems with two enrichment zones in cylindrical geometry was determined to be 2.6%. In this calculation it was assumed that the height was constant, that is, there was no change in the axial direction. Calculations in spherical geometry analogous to those in which the equilibrium composition was determined showed that the correction for axial effects would be less than $0.2\% \Delta k/k$.

The burnup calculations support the original assumptions on which the reference design was based, both as to equilibrium composition and the amount of reactivity which will be lost by burnup each cycle. The latter quantity also supports the estimate of average amount of boron which must be present for

the purpose of the reactivity variation over a burnup cycle.

Reactivity Coefficients and Dynamic Behavior of Modules

Thermal expansion of the reactor modules can be divided into radial and axial components. Estimates of these components were made by calculating, in spherical geometry (with uniform core composition), the volume coefficient of expansion for the sphere and converting it into components of a cylinder, by means of the equation

$$\frac{\Delta V}{V} = \frac{\Delta h}{h} + 2 \left(\frac{\Delta r}{r} \right)_{\text{cylinder}}.$$

Table 10 gives the results obtained in this way for various assumptions about the mechanism of the expansion.

Exploratory studies of the dynamic behavior of a single module of the 1000-MW system have been made for two kinds of transient conditions important to considerations of safety: ramp insertions of reactivity and loss of coolant flow. Calculations were performed with a modified version (RP351X) of the FORE⁽⁴⁾ program. This cylindrical-geometry code computes the temperature distribution as a function of time across an average and the hottest fuel pins under conditions of time-varying reactivity and, if desired, time-varying coolant velocity. The code includes feedback from Doppler effect and from expansion of fuel, clad, coolant, or structure.

Information was sought on the importance of the magnitude of the Doppler feedbacks and of fuel or structural expansion. Doppler coefficients appropriate for systems both with and without a beryllium region at the core-blanket interface were used. Thermal expansion coefficients corresponding to axial expansion of fuel meat and radial expansion due to the

TABLE 10. EXPANSION COEFFICIENTS OF REACTIVITY

	$\left(\frac{\Delta k}{k} = \alpha \frac{\Delta h}{h} + \beta \frac{\Delta r}{r} \right)$	
	α	β
1. Axial (Expanding Fuel, Displacing Blanket)	-0.30	
2. Axial (Contracting Fuel, Leaving Clad, Coolant, and Structure in Gap)	-0.26	
3. Radial (Contracting Core Radius, Leaving Clad, Coolant, Structure in Gap)		-0.53
4. Radial (Contracting Core Radius, Filling Gap with Inner Radial Blanket)		-0.62

stainless steel structure were also used. These were selected as possible sources of negative feedback for calculational purposes as examples. In practice this axial expansion of the fuel might be lowered by its confinement in the expanding Ti-V clad. In addition, availability of fuel expansion with the high-burnup fuel remains to be demonstrated. Samples of U-5 w/o Fs alloy, however, with burnup of about 1.3 a/o demonstrated essentially the same transient expansion in TREAT experiments as fresh fuel.⁽⁵⁾

The results shown in Table 11 indicate that the time before fuel melting may be increased 20% by the addition of a sufficiently thick region of beryllium. Fuel temperature coefficients of reactivity corresponding to axial expansion result in a 33% increase under the same conditions. For problems involving loss of coolant flow the input reactivity was held constant. The results indicate that a radial expansion coefficient of reactivity characteristic of stainless steel expansion contributes more feedback than the Doppler effect, even with the beryllium region present. The modular core has the feature (of importance for metal fuel because of the small Doppler effect available) of making possible significant expansion effects.

From the results it is seen that the prompt feedback available between operating temperature and fuel melting is not more than about \$0.5 from the combined effects of axial fuel expansion and Doppler effect. The Doppler effect is necessarily limited with metal fuel even with the addition of the thin, moderat-

ing beryllium annulus. This is primarily because there is only about a 20% rise in absolute temperature available between fuel operating and melting temperatures. For improved feedback, radial expansion introduced, for example, by a fuel cermet structure could provide an improved reactivity feedback mechanism.

In all of the studies of loss of coolant flow, initial boiling took place near the axial core boundary. Thus, initial sodium void reactivity would be negative. Initial coolant voiding in the ramp studies also took place above the core centerline, but not as close to the axial core extremity as with loss of flow. Generally speaking, the larger Doppler coefficient of reactivity obtained when a beryllium annulus was present seems to provide a time to melting that is consistent with times necessary to initiate protective devices for reactivity reduction. A small Doppler coefficient coupled with a reliable prompt axial expansion coefficient provides similar times to melting. In all studies it was assumed that the modules are relatively uncoupled. Detailed study of the interaction of a malfunction in one module and its influence upon a neighboring module is a problem requiring additional study.

Potential slumping of fuel pins within the clad presents an area of undetermined hazard. The initial, unirradiated, fuel-pin diameter is 0.15 in.; the inside clad diameter is 0.174 in. The large amount of effective void filled with bond sodium is required to accommodate irradiation growth. If effective slumping were to occur, the height of the fuel column in the clad could

TABLE 11. DYNAMIC BEHAVIOR OF SINGLE MODULE

No.	Type of Problem	$(dk/dT)_{\text{expansion}}$	$(T \frac{dk}{dT}) 10^2$ Dopp	Fuel Melts (sec)		Coolant Boils (sec)		Feedback Reactivity ^(a) (\$)
				Peak Channel	Average Channel	Peak Channel	Average Channel	
1	Ramp insertion of \$2/sec; large Doppler Coefficient		-0.47	0.44	0.52	>2.0°	>2.0°	-0.18
2	Ramp insertion of \$2/sec; small Doppler Coefficient		-0.12	0.36	0.45	>0.57°	>0.57°	-0.04
3	Ramp insertion of \$2/sec; small Doppler Coefficient and axial fuel expansion	$-8.1 \times 10^{-6}/^{\circ}\text{F}$	-0.12	0.48	0.74	1.46	1.56	-0.40
4	Ramp insertion of \$0.20/sec; small Doppler Coefficient		-0.12	2.18	2.39	2.49	2.57	-0.10
5	Loss of coolant flow ^(b) ; large Doppler Coefficient		-0.47	6.25	7.40	7.60	8.16	-0.17
6	Loss of coolant flow; large Doppler Coefficient and radial expansion	$-3.7 \times 10^{-6}/^{\circ}\text{F}$	-0.47	9.20	9.70	>10.0°	>10.0°	-0.58
7	Loss of coolant flow; small Doppler Coefficient and radial expansion	$-3.7 \times 10^{-6}/^{\circ}\text{F}$	-0.12	8.30	8.80	9.47	9.47	-0.42

^(a) Total feedback reactivity at time corresponding to peak channel fuel melting.

^(b) Flow decays exponentially from 22 ft/sec for 13.2 sec to a final velocity of 0.8 ft/sec.

^(c) Problem terminated at time shown before boiling occurred.

be as much as 25% less than for the unirradiated pin. However, only freshly charged fuel is susceptible to efficient slumping, since it is expected that swelling to fill the radial gap within the clad will occur when only partial burnup has been attained. Furthermore, one-third of the core is replaced at each cycle; thus, only one-third of the core would be available for efficient slumping for roughly half of the time between successive refuelings. It is believed that the reactivity increase would accrue gradually over a period of time, which would provide an opportunity for protective measures. If detailed design were to show that less void space is required in the fuel, the slumping reactivity would be correspondingly less. It is also possible to conceive of a metallic, segmented fuel element, consisting of individually supported segments within the clad, thus significantly reducing potential slumping reactivity. Such a design eliminates the (already unreliable) negative axial expansion coefficient of reactivity. This latter approach would, in this design concept, require both the beryllium annulus for enhanced Doppler reactivity as well as the fueled cermet for structural expansion reactivity.

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Acknowledgments

The authors wish to thank Dr. H. H. Hummel for his interest and helpful comments on various aspects of the study. Thanks are also due to Mr. K. A. Hub and Mr. J. T. Weills for numerous helpful discussions on engineering-related questions.

A Large Metal-fueled Fast Reactor Study

Part II. Core Design and Fuel Cycle

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Introduction

Four divisions of Argonne National Laboratory have participated in the preliminary study of a high-power, metal-fueled reactor system. The Metallurgy and Chemical Engineering Divisions have extrapolated their combined experience in remote fabrication and pyrochemical-processing experience to a large-scale fuel cycle facility for handling high-burnup core and blanket fuel. The Reactor Physics and Reactor Engineering Divisions have studied the neutronic performance and prepared the preliminary engineering design.

Core Design

The core is made up of six modules as shown in Fig. 1. The active zones of each module are surrounded by blanket regions which, in total, are edged inside and out by beryllium. The beryllium, in turn, is backed up by stainless steel. About one-third of the radial blanket is common to two modules. An 800-liter-core module with a length-to-diameter (L/D) ratio of 0.9 is consistent with a small sodium voiding reactivity effect. Six modules are required to generate heat for producing 1000 MW (e).

The module features are reproduced to larger scale in Fig. 2. The 61 subassemblies of the core are divided into two zones of enrichment. The outer 24 subassemblies are at higher enrichment to improve power flattening. The outer zone subassemblies in combination with the inner radial blanket subassemblies have appropriately located beryllium to form the inner beryllium zone. The zone enrichments are adjusted to provide a radial maximum-to-average heat generation density in the core of 1.2. The inner enrichment zone contains the 13 control positions, a central position for regulating rod, and 2 concentric rings of positions for shim and safety rods.

The radial blanket is also in 2 zones. The 30 subassemblies adjacent to the core have smaller-diameter elements to match the requirement for higher heat removal.

The fuel elements are designed partially to accommodate and partially to constrain the tendency of me-

tallic fuels to expand as burnup proceeds. The heavy atom density of the fuel in its expanded form is 12 g/cm³. The fuel elements are assembled into subassemblies as shown in Fig. 3. Tubular spacing of fuel elements is necessary to achieve acceptable pressure drop of the closely spaced fuel elements over their 87-in. (220-cm) length. The total subassembly length is 20.5 ft (6.3 m). The tops of the subassemblies extend above the level of the sodium so that refueling is performed with visual aid.

The characteristics and design performance of the fuel elements are indicated in Table 1. A consistent approach has been applied to the design of a metal fuel element. The fuel is regarded as arbitrarily weak, and the fuel cladding is assumed to act as a tube pressurized according to the degree of burnup with an allowance for both the fission product gas volume and the temperature level.⁽¹⁾ The 87-in.-long fuel tube contains the uranium pin for the lower axial blanket, the fuel pin, upper uranium pin, and a plenum for displaced sodium and inert gas. The plenum height is set to provide the final volume of the inert gas without significant contribution to the wall stresses in the fuel tube. A plug is inserted above the upper blanket pin and restrained to allow only 2 in. (5-cm) of axial movement. Essentially, all of this movement is expected to be expansion of the fuel pin.

Two approximations of the burnup model were utilized in design to arrive at an average axial burnup of the fuel pin. An average burnup of 7.1 percent is consistent with the approximation of a uniform fission product gas pressure over fuel height producing the limiting stress condition at the top of the core where the coolant is hottest. A maximum of 8.3 percent is consistent with the approximation of a localized pressure against the cladding somewhat downstream of the core midplane, where a combination of sodium temperature rise and local burnup produces the limiting condition. The local burnup value is consistent with the average axial burnup approximation of 7.1 percent. The radial maximum-to-average power generation in the core reduces the burnup to 6 percent for the discharged fuel. The most recent irradiation data on these

FIG. 1. Horizontal Section of Reactor at Midplane

types of metallic fuels lead to confidence that the reference design element has the capability to achieve an average burnup of at least 7 percent.

The design of the fuel element and the design of the subassembly are matched to the techniques and equipment determined to be suitable for remote manufacture of low-decontamination fuel. The 3-ft (91-cm) long pin may be beyond the practical limit for casting in Vycor tubing, in which case two castings would be

used. The difference in radial clearance between cast pin and fuel cladding is just large enough to give sodium bonding without prolonged vibration to ensure specified completeness of thermal contact. Fuel elements can be loaded into the hexagonal shroud tube in the manner of EBR-II manufacture to form the fuel subassembly.

The fuel subassemblies of a core module are locked radially by the differential expansion between a ti-

FIG. 2 Section of One Module

FIG. 3. Isometric View of Fuel Subassembly for 1000-MWe Metal Fuel Breeder Reactor Study

tanium pivot plate and a stainless steel driver plate acting at two levels on the lower adapter of each subassembly. The radial clearances that are present among assemblies at the refueling temperature are removed before the approach to criticality by raising the temperature of the system to a point approaching the operating temperature.

Reactor System

Figure 4 is a schematic vertical section of the reactor and one of the six primary sodium loops. Two core modules are shown. Hot sodium exits from the subassembly hexagonal tubes just below the neutron-shield region and spreads out over the module area. Most of the flow is expected to be directly upward before radial flow to the six exit pipes. The shield fitted to the top of the reactor tank is rotated for refueling after control rod disengagement. A removable plug exposes the entire area module, including the outer beryllium, for subassembly replacement for relocation. It is proposed that a spent subassembly be lifted into a sodium-filled tube for transport to storage within the refueling cell.

Reactor characteristics are based on replacement of one-third of the core zone fuel in each module at each refueling period. One-sixth of the inner blanket zone and one-ninth of the outer blanket zone are replaced at each refueling cycle. The period of the refueling cycle is 206 days, with an annual plant load factor of 80% and 6% average burnup of the heavy atom content of the core fuel. The reactivity loss over such a cycle is about 3 percent $\Delta k/k$; roughly 10 percent of this is estimated to be due to axial expansion of the new fuel added at the beginning of each cycle.

The salient design information is summarized in Table 2 for the larger-scale features and reactor performance. The net electrical output is achieved by two turbine generator units matched to 1800-psig (122 atm) steam at 900°F (482°C) and 900°F (482°C) reheat.

Fuel Cycle

The vital function of a fuel cycle facility is to restore irradiated fuel to a form suitable for return to the reactor. For a fast reactor fuel the reprocessing requirements for restoration of the fuel are partial fission product removal and reconstruction of the heavy atom and inert alloying constituents.

Pyrochemical processing and remote fabrication are proposed for the 1000-MW(e) metal-fueled fast reactor. Pyrochemical processing is the generic name chosen by Argonne National Laboratory for a class of nonaqueous processes for metal, oxide, and carbide fuels that are currently under development. These processes employ liquid-phase reactions at tempera-

TABLE 1. FUEL ELEMENT AND DESIGN DATA

Average Burnup in Discharged Fuel, %	6	
Fuel Element Composition, %		
Vanadium-20 w/o titanium cladding	31	
Expanded U-Pu-9 a/o Ti Fuel	69	
	Fuel Pin Features:	
	As Cast	Expanded
Density, g/cm ³	17.5	12
Diameter, in. (mm)	0.150 (3.8)	0.174 (4.4)
Length, in. (cm)	34 (86)	36 (91)
Fuel Tube		
Outside Diameter, in. (mm)	0.210 (5.3)	
Wall thickness, in. (mm)	0.018 (0.46)	
Fuel Enrichment as Charged		
Inner Zone, Pu ₇ /Pu + U, w/o	13.5	
Outer Zone, Pu ₇ /Pu + U, w/o	17.5	
Thermal Data		
Average Linear Power, kW/ft (W/cm)	6.6 (217)	
Hot Spot Cladding Temperature, °F (°C)	1126 (608)	
Hot Spot Fuel Centerline Temperature, °F (°C)	1439 (782)	
Heat Flux, Btu/hr ft ²		
Maximum	6.9×10^5	
Average	4.1×10^5	

tures of 1200 to 2300°F (650 to 1250°C) to effect adequate removal of fission products and the reconstruction of the core and blanket fuel discharged from fast reactors fueled with metal or ceramic fuel. Batch processes are used which employ oxidation-reductions in molten metal-fused salt systems to separate the fuel values from most of the fission products and then to effect the separation of uranium and plutonium. A pyrochemical process for metal fuels, melt refining, is employed in the EBR-II fuel cycle facility, and the remote fabrication procedures considered here are identical in principle to the EBR-II procedures.⁽²⁾

Comparative cost studies are currently being made at Argonne National Laboratory on various processing methods for fast reactor plutonium fuels. These studies indicate that a close-coupled pyrochemical processing plant has about half the cost of an aqueous processing plant of equal capacity processing highly burned and highly enriched plutonium-containing fuels. The lower cost of pyrochemical processing derives from the much lower cell-space requirement, the small volume, the convenient form of the wastes produced, and the shorter period of cooling required before processing.

A simplified flow chart of the fuel cycle proposed for metal fuel is shown in Fig. 5. The fuel cycle consists of three basic components: 1) breakdown of the fuel subassembly, 2) pyrochemical refining of the spent fuel, and 3) fabrication of the fuel subassembly.

FIG. 4. Vertical Section of Reactor and One of Six Primary Sodium Loops

Dismantling and Decanning

The subassembly breakdown is a two-step operation involving dismantling and decanning;⁽⁴⁾ both of these are purely mechanical processes. The dismantling step is a process by which an expended fuel subassembly is stripped of its outer hardware. The exposed bundle of fuel rods is disassembled, and the rods are charged to a magazine which is then loaded into the decanning machine. The decanning machine removes the jacket from the fuel rods and reduces the unprotected fuel pin to short slugs. The fuel discharged from decanning is the input to pyrochemical refining.

Processing

The pyrochemical process is shown in Fig. 6. Six processing steps are required:

1. Core fuel is melted in a beryllia crucible and contacted with a fused salt. The rare earth, alkali metal, and alkaline earth fission products are extracted into the salt phase, which is discarded as waste. Volatile fission products are released to the offgas system.
2. Core fuel is chlorinated with cadmium chloride in

a tungsten vessel. The noble metals remain in the metal phase, which is discarded as waste.

3. The salt from the chlorination step, which contains the uranium and plutonium from the core and axial blanket fuel, is charged to a stainless steel vessel. Uranium and plutonium chlorides from the core fuel are reduced by contacting with a solution of cadmium, zinc, and magnesium. The radial blanket fuel is dissolved. Alkali metal and alkaline earth fission products from the blanket fuel are extracted into the salt phase, which is discarded as waste. Volatile fission products and volatile chlorides of zirconium, niobium, and titanium are released. The rare earth and noble metal fission products in the blanket fuel are not removed.

4. The metal phase from step (3) is charged to a beryllia vessel and retorted to remove most of the cadmium and zinc. The uranium precipitates, and the plutonium remaining in solution in the supernatant is transferred to a separate beryllia retort vessel.

5. The uranium product is retorted to separate solvent metal. Uranium is recovered as a metallic ingot. The solvent metal is recycled (to step 3).

6. The plutonium product is retorted to separate solvent metal. Plutonium is recovered as a metallic ingot. The solvent metal is recycled (to step 3).

Fabrication

Recovered uranium and plutonium are alloyed with the required amount of titanium for feed for the fabrication of the core fuel. This process is a combination of four metallurgical and mechanical operations which convert the fuel alloy ingot into a core subassembly acceptable to the reactor. The various unit operations in the fabrication process are shown in Fig. 7. The blanket fuel is unalloyed uranium.

Fabrication starts with the conversion of a fuel alloy billet into right circular cylindrical castings. The procedure employed is a well-developed^(5, 6, 7) process called injection casting. The casting molds are stripped from the fuel pins and enter a waste stream. The fuel pins enter the mechanical phases of the operation, in which they are cropped to desired length and inspected. Inspection consists of scanning for gross surface de-

FIG. 5. Simplified Flow Chart of the Fuel Cycle

fects, weighing, and checking the diameter and length of the pin.

The assembly of fuel elements from fuel pins and jacket hardware is the second of the four basic fabrication operations. In this phase, sodium is loaded into preassembled and inspected jackets, which are introduced into the remote facility. Acceptable fuel pins from inspection are placed in the jackets containing sodium and bonded. Bonding consists of heating and impacting the jackets until a void-free annulus of sodium is achieved between the inside surface of the jacket and the fuel-pin perimeter. Following bonding, a restraint plug is inserted and held in place by a circumferential depression in the jacket. A top-end plug is then inserted into and fused to the jacket by welding.

The third broad fabrication operation is that of inspection of fuel elements. The end closure on each fuel element is inspected to insure leak-tight integrity. The process employed is a pressure technique developed for EBR-II application.⁽⁸⁾ Since the sodium annulus between the jacket and fuel is generous, no inspection for bond integrity is planned. If it were found necessary to inspect for sodium bond and reclaim non-bonded fuel elements, the technology to do this exists.⁽⁹⁾

The final operation in the fabrication scheme is the assembly of fuel elements into subassemblies acceptable for the reactor. In this operation fuel elements and tubular spacers are loaded alternately, one row at a time, into a hexagonal array. The array is forced into a hexagonal shroud, and the protruding end of each fuel element is attached to a fuel-element support grid which is shouldered against the shroud. After all fuel rods have been attached to the grid, end fittings are mounted and welded to the hexagonal shroud. The completed fuel subassembly is gauged, inspected, and, if found acceptable, is ready for return to the reactor.

TABLE 2. GENERAL REACTOR FEATURES AND PERFORMANCE DATA

Breeding Ratio	1.48
Core Breeding Ratio	0.64
Doubling Time for Reactor Fuel, yr	7.6
Gross Reactor Power, MW(t)	2513
New Plant Output, MW(e)	1000
Primary Sodium Outlet Temperature, °F (°C)	1000 (538)
Primary-Sodium Flow Rate, lb/hr (kg/hr)	10 ⁸ (4.54 × 10 ⁷)
Maximum Sodium Velocity in Core, ft/sec (m/sec)	24 (7.3)
Core Volume per Module, liters	785
Equivalent Core Diameter per Module, ft (cm)	3.46 (105)
Core Height, ft (cm)	3.0 (91)
Core Power Density, kW/liter	
Average	450
Maximum	650
Fraction of Power Generated in Core	0.85

	Core Composition, v/o	
	Inner Zone	Outer Zone
Sodium	39	42
Stainless Steel Structure	9	8
Control Region	6	—
Fuel Element	46	50
	100	100

FIG. 6. Principal Steps in Pyrochemical Processing

FIG. 7. Principal Steps in Fabrication

A conceptual sketch of the fuel cycle facility is presented in Fig. 8. Processing and fabrication operations are conducted remotely under an inert atmosphere in a heavily shielded annular cell which is 20 ft (6 m) wide and has an inner diameter of 74 ft (22.5 m). Fuel subassemblies enter and leave the cell via a tunnel. Operations in the cell are viewed directly through strategically placed windows and are controlled from a 64-ft (19.5-m) diameter control room inside the annulus. Fabrication hardware is assembled and the fuel

tubes loaded with sodium in a large glovebox which provides direct access to the cell. Supporting facilities are located along the outer perimeter of the facility and in the subcell area. Two wings are provided: one houses the administrative offices, lunchroom, and locker room; the other the warehouse, refabrication influents preparation facility, and the stockroom.

Costs

A cost estimate, based largely on EBR-II building and equipment costs, has been made for a fuel cycle facility for the Argonne Reference Metal Fueled Reactor. The plant capacity chosen is considered to be in the area of commercial interest. However, further economics can be realized for plants of larger scale since for most promising steps the criticality limitation on batch sizes has not been reached. An analysis of fuel-cycle economics indicated that for a single 1000-MW(c) reactor, a two-shift-per-day, five-day-per-week plant operation is needed for the plant size chosen. This case has been termed Partial Capacity operation. The operation of the same plant on a schedule of three shifts per day, seven days per week, corresponding to an 80% plant factor, would afford the servicing of about three reactors of the characteristics of the reference metal fuel reactor. This case has been termed Full Capacity operation.

The estimated fuel cycle investment requirements for building and equipment appear in Table 3. For Partial Capacity operation the estimate is $\$11.4 \times 10^6$ and for Full Capacity operation $\$12.3 \times 10^6$. The cost difference is due to the added capacity for waste storage needed for a Full Capacity Plant.

1. TUNNEL CONNECTING REACTOR AND FUEL CYCLE FACILITY
2. HOT MAINTENANCE SHOP
3. DECONTAMINATION CELL
4. ANALYTICAL LABORATORY
5. OUTER OPERATING ANNULUS
6. REFAB. INFLUENTS GLOVEBOX
7. ACCESS TUNNEL TO SUBCELL
8. WAREHOUSE
9. ADMIN. OFFICES, LUNCHROOM, AND LOCKER ROOM
10. PROCESSING ANNULUS
11. MAINTENANCE ANNULUS
12. INNER OPERATING ANNULUS
13. OFFICE SPACE
14. PROCESS CONTROL ROOM
15. SUBCELL
16. COLD RECEIVING DOCK
17. ELECTROMECHANICAL MANIPULATOR
18. BRIDGE CRANE

FIG. 8 Fuel Cycle Facility

Estimates of the annual charges for the fuel cycle facility are presented in Table 4. The charges are based on a 6.75% cost of money, a 13.0% return, and depreciation schedules of 30 years for building, 15 years for nonprocess equipment, and 5 years for process equipment. Allowances are included for construction overhead, startup expenses, and working capital. The

TABLE 3 SUMMARY OF FUEL CYCLE INVESTMENT REQUIREMENTS FOR REFERENCE METAL FUELED REACTOR

	Investment in Building and Equipment, 10 ⁶ \$	
	Partial Capacity*	Full Capacity†
Processing and Refabrication	10.2	10.2
Waste Disposal	1.2	2.1

* Annual throughput of 10,800 kg Core Fuel and 23,600 kg Blanket Fuel

† Annual throughput of 28,400 kg Core Fuel and 62,100 kg Blanket Fuel

TABLE 4. SUMMARY OF ANNUAL CHARGES FOR FUEL CYCLE FOR REFERENCE METAL FUEL

	Annual Charge, 10 ⁶ \$	
	Partial Capacity*	Full Capacity†
Processing and Fabrication		
a) Capital Charges and Depreciation	2.59	2.99
b) Operating Expense	4.12	10.63
Waste Disposal		
a) Capital Charges and Depreciation	0.24	0.41
b) Operating Expense	0.17	0.39
Total Annual Charge	7.12	14.42

* Annual throughput of 10,800 kg Core Fuel and 23,600 kg Blanket Fuel

† Annual throughput of 28,400 kg Core Fuel and 62,100 kg Blanket Fuel.

waste-disposal charge includes allowances for shipping and ultimate disposal offsite. The total annual charge is $\$7.1 \times 10^6$ for Partial Capacity operation and $\$14.4 \times 10^6$ for Full Capacity operation. This amounts to a total unit fuel cycle cost of \$160 per kg of uranium plus plutonium with plant operating at Full Capacity.

A fuel-cycle cost analysis has been made assuming the fuel-cycle facility operating at Full Capacity and using a plutonium value of \$10 per gram of Pu^{239} plus Pu^{241} , private ownership of fuel, and 9% annual charge on working capital; the fuel-cycle cost is 0.9 mill/kWhr at 6% average burnup of core fuel and 0.8 mill/kWhr at 7% average burnup. If these same rules are applied to two previous 1000-MW(e) fast reactor studies,^(10, 11) the fuel cycles represent 0.8 mill/kWhr for both. These latter studies presented fuel-cycle costs based on extrapolations of AEC reference charges for a hypothetical aqueous processing plant⁽¹²⁾ designed for enriched uranium, not high-plutonium, high-burnup fuels.

Based on recent Argonne fuel alloy development and test work, the metal fuels look quite promising.⁽¹³⁾ The uranium-plutonium-titanium alloy used for the reference design is compatible with vanadium-alloy cladding. The uranium-plutonium-zirconium alloy is equally promising. In addition, when the zirconium content is 10 w/o or higher, the alloy appears compatible with stainless steel cladding at suitable operating temperatures; stainless steel has a significant cost advantage over the use of vanadium. Capsules of both fuel alloys are under test and as of September, 1965, have successfully gone beyond 4% burnup. Additional encouragement is found for metal fuels in irradiations of the less desirable uranium-plutonium-fissium alloy which has been irradiated beyond 6% burnup. Based on potential fuel-cycle cost, there appears to be no distinction, at present, between the mixed carbide, metal, or oxide fuel systems for fast reactors.

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Discussion

Mr. Greebler (GE): Have you looked into the effects of the plutonium buildup in the two blankets, and what effect does this have on the coupling between the module of the sodium void effect and also on bypass sodium flow through the blankets?

Mr. Meneghetti: These calculations have included the presence of some buildup in the blanket regions. However, the effect of the buildup on the coupling has not been studied. Perhaps Mr. Link can answer better the other part of your question.

Mr. Link: The same approach was taken in this particular study as was applied to EBR-II in that the fuel elements are orificed separately and must take the flow which comes about at the maximum heat generation at the end of the cycle, so they may slightly undercool at the beginning of cycle.

Mr. Häfele (Karlsruhe): Upon what is that figure of 6% burnup based? A certain estimation or calculation, or do radiation experiments of that kind exist which lead to that figure?

Mr. Link: We have radiation experiments at the present time which have reached this figure under conditions that approach it.

Mr. Häfele: At what temperature was that?

Mr. Link: This particular one has a maximum temperature of 1400°F with 1100°F external or clad temperature.

Mr. Häfele: Did you have a look in capital cost potential of such a modular arrangement? Did you try to evaluate a capital cost aspect to some extent?

Mr. Link: No, we have not.

Mr. Wirtz (Karlsruhe): Why did you use 9 w/o titanium in your fuel?

Mr. Link: It gives a fair degree of radiation stability and raises the melting point of the alloy appreciably above that which we have experienced in the fissium

alloys in EBR-II. With this alloy we can operate at allowable temperatures higher by about 200°C than we have experienced with fizzium.

Mr. Wirtz: What is the highest temperature in your fuel?

Mr. Link: A little over 1400°F for the internal temperature in the fuel.

Mr. Zebroski (GE): What is the reactivity effect of the fuel column slumping by roughly 7 in., as I calculated, because of the large annulus that you have; the core length could decrease from 34 in. to about 27 or 28 in.? Secondly, does the doubling time of 7.6 years include the inventory in cooling and processing?

Mr. Link: Yes, it does. It includes the external inventory and this is calculated on the so-called compound basis in which the assumption is made that the fuel on recovery is reinvested in some reactor system.

Mr. Meneghetti: With regard to the first question, this is an undetermined hazard at present. However, a large amount of effective void with bond sodium is required in order to accommodate a radiation growth. If effective slumping were to occur, the height of the fuel column in the clad could be as much as 25% less

than for the unirradiated pin. Only freshly charged fuel is susceptible to efficient slumping, since it is expected that swelling to fill the radial gap within the clad will occur when only partial burnup has been obtained. Furthermore, only $\frac{1}{3}$ of the core is replaced at each cycle; thus, only $\frac{1}{3}$ of the core would be available for efficient slumping.

Mr. Sherer (GE): Do you happen to know what the thickness of the hexagonal channel or subassembly wall is?

Mr. Link: About 80 mils on the hex can around the

Selected Optimization Studies in the Use of Carbide Fuel in Large Fast Breeder Reactors

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1. SELECTION OF (UPu)C FUEL-ROD DESIGN

1.1 Introduction

Analyses have been performed in order to arrive at a (UPu)C fuel-rod design which will allow for currently expected fuel swelling at 100,000 MWD/MT and keep the maximum fuel temperature below 3500°F at a fuel rating of 30 kW/ft. Two principal methods can be considered to accommodate swelling. In the first, fuel-clad interference is permitted and the cladding is allowed to deform plastically. In the second, lower effective fuel density is assumed to absorb swelling. Both approaches to solve the problem of fuel swelling are being planned for current EBR-II irradiation tests. Fuel with high and low "effective" densities will be irradiated in Type 316 stainless steel clad to determine the best compromise between clad strain, fuel temperature, and effective fuel density. Only the results of such experimental data can guide the further design philosophy for ceramic fuel rods for high burnups.

If the fuel center temperature is to be kept below 3500°F at the beginning of life, a clad strain of 2.8% must be accommodated at the end of the 100,000-MWD/T life for a 95% density fuel. At the present time, data on the effects of fast flux on the ductility of clad materials are lacking. The alternative approach

of lower effective density of fuel to prevent excessive clad strain at end of life was therefore considered in this paper.

Helium is preferred to sodium as a thermal bond material for the following reasons:

1. Clad carburization is facilitated by the presence of sodium.
2. Fabrication costs are higher with sodium.
3. Uncontrolled presence of bubbles in the sodium can lead to excessive fuel temperatures.
4. Contact of sodium with fuel at the center of the pellet may cause excessive internal pressures.

Fission gas venting to the coolant is not taken as a near-term design objective.

Material properties and irradiation effects for the (UPu)C fuel were taken from data recently obtained by United Nuclear Corp.⁽¹⁾ The specific values of the properties used together with properties of the cladding, the filler gas, and the sodium coolant are listed in Table I. The gap conductance (h_g) across the helium filler gas between the fuel and the clad was calculated based on the actual gap thickness between pellet and cladding, taking into account thermal expansion and swelling. When the fuel and clad are in contact, a maximum value of $h_g = 2000$ Btu/hr-ft²-°F was used.

A maximum volume increase of 18% at a burnup of 100,000 MWD/MT⁽¹⁾ is used and assumed to occur isotropically for fuel of 95% theoretical density based on irradiation swelling data recently determined. For fuel of less than 95% theoretical density, the internal voids are assumed to be effective in reducing the external dimensional increases of the fuel pellet. For a pellet with a density of ρ/ρ_T , the external swelling is calculated as $0.18 - [0.95 - (\rho/\rho_T)]$.

The fuel conductivity is assumed to vary directly with the fuel density over the range investigated.

1.2 Summary of Results

Fuel Density and Assembled Radial Gap Width

The determination of the fuel density and assembled radial gap width for the reference fuel-rod design was

TABLE 1. MATERIALS PROPERTIES

	Material		
	(UPu)C	316SS	Na
Density, g/cm ³	13.6 (100% theoretical)	7.8	0.83
Thermal conductivity, BTU/hr-ft-°F	11.6	12.0	40
Specific heat, BTU/lb-°F	0.062	0.134	0.3
Coefficient of thermal expansion, °F ⁻¹ × 10 ⁶	6.1	10.0	

Conductivity of He filler gas = 0.18 BTU/hr-ft-°F.

conducted by means of a parametric survey with maximum fuel temperatures and end-of-life pellet-clad interference calculated as a function of effective density and assembled cold-gap thickness. Effective density is defined as the mass of fuel per unit volume inside the fuel rod, which includes the fuel pellet and the radial gap. Effective density is lower than the fuel pellet density by the ratio

$$\left(\frac{\text{pellet diameter}}{\text{clad inside diameter}} \right)^2.$$

All calculations were made with the reference fuel-rod design of 0.265-in. diameter and with a clad thickness of 0.015 in.

The effects of the assembled radial gap and effective density on maximum fuel temperature are shown in Figure 1. The maximum fuel temperature, which occurs at the beginning of life, is strongly dependent upon the gap width because of the inverse relationship between gap conductance and gap width. Since the thermal conductivity of the fuel is quite high, the maximum fuel temperature is not strongly dependent on fuel density, although fuel conductivity is assumed to be directly proportional to fuel density.

Examination of Figure 1 reveals that an assembled gap width less than ~0.0025 in. is required to keep the maximum fuel temperature below 3500°F. Either of the densities shown would be acceptable.

However, as the assembled radial gap thickness is reduced, there is less room available to accommodate irradiation swelling of the fuel pellet, and the end-of-

FIG. 1. Effect of Assembled Radial Gap on Maximum Fuel Temperature.

FIG. 2. Effect of Assembled Radial Gap on End-of-life Pellet-Clad Interference.

life pellet-clad interference increases, as shown in Figure 2. This interference is strongly dependent on effective density because of the swelling model discussed in Section 1.1. An assembled radial gap of greater than 0.003 in. is required to insure no pellet-clad interference at the end of life with the lower effective density (81%). An effective density of 81% and an assembled radial gap of 0.002 in. were chosen to preserve acceptable nuclear characteristics. This corresponds to a pellet density of 83.7% theoretical.

Reference Fuel-rod Characteristics

The selected values of the assembled radial gap width and effective density for the reference fuel rod represent a compromise among the mutually conflicting requirements of breeding ratio, maximum fuel temperature, and pellet-clad interference. The nominal gap thickness of 0.002 in. and the effective density of 81%, chosen for the reference fuel rod, result in a maximum fuel temperature of 3200°F and an end-of-life cold pellet-clad interference of 0.001 in. The corre-

TABLE 2. REFERENCE FUEL-ROD CHARACTERISTICS

Rod outside diameter, in.:	0.265
Clad thickness, in.:	0.015
Rod inside diameter, in.:	0.235
Radial gap thickness, in.:	0.002
Fuel-pellet diameter, in.:	0.231
Effective density, % theoretical:	80.9
Actual fuel density, % theoretical:	83.7
Active core length, ft:	3.0

sponding hot pellet-clad interference at the end of life is 0.0005 in.

The characteristics of the reference fuel rod are listed in Table 2.

2. CORE-BLANKET COMPOSITION AND CONFIGURATION

2.1 Introduction

Analyses have been performed in order to assess the influence of (UPu)C fuel density on reactor character-

TABLE 3. RESULTS OF COMPOSITION-CONFIGURATION SURVEY CALCULATIONS

Case No.	Reactor Type	Smeared (UPu)C Density, g/cm ³	Core Size,* OD × H, ft	Reactor Size, OD × H, ft†	Core Volume, liters	Specific Power, kW/kg fissile	Inventory, kg (49 + 41)	Fissile Enrichment 49 + 41/40 + 28 + 49 + 41	Core Conversion Ratio	Total Breeding Ratio	$-T \frac{dk}{dt}$	Na Void Effect, % $\Delta k/k$		Fraction of Flux below 17 keV
												50% Core	100% Core	
1	Pancake, high-density fuel	12.9 (95% theo.)	10 × 2	12.5 × 5	4448	1200	1830	0.124	0.86	1.50	0.0045	+0.621	+1.25	0.29
2	Pancake, medium-density fuel	11.0 (81%)	10 × 2	12.5 × 5	4448	1260	1749	0.139	0.75	1.44	0.004	+0.687		0.29
3	Pancake, low-density fuel	9.03 (66.5%)	10 × 2	12.5 × 5	4448	1320	1665	0.161	0.64	1.39	0.0035			0.29
4	7 hexagonal core modules	11.0	Hexagonal module, 3 high × 1.7 on side	18 × 6	635 × 7	770	2287	0.182	0.51	1.67	0.002		-1.33	0.24
5	3 rhombic core modules	11.0	4.5 × 4.5 × 3 rhombic module	15 × 6	1483 × 3	1200	1836	0.146	0.70	1.65	0.003		-0.307	0.27
6	Pancake, axial carbide blanket in Case No. 1	12.9	10 × 2	12.5 × 5	4448	1200	1830	0.124	0.84	1.60				
7	Pancake, thick clad in Case No. 1	12.9	10 × 2	12.5 × 5	4448	1180	1870	0.126	0.84	1.48				
8	Pancake, niobium clad in Case No. 1	12.9	10 × 2	12.5 × 5	4448	1060	2066	0.140	0.71	1.19	<0.001			0.21
9	Pancake, vanadium clad in Case No. 1	12.9	10 × 2	12.5 × 5	4448	1220	1800	0.122	0.86	1.50	~0.005			0.35

* Core volume fractions are 27 v/o fuel, 14 v/o clad and structure, and 59 v/o sodium, except for Case No. 7 which contains 27 v/o fuel, 21 v/o clad and structure, and 52 v/o sodium.

† Reactor height does not include fission gas reservoir. Axial blanket composition is same as core. Radial blanket composition is 55 v/o fuel, 20 v/o clad and structure, and 25 v/o sodium. Fuel is UO₂ at 9.5 g/cm³.

TABLE 4. SPECTRAL INDICES FOR CORE REGION

Case	$\sigma_c^{49}/\sigma_f^{49}$	$\sigma_c^{28}/\sigma_f^{49}$	$\sigma_f^{10}/\sigma_f^{49}$	$\sigma_f^{28}/\sigma_f^{49}$
1	0.17	0.14	0.19	0.027
2	0.17	0.14	0.19	0.027
3	0.17	0.14	0.20	0.028
4	0.14	0.13	0.22	0.033
5	0.16	0.14	0.19	0.029
8	0.13	0.13	0.22	0.031
9	0.19	0.15	0.18	0.026

istics in order to permit evaluation of the cost penalty associated with low-density fuel. Calculations have also been performed to determine the effect of a thick clad, as well as the use of niobium rather than stainless steel clad. Low-density fuel and restraint by thick (30 mils) clad provide two approaches to allow for expected fuel swelling at high burnup.

An investigation of reactor configurations was carried out to arrive at a reasonable compromise between economic parameters such as breeding ratio and inventory, on the one hand, and safety parameters, i.e., Doppler coefficient and sodium void reactivity effect, on the other hand.

The calculations were performed with the use of the following cross sections:

1. Fe, Ni, Cr, O, C, Nb 16-group Hansen-Roach Set^(2, 3)
2. U-238 16-group Hansen-Roach as modified by Spielberg⁽¹⁾
3. Pu-239, Pu-240 16-group Hansen-Roach, infinite dilute modified by self-shielding factors generated using methods of Graves.⁽⁵⁾
4. Na 16-group Hansen-Roach with modification of the σ_c in 2.85-keV resonance by data of Hibdon.⁽⁶⁾
5. V 16-group generated using HRG slowing-down calculation.⁽⁷⁾

Further details concerning these cross-section calculations are given in Reference (8).

Critical enrichment and reactivity were calculated by use of the DS₂ approximation to the neutron transport equation by means of the DTF code.⁽⁹⁾ This code is a one-dimensional multigroup program for solving the neutron transport equation by the S_n approximation.

Doppler coefficients were calculated by performing two k -eigenvalue calculations, one at 1000°K and one at 1500°K, and determining $T(dk/dT)$ from these results as follows:

$$T \frac{dk}{dT} = \text{Constant} = \frac{k(1500) - k(1000)}{\ln \frac{1500}{1000}}.$$

For coolant void reactivity effects, two k -eigenvalue calculations were performed at operating temperature, one with full sodium density, and one with 50% or no sodium present, from which the void effect was calculated.

Breeding ratio and spectral indices were calculated directly from DTF output.

2.2 Summary of Results

The results of the various calculations are summarized in Tables 3 and 4, and in Figure 3.

Effect of Core Subdivision

Comparing Cases 2, 5, and 4 of Table 3, it is seen that as the core region is subdivided, critical loading and enrichment increase, and the sodium reactivity effect becomes more negative. These effects are due to increased core leakage and a harder neutron spectrum. The spectral hardening as well as increased enrichment results in a decreased Doppler coefficient.

The conversion ratio in the core decreases as core size is reduced, leading to more frequent refueling shut-downs. In the case of the modular cores, better use is made of the radial blanket, which can contain an appreciably larger fertile material volume fraction than is possible in the core and the axial blanket. (55 vs. 27 v/o fuel).

It is seen that the three-core module reactor represents an attractive compromise between the pancake design (low total breeding ratio, low fissile fuel inventory, modest Doppler, positive sodium void effect) and the seven-core module arrangement (high total breeding ratio, high fissile fuel inventory, small Doppler, negative sodium void effect). The three-core modular arrangement also permits a relatively compact reactor configuration compared to a seven-core modular arrangement. For given core and blanket volumes, a four- or five-module arrangement is less compact.

Effect of (UPu)C Fuel Density

The effect of lower fuel density is seen mainly in an increase in enrichment and a reduction in overall breeding ratio, as seen in Table 3 and Figure 3. The effect of fuel density on total breeding ratio is less intense than it is on core conversion ratio. It is seen from the data of Table 3 that a smeared fuel density as low as ~80% theoretical yields a breeding ratio of 1.65 in the modular core.

FIG. 3. Breeding Ratio vs Fuel Density for 1000-MW(e) (UPu)C Pancake Reactor.

Effect of Thick Clad

A calculation was performed to assess the effect of increasing the thickness of stainless steel clad from 15 to 30 mils. The stainless steel volume fractions corresponding to these two clad thicknesses are 6 and 13 v/o, respectively. Other structure accounts for 8 v/o. It is seen from Table 3 (see Case 7) that the resultant penalty is small: an increase of 50 kg in fissile inventory, and a reduction in breeding ratio of ~ 0.02 .

Effect of Niobium and Vanadium Clad

Cases 8 and 9 in Table 3 show the effect of substituting niobium or vanadium clad for stainless steel. Although the substitution of niobium is not acceptable, that of vanadium is. There is an increase in Doppler coefficient when vanadium is used. These results are in agreement with a similar earlier analysis.⁽¹⁰⁾

The calculations reported to date on the effect of niobium, including the present calculation, make no allowance for self-shielding of the niobium resonances. Since the niobium resonances lie just above the important U-238 resonances, it is expected that proper treatment of the niobium resonances could appreci-

ably alter the predicted effect of niobium on fast breeder performance.

Effect of UO_2 Versus UC Blanket

Comparison of Cases 1 and 6 of Table 3 indicate the effect of substituting UC for the UO_2 in the axial blanket of the pancake reactor. The core inventory and conversion ratio are essentially unchanged, while the total breeding ratio is increased by 0.1. This is a significant increase; however, as discussed in Section 4.2, it does not lead to a decrease in fuel-cycle costs due to the currently postulated higher fabrication costs of UC vs. UO_2 . Changeover to a carbide blanket is available at a relatively small cost penalty (~ 0.04 mill/kWhr(e)) to raise the overall breeding ratio by ~ 0.1 should it be necessary to soften the spectrum in the core to enhance the Doppler coefficient.

3. REACTIVITY TRANSIENTS

3.1 Introduction

Response of (PuU)C to transients caused by reactivity insertion was examined to evaluate the effect of

FIG. 4. Comparison of Carbide and Oxide Fuel Rods.

relatively low initial operating temperatures, and the effect of high conductivity and thermal diffusivity of the fuel. This behavior was compared to $(\text{PuU})\text{O}_2$ fuel under similar conditions of reactivity insertion to assess the differences in behavior between the two fuels.

The major contributor to this different response is the thermal conductivity, which is about 11.6 BTU/hr-ft-°F for $(\text{UPu})\text{C}$ and about 1.5–2.0 BTU/hr-ft-°F for $(\text{UPu})\text{O}_2$. Figure 4 illustrates the difference in steady-state temperatures between carbide and oxide fuel at 10 kW/ft, and the difference in maximum heat generation per unit length corresponding to incipient melting.

The difference in thermal conductivity is also reflected in a difference in thermal diffusivity, which is about 0.22 ft²/hr for the carbide and 0.032 ft²/hr for

the oxide. This means that for the carbide the heat is released to the coolant much faster.

An investigation was made of the response of the reference carbide fuel rod described in Section 1 and a typical oxide-fueled core⁽¹¹⁾ to transients produced by two reactivity insertions. A total reactivity of \$1.40, which is typical of the amount which might be inserted by the withdrawal of one group of control rods, was inserted at rates of \$2.00/sec and \$0.20/sec. Table 5 shows the kinetics parameters for these problems which were run on the coupled kinetics-heat transfer code, SCARE, written at United Nuclear Corporation, which treats an individual fuel rod and its associated coolant. The Elrod lumped-parameter formulation^(12, 13) is used as the transient heat transfer model for the radial conduction in the fuel rod. The set of simultaneous

TABLE 5. PARAMETERS FOR REACTIVITY TRANSIENTS CALCULATIONS

Total Reactivity Insertion, \$: 1.40		
Reactivity Insertion Rates, \$/sec: 2.00 and 0.20		
	Fuel	
	(UPu)C	(UPc)O ₂
Neutron Lifetime l , sec $\times 10^7$	4.0	5.7
Effective Delayed-neutron Fraction β_{eff}	0.0040	0.0032
Doppler Coefficient Constant $-T(dk/dt)$	0.010	0.010
Doppler Coefficient, at Initial Average Fuel Temperature, $^{\circ}\text{F}^{-1} \times 10^6$	-4.2	-3.5
Initial Average Fuel Temperature, $^{\circ}\text{F}$	1940	2400
Average Power Rating, kW/ft	18.0	10.4

first-order differential equations which are obtained by coupling the reactor kinetics equations and the heat transfer equations with the temperature coefficients of reactivity are numerically solved by a modified Runge-Kutta finite-difference technique.⁽¹⁴⁾

3.2 Summary of Results

Reactivity Transients in (UPu)C Fuel

Figures 5 through 7 summarize the results of the reactivity transients analyses performed for (UPu)C fuel. In all cases, the total reactivity insertion was \$1.40, introduced at rates of \$2.00/sec and \$0.20/sec. As seen from Figure 7, the maximum fuel temperature stays well below the melting point of 4500°F. The sodium coolant does not boil.

FIG. 5. Reactivity Insertion Accident—Net Excess Reactivity—Carbide Fuel.

FIG. 6. Reactivity Insertion Accident—Power Response—Carbide Fuel.

Reactivity Transients in $(UPu)O_2$ Fuel

The results of reactivity transients analyses for $(UPu)O_2$ fuel are summarized in Figures 8 through 10. The reactivity insertion, reactivity insertion rate, and Doppler coefficient are the same as in the case of the carbide. From Figure 10 it is seen that the oxide melting point of $5000^\circ F$ is reached in about one second for the fast transient and in about 5 sec for the slow transient. Again, as in the case of the carbide, the sodium coolant does not boil under these conditions.

Comparison between Transients in $(UPu)C$ and $(UPu)O_2$; Conclusions

In general, for a given reactivity insertion, reactivity insertion rate, and Doppler coefficient, maximum fuel temperatures attained in a $(UPu)C$ fuel rod are sig-

nificantly below those attained in a $(UPu)O_2$ fuel rod. This difference between carbide and oxide indicates that a lower Doppler coefficient may be tolerated for the carbide fuel. The results also indicate that a Doppler coefficient much less than $0.010 [T'(dk/dt)]$ will not be sufficient to override a total insertion of \$1.40 without resulting in fuel melting.

The effect of sodium void coefficient was factored in by repeating the above transient calculation with negative, zero, and positive void coefficients. The results indicated essentially no effect on fuel and cladding temperatures at 20 sec, since boiling did not take place in any of the cases considered. A negative void reactivity effect is considered particularly important for a carbide-fueled reactor because a larger amount of heat is deposited in the coolant in the earlier stages of the

Fig. 7. Reactivity Insertion Accident—Temperatures in the Average Fuel Rod—Carbide⁵Fuel.

transient, which could lead to boiling for large reactivity insertions (more than \$1.40).

4. FUEL-CYCLE ANALYSIS

4.1 Introduction

Fuel-cycle costs have been calculated in order to assess the economic effect of the following variations of design parameters:

1. the effect of reduced (UPu)C fuel density, in the range from 95% to 66.5% of theoretical density;
2. the effect of going from one single 1000-MWe

pancake geometry core to a subdivided core consisting of three or seven core modules;

3. the effect of specific power (kW/kg fissile at constant kW/ft);

4. the effect of plutonium value and working capital charges reflecting conditions under private fuel ownership;

5. the effect of burnup;

6. the effect of using a carbide rather than oxide axial blanket;

7. the effect of using a thick clad, which may be desirable to restrain fuel swelling, and other clad materials, such as niobium and vanadium.

FIG. 8. Reactivity Insertion Accident—Net Excess Reactivity—Oxide Fuel.

A number of significant economic parameters, particularly relating to fabrication, processing, and shipping costs, can at this time be estimated only with considerable uncertainty. In Table 6 are listed the parameters used in the present study. These are present "best" estimates and are consistent with other recent analyses.^(11, 15, 16) Although the absolute magnitudes of fuel-cycle costs thus represent a projection with some uncertainty, the differences between the various cases, however, should reflect fairly reasonably the economic effects of design parameters in question.

4.2 Summary of Results

Effect of Fuel Density

Table 7 summarizes the fuel-cycle costs obtained for the 1000-MWe (UPu) C-fueled pancake reactor at fuel densities of 95, 81, and 66.5 percent of theoretical (see

Table 3, Cases 1, 2, and 3). In these reactors, both blankets are fueled with UO_2 , and the residence time of the radial blanket is twice that of the core and axial blanket. It is seen that going from 95% to 81% theoretical density increases fuel-cycle costs by a factor of 1.3; going from 95% to 66.5% increases costs by a factor of 1.65. Sixty-six and one-half per cent (66.5%) is an unrealistically low density. At a density of about 80%, fuel-cycle costs can be kept below 0.5 mill/kWhr for the pancake core geometry.

Modular Versus Pancake Geometry

The principal economic advantage of a modular versus a pancake core geometry is more efficient utilization of the radial blanket. The radial blanket may contain about twice the fuel volume fraction than the core, while the axial blanket must contain the same fuel volume fraction as the core so long as the core and axial

FIG. 9. Reactivity Insertion Accident Power Response—Oxide Fuel.

blanket form integral fuel assemblies. This results in a higher breeding ratio in the blanket for modular geometry.

A second advantage is that the radial blanket can have a longer residence time than the core. It does not have to be moved every time core fuel is changed. The axial blanket, on the other hand, generally has to be moved more frequently than is desirable, since it is integral with the core. This results in reduced radial blanket fabrication, reprocessing, and shipping costs per unit energy generated in the core, in the case of modular geometry.

A third advantage is that since the volume of a single core module is considerably smaller than the volume of a single core region reactor, the total breeding ratio is increased because of a somewhat harder spectrum.

There are two principal economic disadvantages with a modular core. First, the fissile fuel inventory is increased as core volume is decreased and, secondly, the core conversion ratio is reduced. Table 3 summarizes the physics data for a pancake, three-module, and seven-module reactor (Cases 2, 5 and 4). The fuel inventories are 1749, 1836, and 2287 kg fissile plutonium, respectively; the core conversion ratios are 0.75, 0.70,

FIG. 10. Reactivity Insertion Accident—Temperatures in the Average Fuel Rod—Oxide Fuel.

and 0.51, respectively; the total breeding ratios are 1.44, 1.65, and 1.67, respectively.

Fuel-cycle costs for the three-module and the seven-module reactors are summarized in Table 8. It is seen that the total fuel-cycle cost is estimated at 0.21 mill/kWhr, a reduction of more than 50% over the 0.44 mill/kWhr obtained for the pancake geometry. Comparing Case B of Table 7 and the data in Table 8, it is seen that for the modular case, core inventory costs have increased to 0.297 from 0.283 mill/kWhr, and core burnup has increased to 0.291 from 0.242 mill/kWhr. This is, however, more than offset by the increase of blanket breeding gain to -0.92 from -0.67 mill/kWhr.

As is also seen from Table 8, the fuel inventory costs are beginning to hurt more in the case of the seven-

module reactor, resulting in a 57% increase in fuel-cycle cost over the three-module reactor. This fact, plus the fact that the seven-module reactor has a smaller Doppler coefficient (see Table 3), and larger volume of core plus blanket, makes it undesirable. The three-module core, on the other hand, has the advantages of low fuel-cycle cost, compactness, and negative core sodium void effect, and shows only a moderate decrease in Doppler coefficient.

Effect of Specific Power

For a given core composition and power output, increasing the specific power (kW/kg fissile) at constant linear power generation will decrease the core volume, increase inventory, decrease fuel-rod diameter, and change the breeding ratio.

Figure 11 illustrates the effect of changing specific power on fuel-cycle costs for the three-module reference reactor. The data in this figure correspond to a constant maximum heat generation rate of 30 kW/ft, but variable fuel-rod size and variable core volume. There is some uncertainty on the variation of fabrication cost with fuel-rod diameter. In the range of interest, down to a diameter of about 0.2 in., it is expected that the fabrication cost will vary between costs proportional to (diameter)⁻¹ and (diameter)^{-1/2}. It is seen from Figure 11 that high specific power results in significant reduction of fuel-cycle costs. It appears that under conditions of high working-capital charges, such as are expected under private fuel ownership, one should push for as high specific power as is metallurgically feasible.

Unless fuel fabrication becomes actually impractical, fabrication costs are a secondary consideration. The calculations represented by Figure 11 neglect the effect of specific power on refueling interval, since no adequate information is available at this time on the period between refueling shutdowns. The core with higher specific power will have a lower core breeding ratio, and therefore more frequent refuelings, for a given amount of available control.

Effect of Plutonium Value, Working-capital Charges, and Burnup

Figure 12 illustrates the effect of fissile plutonium value on fuel-cycle costs. The modular core, having a higher breeding ratio than the pancake, shows a more pronounced cost variation with plutonium value. In the

TABLE 6. PARAMETERS USED IN FUEL-CYCLE COST ANALYSIS [(UPu)C Reference Case, 81% Theoretical Density]

Core Fabrication	\$200/kg U + Pu*
Core Processing	\$90/kg U + Pu
Blanket Fabrication (oxide)	\$60/kg U
Blanket Fabrication (carbide)	\$100/kg U
Blanket Processing (oxide)	\$30/kg U
Blanket Processing (carbide)	\$50/kg U
Core Shipping	\$24/kg U + Pu
Axial Blanket Shipping	\$24/kg U
Radial Blanket Shipping	\$12/kg U
Core Burnup	100,000 MWD/MT (U + Pu)
Thermal Efficiency	40%
Load Factor	0.8
Plutonium Value	\$10/gm fissile
Working Capital Charge, Fuel Inventory	10% per annum
Working Capital Charge, Fabricated Fuel	10% per annum
Fuel Losses	Included in above costs

* (For a 0.26-in.-OD rod).

TABLE 7. EFFECT OF (UPu)C FUEL DENSITY ON FUEL-CYCLE COSTS (1000-MWe Pancake Geometry Reactor, Costs in Mills/kWhre)

	Core	Axial Blanket	Radial Blanket	Sum
A. 95% Theoretical Density*				
Fabrication	0.178	0.073	0.028	0.279
Processing	0.094	0.036	0.014	0.144
Net Burnup (or Breeding)	0.133	-0.445	-0.162	-0.474
Fuel Inventory	0.297	—	—	0.297
Shipping	0.025	0.029	0.005	0.059
Capitalization	0.020	0.008	0.004	0.032
	0.75	-0.30	-0.11	0.34
B. 81% Theoretical Density*				
Fabrication	0.209	0.085	0.036	0.330
Processing	0.094	0.042	0.018	0.154
Net Burnup (or Breeding)	0.242	-0.494	-0.174	-0.426
Fuel Inventory	0.283	—	—	0.283
Shipping	0.025	0.034	0.007	0.066
Capitalization	0.020	0.008	0.004	0.032
	0.87	-0.32	-0.11	0.44
C. 66.5% Theoretical Density*				
Fabrication	0.254	0.104	0.041	0.399
Processing	0.094	0.052	0.021	0.166
Net Burnup (or Breeding)	0.356	-0.544	-0.198	-0.386
Fuel Inventory	0.270	—	—	0.270
Shipping	0.025	0.042	0.008	0.075
Capitalization	0.020	0.008	0.004	0.032
	1.02	-0.34	-0.12	0.56

* Smeared density with cold ID of fuel tube. Theoretical density taken as 13.6 g carbide/cc.

long term, plutonium value is expected to go up. This would favor the higher breeder.

The variation of fuel-cycle costs with working-capital charges and burnup is shown in Figures 13 and 14. Both variables have a strong influence on costs.

It is seen that an average burnup of 100,000 MWD/MT is a reasonable design objective. Beyond this value, increased burnup begins to yield diminishing cost reductions.

It is seen that fuel-cycle costs are extremely sensitive to the average burnup achieved, especially for the modular core, for which a relatively high breeding ratio is predicted. The sensitivity of fuel-cycle cost to burnup at discharge becomes even stronger at plutonium values greater than 10 \$/g fissile.

TABLE 8. FUEL-CYCLE COSTS OF THREE- AND SEVEN-MODULE (UPu)C-FUELED 1000-MWe REACTORS
[Costs in Mills/kWh (UPu)C Fuel at 81% Theoretical Density]

	Core	Blanket	Sum
A. Three-core Modules			
Fabrication	0.209	0.112	0.321
Processing	0.094	0.056	0.150
Burnup/Breeding	0.291	-0.917	-0.626
Inventory	0.297	—	0.297
Shipping	0.025	0.011	0.036
Capitalization	0.020	0.015	0.035
	0.94	-0.73	0.21
B. Seven-core Modules			
Fabrication	0.209	0.156	0.365
Processing	0.094	0.078	0.172
Burnup/Breeding	0.470	-1.121	-0.651
Inventory	0.370	—	0.370
Shipping	0.025	0.016	0.041
Capitalization	0.020	0.016	0.036
	1.19	-0.86	0.33

Carbide Versus Oxide Blanket

In the (UPu)C-fueled reactors under discussion, both the radial and axial blankets are taken as of UO_2 rather than as of UC because of somewhat lower anticipated fabrication costs for UO_2 . In the case of the integral core-axial blanket-fuel assemblies, use is made of inert ceramic pellets to avoid interaction between oxide and carbide pellets at the core-blanket interface.

For the 95% theoretical density, (UPu)C-fueled pancake core, use of a UC axial blanket increases the total breeding ratio from 1.50 to 1.57. Assuming UC fabrication costs of \$100/kg and processing costs of \$50/kg, the result is a net fuel-cycle cost increase of 0.04 mill/kWh over the oxide blanket.

Effect of Clad Thickness and Material

As shown in Table 3, the effect of increasing the stainless steel-clad thickness from 15 to 30 mils for the 95% theoretical density, (UPu)C-fueled pancake reactor is to decrease the total breeding ratio by 0.02, and increase enrichment by 0.2%. The clad thickness is increased at the expense of sodium. The resultant change in fuel-cycle costs is less than 0.005 mill/

FIG. 11. Variation of Fuel-cycle Cost with Specific Power [333-MW(e) (UPu)C-fueled Core Module, Constant Linear Power at 30 kW/ft max, Variable Core Volume and Enrichment].

FIG. 12. Variation of Fuel-cycle Costs with Plutonium Value as a Function of (UPu)C Fuel Density.

kWhre. Thus, a thicker stainless steel clad, which may be desirable to restrain fuel swelling or fission gas pressure, appears economically feasible.

Table 3 also presents data on the effect of using niobium clad: the breeding ratio is reduced appreciably, and the enrichment is significantly increased. This, plus considerations of safety and niobium cost, makes the use of niobium appear undesirable, as has also been shown in previous studies.⁽¹⁰⁾ However, as already noted above in Section 2, the present treatment of the niobium cross sections is somewhat inadequate, and more refined calculations could yield different results.

The use of vanadium as a cladding material is satisfactory from a nuclear point of view, as is shown by Case 9 of Table 3. Costs due to the use of vanadium cladding cannot be calculated with any accuracy at this time since vanadium tubing is not available in commercial quantities. Again, the effect of vanadium is in agreement with previous work.⁽¹⁰⁾

5. SUMMARY OF CONCEPTUAL REFERENCE DESIGN

The characteristics of the 1000-MWe (UPu)C-fueled conceptual reference design developed in the present study are summarized in Table 9. Figure 15 is a schematic sketch of the reactor arrangement, con-

sisting of three rhombic-shaped 1500-liter core modules. Each module consists of fuel rods with 3-ft core section, two 1.5-ft axial blanket regions, and a fission gas reservoir above the top axial blanket region. The core modules are separated by 3 ft of radial blanket and stainless steel structure.

The present configuration was selected because of the following principal considerations:

1. The rhombic, 1500-liter core modules represent a reasonable compromise in attempting to achieve attractive fuel inventory and breeding ratio while maintaining satisfactory safety characteristics. The reactivity effect due to 100% loss of sodium from the core regions is negative. A good peak/average power ratio is also obtained without fuel zoning.

2. The total breeding ratio is increased considerably over that of a single pancake reactor of equal core volume due to higher and more efficient radial blanket breeding. The result is a fuel-cycle cost of 0.21 mill/kWhre versus 0.44 mill/kWhre for the pancake design.

3. Each of the three core modules is neutronically decoupled from the other core modules throughout core life by the presence of 3 ft of radial blanket and steel structure between modules. The configuration is sufficiently compact to fit within an external blanket diameter of ~15 ft. Decoupling the modules is expected to simplify reactor control and significantly reduces the

FIG. 13. Variation of Fuel-cycle Costs with Working Capital Charges as a Function of (UPu)C Fuel Density.

potential hazard of fuel meltdown. The presence of blanket between modules permits incorporation in the core of rigid structure midway between modules for protection against compaction of the entire fuel in the reactor. This structure could be extended above and below the core to provide for three separate flow loops. Such an arrangement would minimize the severity of flow blockage in a given fuel cluster over a pancake core supplied with flow from a single plenum.

4. A disadvantage of the modular core is a reduced Doppler coefficient. The Doppler coefficient for the present 1500-liter module is about 25% smaller than for a 4500-liter pancake core. The significance of the Doppler coefficient has to be assessed for specific reactivity transients, and its magnitude is presently subject to considerable uncertainty. If it turns out that the available Doppler shutdown is inadequate, even in the

presence of favorable sodium void effects, the spectrum may have to be suitably softened. The modular core would continue to have a breeding ratio advantage over a pancake core with moderator such as BeO added to soften the spectrum. Furthermore, the initially high breeding ratio achievable with a modular carbide core, and possibly carbide blankets, should result in a breeding ratio in excess of 1.40 with a softened spectrum.

5. Core height in the present design is chosen as low as possible consistent with acceptable specific power and overall reactor diameter. The reference height of 3 ft for the core and of 1½ ft for axial blankets results in an overall fuel-rod length of 6 ft exclusive of fission gas volume. This is only 1 ft longer than the pancake design.

As is discussed in Section 1, the reference fuel-rod

FIG. 14. Variation of Fuel-cycle Costs with Burnup as a Function of (UPu)C Fuel Density.

TABLE 9. CHARACTERISTICS OF 1000-MWe (UPu)C
CONCEPTUAL REFERENCE DESIGN

Reactor Power	1000 MWe
Thermal Efficiency	40%
Reactor Configuration	3 Rhombic Core Modules, 4.5 by 4.5 by 3 in. High, plus Blankets
Reactor Size	15-in. OD; 6 in. High
Module Power	733 MWt
Fuel Inventory in 3 Modules	1836 kg Pu-239 and Pu-241
Enrichment, Fissile	14.6%
Average Core Power Density	495 kWt/liter
Average Specific Power	1200 kW/kg fissile
Core Conversion Ratio	0.70
Total Breeding Ratio	1.65
Doubling Time	~7 years
Maximum Fuel Rating	30 kW/ft
Peak/Average Power Ratio	1.68
Coolant Inlet Temperature	800°F
Coolant Outlet Temperature	1100°F
Maximum Fuel Temperature	3200°F
Doppler Coefficient	-0.003 $T(dk/dt)$
Sodium Void Worth (100% core module)	-0.31% $\Delta k/k$
Burnup	100,000 MWD/MT
Fuel Residence Time	2.2 years
Load Factor	0.8
Fuel-cycle Cost	0.21 mill/kWhre
Fuel Material	(UPu)C
Fuel Density (smeared over cold clad ID)	11.9 gm/cc (81% theoreti- cal)
Fuel-rod-clad OD	0.265 in.
Clad Material	Type 316 SS
Clad Thickness	0.015 in.
Thermal Bond Material	Helium
Cold Gap Width	0.002 in.
Cold Pellet OD	0.231 in.
Core Composition, v/o Fuel/ Sodium/Structure	27/59/14
Blanket Material	UO ₂
Blanket Thickness	1.5 ft
Radial Blanket Composition, v/o Fuel/Sodium/Structure	55/25/20

characteristics and smeared fuel density of 81% theoretical is based on presently postulated fuel swelling to 100,000 MWD/MT and the assumption that internal void will absorb this swelling. This is subject to verification through irradiation testing in a fast-flux environment. Present swelling data are based on thermal-flux irradiation tests. As has been shown in Section 4, increasing clad thickness from 0.015 to 0.030 in. will have a negligible effect on fuel-cycle costs. Thus, a thicker clad than the reference 0.015 in. may be used to restrain fuel swelling if it is verified that cladding embrittlement does not occur under fast-flux conditions. This might permit the use of smeared (UPu)C fuel densities closer to 95% of theoretical, with associated reduction in fuel-cycle costs (see Figure 12).

FIG. 15. Schematic Sketch of 1000-MW(e) Modular (UPu)C Reactor Configuration (Core Height 3 ft, Axial Blankets 1½ ft Each, Fission Gas Reservoir).

It is evident that these possibilities must await the results of extensive fast-flux irradiation testing.

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Discussion

Mr. Campise (AI): Was the Doppler coefficient you showed in your table the Doppler of the core only or was it the isothermal Doppler for the module?

Mr. Sofer: The Doppler for the module.

Mr. Campise: I find this strange because most of the modular studies have shown an increase in the Doppler coefficient by going to a blanketed module, like in the Argonne study a while back.

Mr. Sofer: The modules are neutronically separated so that really the modules behave as if they were cores by themselves. These cores are 1500 liters each, and the spectrum of a 1500-liter core is certainly harder than one of much larger volume. This is why the Doppler is low.

Mr. Dalle-Donne (Karlsruhe): Don't you think that the problem of the heat transfer coefficient between the fuel and the cladding is very important for carbides because of their very good thermal properties, particularly the thermal conductivity? In other words, you put something which has a very bad heat transfer coefficient in series with something which is very good. Should you not try to use sodium bonding?

Mr. Sofer: We recognize this as being a definite problem. We're not ignoring it. The temperature drop across the gap is not negligible. However, there is this revealing aspect to it in that as the fuel temperature goes up, expansion will take place and tend to close the gap. Now the problem is: can we get it so that we don't overly strain the clad as a result of expansion and swelling? Sodium bonding is one way to do it, but we have problems with that, too.

Mr. Cobb (Nuclear Power Group, England): In Fig. 1 you show that for a 5% change in carbide density you got about only a 2% change in the center fuel temperature. Could you say how much the actual change in conductivity was; presumably the gap-closing effect is coming in here?

Mr. Sofer: I think I understand your question to be as follows: What is the change in conductivity of the fuel as we go from the pellet density of, say, 95% to 85%? There are limited data on the subject, but we assumed in our calculations that the conductivity is proportional to the density.

Mr. Chernick (BNL): Have you assumed that the carbide fuel remains intact? I did notice some very large burnup figures, like a 100,000 MWd/ton. What is going to happen when fission products get out and gases get mixed with the helium? This is a known problem with thermal reactors.

Mr. Sofer: We really cannot be presumptuous on this matter. There have been all kinds of concern as to what happens really when carbide cracks. It has a nice

conductivity as a solid pellet. What happens to that conductivity when it cracks? Radiation data we have obtained operating at 20 kW/ft indicated that the conductivity doesn't drop appreciably. There was no grain growth and no sintering, which indicates that the conductivity somehow seems to maintain itself.

Mr. Chernick: I'm really more concerned about the conductivity of the gas, not the pellet itself. The small amount of the fission gases does seem to ruin the conductivity of helium.

Mr. Sofer: Yes, there have been data showing that the conductivity of the gas tends to drop when fission gas enters, but again we have to really go on the basis of experimental evidence.

Mr. Bradbury (Harwell): For what percentage of fission gas release from the fuel are you designing?

Mr. Sofer: At these burnups we would prefer to design for total release.

Mr. Kummerer (Karlsruhe): What is the reason that you consider mainly niobium cladding as a substitute for Type 316 stainless steel instead of the vanadium-titanium alloy?

Mr. Sofer: The reason I brought niobium into the picture was because the niobium behaves so well. People have always asked: "Well, now why can't you use niobium?" The answer is that, at least if the Hansen-Roach cross section for niobium is used, there is produced a rather detrimental effect on breeding ratio. We are now planning some pins with vanadium. One problem with vanadium is cost.

Mr. Kummerer: Is niobium material available in tubing?

Mr. Sofer: Niobium is costly, too. Of course, the preferred choice is stainless steel.

Mr. Bump (ANL): Do you have a reference for the conditions under which you observe the cracking in the stainless steel cladding?

Mr. Sofer: The conditions were 100,000 MWd/ton of burnup, 20 kW/ft, temperatures of the pellet were up to about 2400°F, and there was evidence of embrittlement of the cladding and, therefore, cracking of the cladding. We have been hopeful that the reason for this embrittlement was the presence of low amounts of boron in the cladding, which in a thermal flux may have resulted in helium precipitation which led to embrittlement. There is some evidence that small amounts of boron will lead to embrittlement in high-temperature radiation in thermal flux, supported by data from the British, but we will have to find out experimentally what happens in a fast spectrum.

Mr. Bump: What is your attitude concerning the effect of the low smear density of the fuel with regards to safety, particularly slumping?

Mr. Sofer: We are ignorant like everybody else is.

A Re-examination of Design Factors for a 1000-MWe Fast Breeder Reactor

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1. Introduction

The conflicting requirements encountered in designing a large fast breeder reactor which will have good safety characteristics while maintaining attractive fuel-cycle costs and a low fuel doubling time were well demonstrated in four 1000-MWe Large Fast Breeder Design studies.⁽¹⁻⁴⁾ Each of the organizations undertaking the studies worked independently under ground rules which, except for the type of fuel, were common to all. Each organization produced a design which represented a reasonable solution to the conflicting design requirements. These designs provide an opportunity for evaluating the merits of four different approaches to the design of a large fast breeder reactor.

2. Major Conclusions of Studies

We have reviewed the alternative approaches to the Allis-Chalmers annular core to see if they offer major advantages. Before proceeding, we feel that certain general conclusions can be drawn from the four design studies.

A major question prior to the studies was the feasibility of designing a fast reactor core capable of producing 1000 MWe (2500 MWt); i.e., could such a core be designed to have reasonable safety characteristics and, if so, would the necessary design measures make the core unattractive in terms of breeding ratio or fuel-cycle economics? We believe this question of the feasibility of large breeder reactors was answered affirmatively by each of the studies. The Allis-Chalmers annular core, the General Electric "pancaked" cylinder, and the more conventional cylinder of Combustion Engineering are each a single core capable of producing 1000 MWe. The weakly coupled modules which make up the Westinghouse core may raise semantic arguments, but the Westinghouse concept accomplishes the production of 2500 MWt within a single space envelope and may also be thought of as a single core. Each of the cores exceeded the design criteria of a breeding ratio equal to 1.2, and fuel-cycle costs were low for all designs.

Insofar as reasonable safety characteristics can be taken to mean a minimal reactivity gain on complete loss of core sodium, this was met by each of the design studies.

We believe that conclusions may also be drawn from the fuel-cycle costs shown in the design studies. The fuel-cycle costs reported in each study have been normalized to common fuel fabrication, shipping, and reprocessing procedures.⁽⁵⁾ Further normalization on the basis of consistent physics data has been performed⁽⁶⁾ to provide comparable values for the breeding ratio and hence the value of plutonium credit for each system. Fuel-cycle costs after these normalizations are generally low and range from a minimum of 0.43 mill/kWh to a maximum of 0.85 mill/kWh. Demonstration of high burnups for the ceramic fuels through continuing research programs and the successful development of commercial-scale fabrication and processing procedures for mixed uranium-plutonium ceramics used in a sodium environment are, of course, prerequisites for these low fuel-cycle costs. However, achievement of fuel-cycle costs well under 1 mill/kWh seems a definite possibility and points to the strong economic attractiveness of large fast breeders. That such low fuel-cycle costs were achieved when the designs were based to a large extent on safety considerations and in general did not represent economic optimization even under assumed safety criteria, indicates room for further improvements in fuel-cycle costs.

The relative fuel-cycle costs shown in the studies also indicate that the particular core concept selected for the first-round large-size fast breeder will have a small effect on total power costs. For example, in the design studies in which four substantially different core concepts were presented, the fuel-cycle cost difference between the highest and lowest cost concept was approximately 0.4 mill/kWh. Of this, approximately 0.2 mill/kWh could be attributed to the higher breeding gain and hence plutonium credit achieved through use of carbide rather than oxide fuel. The remaining 0.2-mill/kWh cost difference was due to differences in core configuration. On the basis of capital

costs of \$125/kWe, total power costs of the order of 3.5 mills/kWh have been estimated.⁽⁵⁾ Thus, for these design studies, the effect of core concept as reflected in fuel-cycle costs amounts to only about 5% of the total power costs. Fuel-cycle economics is a major factor in the design of a large fast reactor core. However, due to the minor dependence of fuel-cycle costs on core concept, we think that proof of the attractiveness of fast reactors for large central station power production will be best accomplished if core design features of the first round of large fast reactors are selected with primary emphasis on attaining plant reliability and safety. Thus, simplicity of design, the inclusion of features which promote safety under both normal and abnormal operating conditions, and selection of design features to as large an extent as possible of the basis of proven technology should be main design considerations.

3. Safety Considerations

The basic core concept of each of the design studies was dictated to a large extent by the requirement for attaining satisfactory sodium voiding properties. Consequently, the designs do not achieve as high a plutonium specific power and internal conversion ratio or as low fuel-cycle costs and fuel doubling time as would be achieved with a cylindrical core having a ratio of length to diameter (L/D) near unity. However, we believe that the major emphasis given to sodium voiding properties was fully justified. Further improvements in fast reactor performance may come about through reductions in conservatism of the present designs resulting from better physics data and hence a greater certainty in calculated values of sodium and Doppler coefficients. Additional accident analysis and safety testing, particularly of the effects of transient partial sodium voiding, may also allow reductions in present conservative design criteria.

The specific criteria which were applied to sodium voiding reactivity effects differed in the various studies. A design goal of our study was the attainment of zero or only small reactivity gain on complete loss of core sodium. The maximum reactivity gain resulting from sodium voiding, i.e., in central core regions only, was in excess of one dollar. In another of the design studies, design goals were to keep the maximum partial voiding reactivity effect under one dollar and obtain a negative effect for complete loss of core sodium. In the other two studies, reactivity gain due to complete core voiding and the maximum reactivity gain due to partial voiding were both in excess of one dollar. No answers as to what constitutes acceptable criteria are readily apparent. Hopefully, they will become apparent as detailed

accident studies of various reference designs are carried out.

For current design studies, however, we prefer to retain the criterion that core voiding shall result in zero or only small reactivity gain. This criterion does not eliminate all problems of sodium voiding, since maximum partial voiding reactivity effects may still be high. It at least, however, ensures that large cores have equivalent sodium voiding properties as small cores and minimizes the safety problems of expanding core sizes from those associated with a prototype to a full 1000-MWe design. Other minimum criteria applicable to more realistic cases of sodium voiding are (1) partial or complete loss of core sodium from a single fuel channel shall have a negligible reactivity effect and (2) progressive loss of sodium from the top of the core downward, including the effect of upper blanket (or reflector) voiding, shall at all times be negative.

Two of the design studies were based on use of oxide fuel; the other two were based on carbide. Present discussions of use of mixed oxides vs. carbides are primarily centered on (1) improvements possible through use of carbide in breeding ratio and power density versus (2) the advantage of oxide due to its more advanced technology. Although we believe the latter argument carries more weight at present, a clearcut choice of fuel material is not possible. It is worthwhile considering whether either fuel has a distinct safety advantage.

Present design of oxide fuel elements is based on operation of the fuel with the maximum central temperature just below the melting point. In present designs for carbide fuel, the maximum central temperature is well below the melting point. Thus, carbide fuel is capable of greater Doppler compensation in going from operating temperatures to the melting point. This does not necessarily result, however, in a greater allowable reactivity insertion to the point of fuel failure for carbide cores. TREAT tests⁽⁷⁾ of unirradiated UO₂ specimens have indicated that clad integrity could be maintained for transients when fuel temperatures reached 7000°F. For full-power flow conditions and short-period transients corresponding to those of a fast reactor excursion, analytic studies⁽⁸⁾ have shown that high fuel vapor pressure due to fuel temperatures in excess of 6500°F may be the most likely mechanism for fuel failure in gas-bonded oxide fuel pins.

Sodium-bonded carbide fuel is presently receiving the greatest amount of attention. With this type of fuel, generation of high sodium vapor pressure in the bond may be a possible failure mechanism. Recent TREAT⁽⁹⁾ tests with sodium-bonded UC fuel re-

TABLE 1. CALCULATIONS FOR ANNULAR CORE

Volume, liters	Core OD,* ft	Enrichment, % (Fissile Pu/U + Pu)	Power Peaking Factor, Max/Avg
7820, Original Design Study	16	17.95	1.52
7820, Present Study	16	17.82	1.52
6470	14	17.81	1.53
4990	12	17.77	1.54

* Including 18 in. blanket.

sulted in high fuel dispersal throughout the test capsule for a transient in which the maximum measured temperature was believed to be 5700°F. It was concluded for this test that local sodium pressures may have provided the mechanism for fuel dispersal. Further testing work on transients is required for both oxides and carbides, particularly in defining the failure mechanism for sodium-bonded carbide during short-period transients. On the basis of present data it appears that sodium-bonded carbide fuel may fail at a lower temperature than gas-bonded oxide fuel. Therefore, the Doppler compensation to the point of fuel failure may not be any greater for carbide than oxide.

There is no clearcut advantage to oxide or carbide in sodium voiding reactivity effects. The positive spectral component of the void coefficient is higher for carbide due to the higher ratio of fertile to fissile atoms. This tends to be counteracted by the smaller core volume due to the higher power density possible through use of carbide, and hence greater magnitude of the negative leakage component of the void coefficient. Full exploitation of the high-power-density capability of carbide could yield an advantage to carbide but was not clearly demonstrated in the design studies.

The shorter time constant of carbide fuel (approximately $\frac{1}{4}$ that of oxide as given in two of the 1000-MWe design studies) may lead to disadvantages under some conditions. For example, following reactor scram and in the absence of rapid flow reduction, the core outlet temperature of the sodium will decrease at a rate determined primarily by the fuel-element time constant. Thus, thermal stresses in the hot leg of the primary circuit will be more severe for a carbide-fueled core. In considering the possibility of partial sodium voiding near the center of the core, a short time constant may also be a disadvantage. If reactivity is inserted when reactor power level and flow rate are both low, sodium boiling can be initiated at the core center, where high positive reactivity feedback is possible. Boiling at the core center is enhanced by a short time constant of the fuel.

It may be concluded from these considerations that safety characteristics of cores with oxide and carbide fuel will probably be somewhat different and will both have to be studied in greater detail. At present, neither fuel appears to offer a significant safety advantage over the other.

4. Comparison of Core Geometries*

The most significant difference among the four 1000-MWe studies was that of core geometry. In order to decrease the positive reactivity effect of removal of core sodium, the Allis-Chalmers annular core, the General Electric and Combustion Engineering pancaked cores, and the Westinghouse modular core each had one major core dimension shortened to promote core leakage. There is no core geometry which appears to represent a "best" design, since each has its own advantages and disadvantages. However, we have reviewed our original choice of an annular core in an attempt to decide if its disadvantages or if the advantages of the other designs indicated that some other choice would be preferable.

On the basis of parameters reported in the design studies, the annular concept seemed to have the major disadvantages of a high plutonium inventory and a large outside diameter for the core. Both of these tend to impose an economic penalty on the annular design. In other areas, it seemed approximately equivalent. Comparative calculations⁽⁶⁾ showed it to have the lowest reactivity effect on loss of core sodium, although both the modular core and annulus should be capable of achieving equivalent effects. Breeding ratios, when corrected for differences attributable to fuel material, were essentially the same for all designs. No significant difference in Doppler coefficient for unmoderated cores was demonstrated in the design studies, although the Doppler coefficient was substantially increased in one design by the addition of moderator. Thus, the possibility of achieving lower plutonium inventory in a core having smaller overall dimensions was the major incentive for investigating alternative concepts.

The parameters of plutonium inventory and total core size are strong functions of average core power density. In order to evaluate the effects of changing core power density, calculations for various sizes of annular cores were performed. In each, the height and thickness of the annulus were kept constant while the inner diameter was decreased. One-dimensional CRAM calculations were performed using the axial buckling of our original design study and twenty

* Physics analyses for this section were performed by John Lewellen, Allis-Chalmers Manufacturing Company, Atomic Energy Division.

energy groups. The cross-section set was that of the original design study, except that the four groups of lowest energy were not used and nominal rather than specific composition-dependent values for heavy-element absorption cross sections were used. Fission product concentrations and plutonium isotopic ratios corresponded to a fuel burnup of 67,000 MWD/T. Material volume fractions were those of the original study. For each core, a fissile material search was performed to determine critical loading, and power distributions were determined. The original annular core was recalculated, since a simpler cross section set and less detail in representing blanket plutonium distribution were used than for the original design study. Results are given in Table 1.

In going from the reference annular core to a 5000-liter core, corresponding to a 55% increase in power density, equilibrium core enrichment decreased slightly to 99.7% of the reference. This indicates that, in the annular core, plutonium inventory is essentially in direct proportion to core volume, or inversely proportional to average power density. The power peaking factors also changed very slightly with core volume. Thus, plutonium inventory is also inversely proportional to peak power density in the core, a parameter important in defining thermal limitations of fuel-element design. The fact that enrichment and power distribution changed so slightly over the size range considered indicates that the annular core behaves as an infinitely long rectangular cylinder. Thus, other important parameters of the annular core, such as breeding ratio, Doppler coefficient, and sodium voiding reactivity effects, are expected to be relatively independent of core volume.

Using these characteristics of the annular core, we have performed some comparisons of plutonium inventory requirements and outer core dimension with alternative concepts of the design studies. These comparisons were performed under the assumption that cores are designed to the same *peak* power density. For oxide cores, this is equivalent to the same fuel-pin geometry. This is a rather rough basis of comparison, since for each design the fuel-pin geometry and power density are set by economic optimizations. However, we believe it to be a more equitable basis of comparison than assuming equal average core power densities, since differences in power peaking factors can require substantially different fuel designs in cores having the same average power density.

The original design studies showed a substantial advantage for the pancaked cores as regards plutonium inventory. Comparisons were made with the General Electric core, since it used the same fuel material and its sodium voiding effects were closer to

TABLE 2. CALCULATIONS FOR MODULAR CORE

Module Size, liters	Equivalent Cylindrical L/D	Fissile Pu Inventory, kg	Enrichment, % (Fissile Pu/U + Pu)	Power Peaking Factor	Electric Power per Module,* MWe
7820, reference annulus	—	3033	17.8	1.52	1000
500	1	242	22.3	1.72	57
1000	1	396	18.2	1.86	105
1500	1	539	16.5	1.95	150
2000	1	676	15.5	2.01	195
2500	1	810	14.9	2.06	237
3500	1	1071	14.0	2.14	320
1000	2	410	19.3	1.93	101
3500	2	1136	14.9	2.23	307

* Assuming same peak power density and core to blanket power split as for the 7,820-liter reference annulus.

the annular design. This comparison was made on the basis of reported values of axial and radial power peaking factors, exclusive of hot spot factors. The annulus was assumed to have the same peak value of power per kg of fuel (U + Pu) as the pancake. (Power/kg fuel was used instead of power density to eliminate differences attributable to fuel volume fraction and fuel density.) On this basis, the annular core inventory (initially fabricated) is substantially reduced but is still approximately 16% greater than the reference pancake. Removal of moderator from the reference pancake would decrease its inventory. However, the annular core was designed to give a lower core sodium voiding reactivity effect, and thickening the annular core to achieve equivalent sodium voiding effects would decrease its inventory. Further detailed studies would be required to determine the exact extent of inventory advantage and its effect on fuel-cycle costs of the alternative designs. On the basis of this comparison, however, inventory requirements seem roughly equivalent.

Outer core diameters of the annulus and pancake were also compared on the basis of equivalent maximum specific powers. The diameters were not significantly different. However, the annular core had a slightly smaller diameter than the reference pancake, a different result than shown in the original design studies.

A direct comparison of the annular core with the modular core of the design studies is complicated by the fact that one design used carbide and the other used oxide fuel. To assess relative inventory requirements and core dimensions, we have performed calculations for a series of oxide-fueled spherical cores having the same material volume fractions as our reference annulus. For each core, one-dimensional CRAM

FIG. 1. Fissile Inventory Requirements for a 1000-MWe Modular Core.

calculations were performed to determine power distributions and critical loading, with fission product concentration and plutonium isotopic ratios corresponding to 67,000 MWD/MT. Effects of module coupling on inventory requirements were neglected. Spherical cores with volumes of 500, 1000, 1500, 2000, 2500, and 3500 liters were computed. The spherical cores were used to represent a single cylindrical module of $L/D = 1$. The effects of changing single-module length-to-diameter ratio were evaluated by calculations for cylinders of 1000 and 3500 liters having an $L/D = 2$. Results are given in Table 2 and Figure 1.

In Figure 1, total fissile inventory is plotted versus the number of core modules used to produce 1000 MWe (assuming the same core to blanket power split for each). Results for the modular cores are given in two curves. The upper curve corresponds to the assumption that the modular cores are designed to the same *peak* power density as the 7820-liter reference annulus, which we consider the proper basis of comparison. The second curve corresponds to the assump-

tion that the modular and annular cores have the same average power density, or equal total core volumes. The dotted line represents fissile inventory for the annular core. These results show that, when designed to the same peak power density, a modular core will have a higher plutonium inventory than the annular core regardless of the number of core modules used. For cylinders having L/D greater than unity, the inventory advantage of the annular core is increased. The requirements of lower inventory for the annulus results from its low total power peaking factor relative to the modular cores. This permits a smaller total core volume for the same power production, which more than offsets the higher enrichment of the annulus when compared to modular cores made up of single modules of 1100 liters or greater. (For smaller-sized modules, the enrichment is higher than that of the annular core.)

We have also compared outer core diameters of annular and modular cores. The outer diameter of a modular core will depend upon the L/D ratio of the

individual modules, size and number of modules used, blanket thickness, and power density. We looked only at hexagonal cylinders with L/D of 2, employing the same blanket thickness as the Westinghouse 1000-MWe design study modules, and with either 4 or 7 modules per core. Allowable power density was determined from our calculations for a spherical core. The outer core diameter of the reference annulus is 16 ft. A four-module core having the same peak power density would be 15.7 ft in OD. A seven-module core of the same peak power density would be approximately 14.1 ft in OD. If a 5000-liter annulus is used, corresponding to increasing the power density of the reference annulus, its outside diameter would be 12 ft. Four- and seven-module cores having the same peak power density as this smaller annulus would be 13.9 and 12.6 ft in diameter, respectively. Although these results cannot give a definite answer as to whether a modular or annular core will have a greater diameter, they do indicate that neither design has a strong advantage as regards diameter.

From these comparisons of the annular core with the alternative geometries of the pancake and modular cores, we have concluded that the apparent disadvantages of high plutonium inventory and large outside diameter are not an inherent disadvantage of annular cores. On the basis of equivalent thermal design, there was a small inventory advantage to the reference pancake, although much less than that shown in the design studies. Our calculations with modular cores showed the annular core to have lower inventory requirements than a modular core, regardless of the number of modules used. No design showed a distinct advantage as regards core diameter.

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Discussion

Mr. Gibson (AI): You indicated that the disadvantage of the modular core was the high inventory required, aside from considerations of plutonium availability; that's only a disadvantage if it economically is unattractive. Did you do any fuel-cycle studies of the modular versus the annular core?

Mr. Strawson: The original intent of our studies was to see if what appeared to be inventory disadvantage of the annular core was, in fact, a major disadvantage. We concluded from our studies that it was not. We have not carried through detailed studies of fuel-cycle cost.

Application of the Vented Fuel Concept to a Sodium-cooled Power Breeder with 1000 MWe*

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The principle of venting the fission gas of a fast ceramic reactor to its coolant has been well-known for some years.⁽¹⁻²⁾ There is an impressive number of advantages:

1. No stress of the fuel cladding caused by fission gas pressure. This results in:
 - a. improved safety against canning rupture;†
 - b. smaller wall thickness of cladding;
 - c. less structural material in the core, i.e., higher breeding ratio;
 - d. no need for the use of exotic canning materials.
2. More compact subassembly, since no fission gas plenum is necessary. This results in:
 - a. smaller pressure drop of coolant, smaller pump-power, and smaller wall thickness of subassembly boxes;
 - b. smaller height of reactor vessel, shielding, refueling equipment, and air locks.
3. No storage of high-pressure radioactive gas. This results in:
 - a. no sudden uncontrollable gas eruption during reactor operation in case of canning rupture or meltdown. This is of special importance in connection with the positive void coefficient;
 - b. no sudden uncontrollable gas eruption during the fuel handling and fuel reprocessing.

On the other hand, some special equipment is needed, which adds to the capital costs of the station:

1. special plugs for the fuel rods;
2. a gas-purification plant with all equipment (blowers, cooling aggregates, heat exchangers, etc.);
3. additional filtering equipment with high retention ability for halogens;
4. elaborate system for control of radioactivity;
5. special flanges and gaskets for high leak-tightness.

The most important item is the purification plant for the cover gas. But it should be kept in mind that we

also save some costs for fission gas treatment at the reprocessing plant.

It is the purpose of this paper to give some answers to the following questions:

1. What are the principal design specifications for the gas-purification plant with respect to operation and safety of the reactor?
2. What are the numerical results for a reference reactor?
3. What is the economical balance of the cited advantages and disadvantages of the "vented-to-coolant" concept?

We are aware of the fact that many groups are working on this subject and probably have arrived at similar results. But very few of these have appeared in the literature so far. This contribution particularly is meant to stimulate the discussion on this very interesting matter.

1. Specifications for the Gas-purification Plant

Figure 1 shows the principal plant. Region I is the cover gas inside the reactor vessel, II the inside of the safety containment. Let a flow rate g of fission gas enter region I; let $\dot{v}_A$ and $\dot{v}_E$ denote the flow rates to and from the purification plant, respectively. Let the leakage flow rate L be from region I to II, with flow rates $\dot{V}_A$ and $\dot{V}_E$ to and from the outer atmosphere. Normally $\dot{V}_A$ is given by the air-exchange rate of the containment volume, and C_A is either given by the allowed activity level inside the building or by the allowed activity output at the stack.

If the respective activity concentrations are c_g , c_E , c_A , c_L , C_E , and C_A (all in Ci/cm³), we get the following balance:

$$(1) \quad gc_g + \dot{v}_E c_E = \dot{v}_A c_A + L c_L;$$

$$(2) \quad L c_L + \dot{V}_E C_E = \dot{V}_A C_A.$$

In general, $c_E = 0$; $\dot{v}_E = \dot{v}_A$; $c_A = c_L$; and $C_E = 0$.

For $L c_L \ll g c_g$, the result is

$$(3) \quad \dot{v}_A = \frac{g c_g}{\dot{V}_A C_A} L,$$

* This paper has been prepared within the framework of the association EURATOM—Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

† This might not be true for fast excursions with sudden fuel melting and subsequent fission gas release from the oxide.

the input of the purification plant. The leakage L , therefore, is the most important parameter for the size of the purification plant. There are several flanges for refueling purposes and control rod drives on the top of our proposed sodium-cooled reactor.

The problem is the definition of a feasible leak-tightness.

The cover gas is helium. The most extensive tests on this have been made in connection with the Dragon project.⁽³⁻⁵⁾ The results have been converted to the cover-gas pressure of 1.2 atm of our reactor according to the Knudsen equation, in which the leakage is proportional to the pressure difference. The result is a leakage per unit length of

$$1.5 \times 10^{-12} \text{ (atm-cm}^3\text{/sec-cm)}$$

or with a sealing length of 12,500 cm, we get

$$L = 1.87 \times 10^{-8} \text{ (atm-cm}^3\text{/sec)}$$

for the total reactor top.

After determination of g and c_g the purification plant can be specified.

We feel it important to underline that these specifications are based on operational safety and not on the analysis of any major accident.

The results of an accident of the vented-to-coolant and the vented-to-reservoir plant are the same, since in this case the destructions of a number of fuel cans in the latter is probable.

2. Fission Gas Production and Numerical Results

The 1000-MWe reference reactor⁽⁶⁾ produces 0.775 cm³/sec of the gaseous fission products bromine, iodine, xenon, and krypton, or approximately 1 cm³ per fuel rod, per day. We assume that these stay inside the fuel for a time $t_c = 5$ days at the ambient pressure of 2 atm. If it is possible to maintain higher gas pressures inside the rod, the retention time would rise proportionately, but it is extremely difficult to build up any overpressure at such a low flow rate.

During a time t_B the gases pass the axial blanket, where t_B is a function of pressure, temperature, and the free cross section.

If we take into account the production rate⁽⁷⁾ of the different isotopes produced directly by fission and the radioactive decay of their parents in the core zone, together with the results of Burris and Dillon,⁽⁸⁾ the activity (Ci/cm³) leaving the fuel rods can be calculated. For a burnup of 100,000 MWd/t release fractions of 100% for xenon and krypton and of 50% for the halogens are assumed. The exit activity has been calculated for two cases:

- a) the fission gas moves through the blanket as a rigid column;

FIG. 1. Principal Reactor Layout.

Basic Equations:

1. $gc_g + v_E c_E = \dot{v}_A c_A + L c_L$ for Region I
2. $L c_L + \dot{V}_E c_E = \dot{V}_A c_A$ for Region II
3. $\dot{v}_A = \frac{g c_g}{\dot{V}_A c_A} L$

- b) the gas entering the blanket zone always is completely mixed with the gas already there.

Tables 1 and 2 show the results for no mixing and complete mixing in the blanket, respectively.

Both I and Br will be chemically bound in sodium. We assume in accordance with GE⁽⁹⁾ that 0.02% of the iodine arriving at the coolant will be contained in the cover gas. The activity of I in Na is small compared with the activity of the Na itself. The activity of Br in the Na and the cover gas can be neglected.

If this be taken into account, c_g and g can be evaluated, and by means of eq. (3) the input $\dot{v}_A$ of the purification plant can be calculated. Figure 2 shows the results as a function of the fuel-element pressure for different assumptions for the process in blanket and plenum. It was assumed that

$$\dot{V}_A = 11 \text{ m}^3\text{/sec}; \quad C_A = 10^{-11} \text{ } \mu\text{Ci/cm}^3$$

(that is, one-tenth of maximum permissible concentration of unidentified mixture; however, without α emitters and without the β emitters Pb²¹⁰, Ac²²⁷, Ra²²⁸, and Pu²⁴¹).

3. Cost Evaluation

A purification plant for 260 m³/hr helium has been planned in connection with Linde AG., Muenchen, and

TABLE 1. EXIT CONCENTRATION OF GASEOUS FISSION PRODUCTS (Ci/cm³ GAS PRODUCED IN CORE) AS A FUNCTION OF FUEL INSIDE PRESSURE (NO PLENUM AND NO MIXING IN BLANKET)

Isotope	Half-life	Fuel Inside Pressure (atm)				
		2	4	8	12	34
Kr-83m	1.3 hr	0.004	—	—	—	—
Kr-85m	4.4 hr	1.2	0.035	0.001	—	—
Kr-85	10.6 yr	0.0047	0.0046	0.0045	0.0044	0.0044
Kr-87	78.0 min	0.0033	—	—	—	—
Kr-88	2.8 hr	0.9	0.0046	—	—	—
Kr-89	3.2 min	—	—	—	—	—
I-131	8.1 d	50.4	39.5	24.2	16.1	3.0
I-132	2.3 hr	0.9	0.0017	—	—	—
I-133	21.0 hr	105.5	28.9	4.5	0.9	—
I-134	53.0 min	—	—	—	—	—
I-135	6.7 hr	29.6	2.3	0.025	—	—
Xe-133	5.3 d	197.0	127.9	72.2	41.9	5.1
Xe-135m	15.7 min	—	—	—	—	—
Xe-135	9.2 hr	204.9	34.0	1.3	0.038	—
Xe-138	17.0 min	—	—	—	—	—

TABLE 2. EXIT CONCENTRATION OF GASEOUS FISSION PRODUCTS (Ci/cm³ GAS PRODUCED IN CORE) AS A FUNCTION OF FUEL INSIDE PRESSURE (NO PLENUM AND COMPLETE MIXING IN BLANKET)

Isotope	Half-life	Fuel Inside Pressure (atm)				
		2	4	8	12	34
Kr-83m	1.3 hr	0.4	0.2	—	—	—
Kr-85m	4.4 hr	7.0	1.9	0.5	0.2	0.024
Kr-85	10.6 yr	0.0044	0.0046	0.0045	0.0044	0.0044
Kr-87	78.0 min	5.4	1.2	0.3	0.2	0.024
Kr-88	2.8 hr	17.2	4.3	1.1	0.5	0.049
Kr-89	3.2 min	0.4	0.2	—	—	—
I-131	8.1 d	52.2	42.2	27.6	19.7	5.4
I-132	2.3 hr	36.5	8.4	2.3	1.1	0.1
I-133	21.0 hr	144.3	55.5	19.0	8.7	1.2
I-134	53.0 min	13.7	3.5	0.9	0.035	0.005
I-135	6.7 hr	87.9	25.3	6.5	1.5	0.4
Xe-133	5.3 d	193.8	132.0	83.4	54.3	12.9
Xe-135m	15.7 min	1.2	0.6	0.3	0.07	—
Xe-135	9.2 hr	310.1	109.3	30.4	10.1	1.8
Xe-138	17.0 min	7.9	1.9	0.5	0.2	0.024

evaluated for a fuel inside pressure of 2 atm, no plenum, and complete mixing. Xenon and krypton are removed from the cover gas by low-temperature adsorption. The plant will be relatively expensive because of the need for continuous operation and the large amount of decay heat. It needs 180 kW pumping power, 75 m³/hr coolant water, and 35 l/hr liquid nitrogen. Table 3 shows the heat production for different conditions.

Compared with the reference reactor with "venting to reservoir," the core height with venting to coolant is reduced by 80 cm. The most important changes are:

	Reference Reactor ⁽⁶⁾	Vented to Coolant
Pressure Drop, atm	4.25	3.20
Cladding Thickness, mm	0.35	0.15
Cladding Fraction of Fuel Element	0.25	0.096
Breeding Ratio	1.42	1.59

Table 4 shows the resulting capital costs. The main contribution comes from the smaller fabrication cost of the shorter fuel elements with \$205,000 per core. For replacing one-third of the core per year, this is equivalent to 1.59×10^6 capital costs for a 15-yr lifetime. The second largest contribution is the capitalized gain in breeding ratio (capitalized for a load factor of 0.8).

The radial blanket is also considerably shorter. For 4 blankets during the reactor lifetime, this amounts to 1.05×10^6 . Smaller savings are gained by the reduction of pumping power.

Gas purification and storage at the reprocessing plant are not needed any more. The costs were evaluated in accordance with Alter.⁽¹⁰⁾ Finally, the reduced height of the core allows for some savings for the core vessel and its internals, such as supporting cylinders, insulation walls, reloading equipment, and thermal shielding. However, this contribution is small.

The last column at the right side of Table 4 shows the additional costs. The net saving amounts to 2.19×10^6 or 2.1 % of the total plant cost.

4. Preliminary Tests

The important point is the development of a suitable plug. The diving-bell design of GE is well-known.⁽¹⁾ It promises a very reliable operation, but it must be of considerable length and, therefore, adds to the pressure drop and to capital costs. We, therefore, thought it useful to study the behaviour of a very simple porous plug of sintered metal.

Two problems are of importance:

- the ability to retain sodium from the fuel. In the case of a reactor shutdown and lowering of the fuel temperature, the outside overpressure of the sodium amounts to 1–2 atm.
- the ability to stay porous for the fission gases.

In a simple apparatus the following plug materials have been tested: Ni, CrNi 18/8, Fe, and CrNi 18/10, with pore sizes between 1 and 5 μ . Sodium leakage started at a pressure difference of about 1.5 atm for sodium temperatures of 500°C, and at about 0.5 atm at 600°C.

The first plugging experiments by artificial evaporation of iodine did not lead to a pressure buildup of the

FIG. 2. Theoretical Input into Purification Plant.

TABLE 3. DECAY HEAT TO BE REMOVED FROM THE ADSORBERS AFTER 3000 HR OF OPERATION TIME FOR DIFFERENT MECHANISMS OF FISSION GAS RELEASE (AT 2-ATM FUEL PRESSURE)

Release Mechanism	Decay Heat of Isotopes (kW)			Total Decay Heat (kW)
	Xe-133	Xe-135	Kr-85	
No Plenum; No Mixing	280.0	57.5	0.15	337.7
No Plenum; Complete Mixing	276.2	94.0	0.15	370.4
Plenum (40 cm); Complete Mixing	200.0	11.7	0.15	211.9
Plenum (80 cm); Complete Mixing	139.0	6.9	0.15	146.1

TABLE 4. COST BALANCE FOR APPLICATION OF A VENTED-FUEL CONCEPT

Savings	10 ⁶ \$	Additional Costs	10 ⁶ \$
Core Fabrication	1.59	Gas-purification Plant	2.30
Breeding Ratio	1.58	Operational Costs	0.77
Radial Blanket	1.04		
Circulation Pumps	0.50		
Gas Treatment at Re-processing	0.34		
Reactor Vessel	0.21		
Total	5.26		3.07

gases. More tests are necessary for the evaluation of the sintered porous plug.

5. Conclusions

From this study the following conclusions can be drawn:

- The venting of fission gases to the sodium coolant allows for savings of the order of 2% of the total plant cost, if compared to the reference reactor. In comparison to a more optimized reactor the savings are much smaller.
- The main savings are from lower costs for core and radial blanket resulting from the elimination of the fission gas plenum and the reduction of cladding thickness, which causes a substantial rise in breeding ratio.
- A low-temperature adsorption process seems to be the most suitable for the purification of the cover gas. The main problem is the decay heat of several hundred kW, mainly from xenon.
- The input specification of the purification plant comes from operational criteria. The unavoidable leakage into the containment building is the determining factor.
- The most important advantage is the gain in safety. Sudden bursts of gas into the coolant and

the consequences with a positive void coefficient are avoided. The transient behaviour needs further studies.

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Discussion

Mr. Gueron (EURATOM, Brussels): In the economy found for the reprocessing plant, did you not assume (a) that you had an integrated reactor reprocessing plant, and (b) that you would have really 100 % loss of fission gases? Are these assumptions perhaps not a little daring?

Mr. Smidt: I think you are completely right. It was a point of this study to find out what, at the maximum, one can gain with venting the fission gas to coolant, and the maximum found is practically nothing. It becomes even worse if one takes into account some of these factors which might be too optimistic in this study. So, I agree with you.

Mr. Zebroski (GE): A very strong parameter which would influence the cost of the processing plant would be the delay time that you assume that the gas sustains within the fuel; if you can be clever enough, say to get 1200 hours delay time, you would issue only krypton-85 and have a negligible gas-purification-plant problem.

Mr. Smidt: That's completely true. The main cost comes from the decay heat. If you could build up a pressure of, say, 10 or 20 atm in your fuel rods, you would have much easier design of the purification plant. However, with a flow rate of one cubic centimeter a day, it is very difficult to get a reliable pressure in this order of magnitude.

Mr. O'Neill (GE): You have chosen the Dragon data and the leakage rates obtained as an impossible situa-

tion, whereas I think we would choose to develop a sealing system that could contain the cover gases.

Mr. Smidt: This would appear in the capital balance end as additional cost.

Mr. Wright (WAPD): The designing for a vented fuel system not only includes the purification problem, but possibly imposes burdens on maintenance and operation of the plant as well. On the other hand, if you look at the 100,000 MWd/ton burnup, one would have difficulty saying that these things are all going to get there without having one or several can ruptures and subsequent leakage. The real problem is not to compare a nonradioactive, nonfission product system to one having fission products, but maybe the difference in the quantities is what you have to compare. I don't think the difference is quite as great as you might guess.

Mr. Smidt: I would repeat again you need a purification plant in any case, but you could assume with no venting through the coolant you would have at least a longer decay time of the fission gases.

Mr. Okrent (ANL): I believe you indicated a safety advantage for venting, from the point of view that you would not be subject to rupture of cladding. How do you evaluate the possible safety disadvantage of a larger fission product burden in the sodium and the increased difficulty of detecting the failure of fuel?

Mr. Smidt: You mean the activity of fission gases in the sodium?

Mr. Okrent: Yes.

Mr. Smidt: We haven't evaluated that. The question we put was whether we need the safety advantage that no sudden bursts of fission gases will be released under transient conditions. This has to be studied much more experimentally. The safety aspect of the picture isn't complete yet.

Mr. Häfele (Karlsruhe): Another advantage is the fact that the plenums at the entrance of the subassembly are no longer under strong pressures. With venting, the potential danger of sudden fission gas release from clad rupture is gone.

Mr. O'Neill: With Dr. Smidt's permission I could help to answer Dr. Okrent's question, I think. There is a way to detect the difference between the fission products that escaped from the vented fuel and the failed fuel in that you look at the age of the fission products. We have some experimental data that says it takes 5 to 10 days for the gases to escape, so it is fairly easy to tell the difference.

Mr. Okrent: If you're looking for fission gases. But, it is a relatively slow detection system if fission gases have to get from the fuel through the coolant out to the cover gas. I was thinking in terms of a delayed-neutron detector, of the sort that is now on EBR-II, which, hopefully, might detect failure on a time scale of a minute or less. If you are running with vented fuel, is there a greater likelihood of having a reasonable amount of fissionable material in the sodium, and is there a greater background, so that the probability of detecting failure then is diminished?

Mr. Jens (APDA): It seems that all fuels we have been talking about have not yet achieved the burnups that we're talking of, and that one of the real advantages of the vented fuel system might be the fact that you can reach the burnup you're talking about, whereas the rest of them can't. Is that burnup advantage at all worked into your numbers?

Mr. Smidt: No. We took a standard burnup, so to say, of 100,000 MWd/ton. I agree with you; you have a point which might also be of value.

Steam Cooling for Fast Reactors

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Introduction

Over the past six years, B&W has been engaged in the study and evolution of steam-cooled reactors. After looking at two thermal reactor designs, work was begun on a direct-cycle fast reactor incorporating a modern 3500-psi, 1000°F steam cycle with 950°F reheat. The study of this reactor was jointly sponsored by the East Central Nuclear Group and B&W; the results have been reported recently in papers presented at Detroit and Gatlinburg.

Since then, B&W has independently investigated the possibilities of cooling a direct-cycle fast reactor with steam at 1200 psi. The purpose of this paper is to describe some of the interesting and encouraging results obtained.

Objectives of Study

A core power of 2500 MWt was selected for the study in order to provide a net plant output in the neighborhood of 1000 MWe. The objectives of the study were to determine the following:

1. Can a 10-year doubling time be achieved?
2. What are the estimated fuel costs?
3. Does 1200-psi steam offer improved safety characteristics?
4. What is capital cost of a 1000-MWe plant?
5. Can plant efficiency be improved?

Breeding Requirements

The first results of our study indicated that a doubling time under 10 years could be achieved. It was found that the required breeding ratio and specific power could be obtained with an equilibrium plutonium cycle based on a pancake core geometry and partial surface roughening.

The principal core design parameters are as follows:

1. Thermal power, MW	2500
2. Core diameter/core height, in.	108/36
3. Axial/radial blanket thickness, in.	15/22

4. Pin diameter/spacing, in.	0.300/0.360
5. Core volume, liters	5400
6. Fuel/Structure/Coolant/
Control Rod Fraction	48.9/9.7/40.2/1.2

Four-group radial lifetime calculations, using an equilibrium plutonium mixture of 64% Pu-239, 25% Pu-240, 8% Pu-241, and 4% Pu-242, yielded the following information:

Physics Parameters

Core life, days of full power	750
Core irradiation, MWD/Tonne	80,000
Fissile plutonium loading, kg metal	2200
Total Pu + U loading, kg metal	22,450
Integrated overall breeding ratio	1.40
Integrated in-core breeding ratio	1.01
Doubling time, years	9.0
Specific power, kW thermal/gm fissile
plutonium in core	1.19

In these calculations, the three fast-group diffusion-theory coefficients were collapsed from a 68-group cross-section library. Resonance integrals were calculated for both fertile and fissile isotopes for all energy groups containing resonances.

Doppler broadening and Dancoff corrections were made in the resonance region. Thermal coefficients were generated by averaging thermal cross sections over a Wigner-Wilkins spectrum. Axial geometric buckling was calculated in the usual way for a cylinder and checked by axial criticality calculations. Fission products were treated on an infinitely dilute basis.

Fuel Cost

By means of the mass balance from these calculations, a fuel cost of 0.50 mill/kWhr was obtained. The components whereby this figure was derived are as follows:

Reprocessing, shipping, and
fabrication	$\$8.48 \times 10^6$
Working capital charge at 10 %
interest	8.05×10^6

Plutonium credit at \$8861/kg (2% loss)	-8.13×10^6
Net core cost	8.40×10^6
kWhr/core	1.69×10^{10}
Fuel cost, mill/kWhr	0.50

If interest on the bred fuel during core life and cooling is left off, the cost falls to 0.42 mill/kWhr. Further, if 80% of core and radial blanket cans are reused once, the savings in fabrication cost bring the fuel cost to 0.37 mill/kWhr. These figures assume the entire radial blanket is reprocessed with each core. Some additional reduction should be possible with a better scheme for blanket management.

Safety

Modification of the plant design to increase capability with a positive voiding characteristic was considered. It was felt that a good start in this direction would be to find means to reduce the maximum credible rate of pressure loss from the system. This led to development of an integral arrangement of the Loeffler cycle (see Figure 1). Instead of enclosing only the reactor core and internals in a pressure vessel as shown here, we investigated the possibility of placing all principal components of the Loeffler cycle inside a reactor pressure vessel which is in turn enclosed in a high-pressure close-fitting containment. This includes the reactor core and internals, desuperheating and steam-separating equipment, and the bypass coolant flow. The circulators are also enclosed in the pressure vessel, and these are driven by steam turbines which are mounted on the vessel and supplied steam directly from it.

How these components can be arranged in an actual reactor design is shown schematically in Figure 2. The turbines and circulators are mounted in the top head without interfering with refueling ports, which are not

FIG. 1. Integral SCBR Flow Diagram.

FIG. 2. Integral SCBR Schematic.

shown. Steam-desuperheating and scrubbing equipment are mounted below the core as shown. These consist of a multiplicity of spray-type desuperheaters followed by a steam-scrubber section such as shown.

By eliminating the relatively large external recirculating steam lines and using a close-fitting, fully pressurized containment, it becomes easier to control the rate of accidental coolant loss as well as to prevent core damage and provide dependable control rod action during such loss. These measures are further augmented by using four parallel turbine steam lines, each of which contains flow restrictors at the reactor vessel penetration.

Another advantage of the integral arrangement could be an inherently short constant for the time loop. This in combination with an excess of desuperheating spray may permit a significant automatic increase in steam generation rate as system pressure starts to fall during a coolant-loss accident. This would reduce the rate of pressure loss and thereby provide a longer time interval for control rod insertion.

If the reactivity change is negative with voiding, coolant loss would appear to involve only the problem of emergency cooling.

In this connection, we believe that improved emergency pumping characteristics are realized by mount-

CAPITAL (14% & 80% L.F.)	1.60
FUEL	.50
O.M. & I	.40
TOTAL MILS/KW. HR.	2.50

FIG. 3. Preliminary Economic Estimates for Integral SCBR.

ing the circulator-driving turbines within the high-pressure containment and integral with the reactor vessel, and supplying them with steam directly from the vessel. Each circulator is also fitted with a pony motor to provide backup emergency pumping power and to drive the circulators during shutdown and startup operations.

Capital Costs

Another advantage of this design appears to be low capital costs. Preliminary estimates obtained by incremental adjustment of costs from the previous 3500-lb study indicate the integral 1200-psi plant would have a direct cost of $\$55.70 \times 10^6$. Adding 35% for indirect costs during construction and contingency, we get $\$75 \times 10^6$, which amounts to $\$80.00/\text{installed kW}$ based on a 940-MW net plant output. We believe this low figure is due to basic simplicity of the integral arrangement plus the inherent low cost of a direct-cycle plant.

Cost of Power Generation

Figure 3 shows preliminary economic estimates in which the fuel and capital cost estimates have been combined with a 0.4-mill/kWhr charge for operation, maintenance, and insurance. This totals to 2.50 mills/kWhr, so that the economic potential of this plant is indeed satisfactory.

Plant Efficiency

Turning now to plant efficiency, we found that the integral design materially reduced requirements for pumping power through elimination of external recirculating lines. If we take into account reasonable losses for flow restrictors, isolation valves, and headers, we find that external recirculation would add about 75 psi to the circulator pumping head or 55 MW to the pumping power requirement. This corresponds to about a 1.3 percentage point reduction in net plant efficiency, which we presently estimate to be about 37.5%.

The fact that this is somewhat below the efficiency of

modern reheat steam cycles encouraged us to look for ways to improve this value. In a Loeffler cycle, a major portion of the reactor outlet steam is recirculated to mix with feedwater which is added to maintain a mass balance in the system and provide reactor inlet steam at or near saturated conditions. The direct spray of water into this hot steam is an undesirable waste of availability as the temperature is degraded from 1050 to about 555°F without doing useful work.

This realization led to a reheat cycle designed to take advantage of the wasted availability (see Figure 4). A once-through boiler is placed between the core grid plate and desuperheaters to cool recirculated steam to about 660°F. Feedwater enters the desuperheaters as before, but since the inlet steam temperature is only 660°F, most winds up in the bottom of the vessel as saturated water. This water is supplied to the boiler at 3800 psi through a booster feed pump. It is heated in the boiler in counterflow heat exchange to 950°F at 3500 psi, expanded in a high-pressure turbine, and returned to the reactor vessel. This turbine produces about 190 MW of power, and thereby increases net plant output about 100 MW and efficiency about 4 percentage points.

The supercritical pressure boiler handles more heat than the reheater it replaces in the 3500-psi SCBR plant cycle. However, in spite of the increased heat load, the actual cost of the boiler may be lower, since the combinations of coolant flow, pressure, enthalpy rise, and specific volume appear more favorable for economical heat exchanger design.

Actually, this power cycle places the reactor between the high-pressure and intermediate-pressure turbines of a supercritical reheat cycle, thus permitting low-

FIG. 4. Reheat Cycle Integral SCBR.

pressure cooling of the reactor core and the resultant benefits of high breeding ratio. We hope it may constitute an advance in the art of applying a direct, high-efficiency reheat steam cycle to a steam-cooled breeder reactor.

Current Activity

We are now studying methods to improve voiding characteristics, are continuing to develop plant design features to improve compatibility with a positive voiding Δk , are investigating performance with other fuel cycles, and will continue investigation of the steam cycle just mentioned.

Discussion

Mr. Smidt (Karlsruhe): Am I right that in your first design you showed the integral design without the reheat cycle and that you have placed your Loeffler apparatus directly below the core? What are the problems of maintenance?

Mr. Schluderberg: Yes, we arranged the desuperheaters below the grid plate where the blanket bundles go in. Since there are no control rod drives in the blanket bundles, we can go through the holes in the grid plate and remove the nozzles and the venturis themselves. Then we have two arrangements: one where the scrubbers are below and one where the scrubbers are up, on the side where they can be gotten to more easily for maintenance.

Mr. Wall (GE): Are the control rods top mounted or bottom mounted? How did you calculate the magnitude of the loss-of-coolant reactivity coefficient? What were your arrangements for emergency cooling?

Mr. Schluderberg: The control rods themselves are pin rods that are located in the fuel-bundle can. One of the unique arrangements of the design, and why we can get down to a 9.7% structure fraction, was by taking the hexagonal spacer tube pieces (they're just short things braced together to form an array) and making the outer pins the whole length of the can, so that these become the can walls. We have put nine pin rods in each can, and they're actuated by a pressure command which is communicated through the grid plate; each pin is, of course, latched in place and requires a special pressure pulse to unlatch it before it can be driven out.

We have just started work on loss-of-coolant reactivity coefficient, and our results are quite preliminary. Perhaps Ralph Webb may say something about the methods we've used.

Mr. Webb: The physics methods are the same as those which were given in the test cases which were reported in this morning's paper.

Mr. Fell (Winfrith): What is it about your present design that has given this big improvement in doubling time? Your previous study showed a breeding gain of 1.08 with supercritical steam. Have you reduced the ratio of hydrogen to plutonium in the core from your earlier design?

Mr. Schluderberg: Yes, we have reduced the ratio. The structure of the fuel fraction is about the same.

Mr. Fell: With low-pressure steam and the much bigger pin spacing there is a greater susceptibility to a flooding accident. Have you had to take special measures, in adding resonance poisons for example, to deal with this, and have these been reflected in your fuel cost?

Mr. Schluderberg: We have placed nine boron carbide pins around the holes in the fuel can. The others contain fuel pins themselves. Nine, of course, isn't the upper limit; we could add 18 or 27, or as many as you wish, to get the amount of poisoned surface to combat the flooding problem. Of course, with equilibrium plutonium fuel it tends to be minimized compared to clean plutonium.

Mr. Webb: We intend to hold down flooding by use of the control rods.

Mr. Häfele (Karlsruhe): Were breeding considerations the basis for selecting 1200 psi as your operating pressure?

Mr. Schluderberg: Yes. We also wanted to see if we could gain anything on the voiding problem. At supercritical pressure the average steam density in the core was about 6½ lb/cu ft, and now it's down to 1.8. Obviously this is a whole lot closer to a voided condition and you should get a lower value of Δk with this.

Mr. Häfele: I would like to point out that according to our investigations there is a serious competition between breeding and safety. The voiding coefficient becomes better the higher the pressure, so that you are in a better position safety-wise. Therefore, our design pressure is higher than 1200 psi. We at Karlsruhe are following both sodium and steam in two parallel approaches. If we give tomorrow a panel paper on sodium and not on steam it is only for the reason that the reference design on steam will not be finished until January.

Mr. Thompson (MIT): It appears that you may add water to the injector systems or you may add superheated steam. Would you explain in a little more detail from a safety viewpoint how you would go about controlling the possible injection and flooding or, on the other hand, the voiding in a rather dynamic system like this?

Mr. Schluderberg: The analysis of the operation of the entire plant is in a preliminary state. We believe that the controls can be designed to prevent flooding

the core due to an overflow of feedwater from the feedwater train as compared with the steam demand. If the pressure is falling in the system, if the coefficient is positive, and if the time constants are quick enough so that this is meaningful from the standpoint of how fast the pressure is falling, the temperature will start to rise. When it does, if there is enough excess spray, you will automatically generate more steam, which will tend to reduce the rate at which the pressure falls and give you more time to get your rods in. If the coefficient is negative instead of positive, then you would try to match the feedwater to the steam demand so that sometimes you would have slightly superheated steam. We ran some pretty extensive operational analysis calculations for the supercritical plant and found that it was indeed possible to design a system where the reactor would follow the load very well and protect itself against small mismatches in feedwater to steam flow. However, we have not gotten our computer methods modified to analyze the low-pressure system, so I can't say any more about it right now.

Mr. Dalle-Donne (Karlsruhe): I would like to ask if reducing pressure increased the cost of the turbines very much or not; secondly, what were the effects on the efficiency of the plant?

Mr. Schluderberg: Reducing the pressure would increase the cost, I think. The efficiency of the supercritical 3500-lb plant, with 1050°F at the high pressure and 1000°F reheat, should be around 43%. Without the reheat provision at 1200 lb and with the lower pressure, I believe it would drop to about 38½%. Of course, you

then tend to have a higher pumping power requirement because you don't have as good a coolant.

Mr. Sofer (UNC): I'd like to make the following comment: Five years ago we studied this concept for the USAEC and pretty much agreed with the results that you mentioned here, namely, that there is a reduction in capital cost. However, there were two problems. One was that we had relatively high fuel inventory which at the time didn't show up in fuel-cycle costs because we were using 4.75% charges with private ownership; this is going to be higher. The second one was the void coefficient of the coolant. We had something like plus \$20 or thereabouts for voiding effect of the coolant from the 300-MW core (the core had an L/D of one). I think you can do better by pancaking. With a 1000-MW reactor I somehow feel that we have a long way to go to prevent the void coefficient from being positive, unless one goes to modular core or pancakes, double pancakes, triple pancakes, etc.

Mr. Schluderberg: Certainly you have to run the leakage up to get that coefficient down. As far as the fuel costs and inventory, I don't think that you had a pancake arrangement, so that you couldn't cool it quite as efficiently as this. Also, as in the low-pressure gas systems, you have to go to surface roughening to get that little kick in thermal performance; otherwise the clad temperatures get too high for this specific power.

Mr. Häfele: I think that the early NDA study didn't have much plutonium-240 in it and that is the key for the help with void coefficients.

Safety and Nuclear Considerations in the Design of the FTR*

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I. Introduction

A conceptual design study for a Fast Flux Test Facility (FFTF) was started by Battelle-Northwest in April, 1965. The study was directed toward the development of a conceptual design and a budget-stage cost estimate for a comprehensive irradiation testing facility to support fuel and material research and development necessary for fast breeder power reactors. The most important component of the FFTF is the Fast Test Reactor (FTR). This paper limits discussion to some of the nuclear and safety features of the FTR.

Although it appears obvious, the principal safety consideration is the provision for the test reactor itself. Although it is possible to perform some fast-neutron tests in power reactors, the design and operational optimizations for power generation are not necessarily compatible with testing. The FTR specifically provides for test-specimen handling, ranging of reactor operating conditions, wide-span reactivity control and adjustment, extensive data sensors and data logging, changing of experimental apparatus, detection of and operation with failed test specimens, and system decontamination. Each of these provisions will and should be part of the original reactor capability.

Safety and nuclear considerations are closely related for fast reactors. This close relation is particularly true for a fast test reactor with its requirements for inherently great accessibility and flexibility for different experiments.

The design for the FTR is not yet beyond the conceptual stage. Indeed, enough difficult problems have been identified to warrant continued intensive conceptual design. However, a preliminary conceptual design has been formulated. As presently conceived, the FTR will be sodium cooled and PuO_2 -stainless steel fueled with a 600-liter core volume. Initial operation at 300 MWt is expected to produce a peak flux of $\sim 10^{16}$ n/cm²-sec at the core center. Ultimate power operation (with corresponding flux increases) of the FTR is set at 400 MWt.

* Based on work performed under U. S. Atomic Energy Commission Contract AT(45-1)-1830.

The current concept provides the facilities summarized in Table 1 for experimental irradiations in and near the core. The core concept is sufficiently general and versatile to permit substantial modification of kind, size, and number of test facilities.

To accommodate these facilities, the reactor core assembly is formed by tubular fuel subassemblies situated at the region of convergence of a skewed, hexagonal array of reactor tubes. The assembly is completed by an Inconel reflector closely surrounding the core (convergence) zone and by the various special-purpose components, such as safety and shim controls, test loop tubes, and flux-monitoring devices. The reactor tubes—the essential structural elements of the core—extend vertically above and below their region of convergence as an expanding array controlled by the geometry of the skewed system. The reactor tubes function (1) as containers to locate the fuel subassemblies in preferred positions in the core, (2) as coolant conduits to convey liquid sodium upward from a supply plenum past the fuel to discharge into the sodium pool in the reactor vessel, (3) as extended guides to permit remote charge-discharge operations from the top of the reactor, and (4) as isolation barriers for closed-loop tubes to prevent intermixing of primary reactor coolants with special coolants used for closed-loop experiments.

Radiation-damage effects leading to dimensional change or nonrecoverable loss of ductility in materials of construction were of concern in the FTR conceptual design. In the intense, high-energy FTR spectrum, both dislocations and hydrogen generation in nickel-bearing steels could lead to the loss of ductility. High operating temperatures would aggravate the effect. Therefore, major consideration was given to reducing the intensity of the damaging flux ($E > 0.1$ MeV) incident on critical, nonremovable structural components. The design concept utilizes sufficient core shielding to reduce the flux intensities on the reactor vessel to levels tolerable for the design lifetime of the reactor. This shielding serves also as the neutron reflector.

Figure 1 shows a schematic of the FTR, including the pressure-containment vessel and the fuel-handling

TABLE 1. FTR EXPERIMENTAL FACILITIES
(also see Fig. 3)

	Initial	Ultimate
In-core Test Facilities		
Closed-loop Fuel Tests, 6 in.	2	4
Closed-loop Fuel Tests, 4.5 in.	2	2
Packaged-loop Material Tests, 4.5 in.	1	2
Open-loop Fuel Tests, 6 in.	2	2
Open-loop Fuel Tests, 4.5 in.	4	8
Reflector Test Facilities		
Open-loop Material Tests	8	4
Packaged-loop Material Tests	0	4
Rapid Transit (Rabbit) Test	1	1

machine. Figure 2 details the skewed-tube arrangement in the reactor. Figure 3 shows a cross section of the initial core arrangement, giving some indication of the complexity.

During the current development of the FTR conceptual design, a large number of parametric studies and safety analyses provided information for the selection of a reference core. It is clear that the optimum reactor size must be a compromise among safety of conducting fuel tests, scope of irradiation test problems to be investigated, number of tests to be conducted simultaneously, time available to do the testing, size of test subassemblies, and available capital and operating funds.

In nuclear design, four major objectives can be identified:

1. The FTR should have the ability of sustaining very high fast-flux levels, about 10^{16} n/cm²-sec above 0.1 MeV.
2. Ample control margins must be available to accommodate low-reactivity test assemblies and to allow for substantial driver-fuel burnup.
3. Inherent safety features must be provided. Specifically, in-core loop design must be such that failure of test assemblies can be tolerated without damage to the reactor.
4. Sufficient alternative nuclear-design variants should be available so that changes in specific power and flux spectrum and the utilization of different driver fuels can be accomplished with relatively minor modifications, and without adverse effects on the safety features of the reactor.

II. Driver Fuel, Flux Intensity, and Flux Spectra

A. Fissile-fuel Choices and Flux Level

The average flux level ϕ in a fast reactor is given by

$$\phi = CP/L\sigma_f,$$

where P is the volumetric power density, L the fissile-fuel density, σ_f the average microscopic fission cross section, and C a conversion factor. Clearly, a high flux requires a high specific power (MW/kg) and a low fission cross section. The high η value of Pu²³⁹, compared with that of U²³⁵, results in relatively low fuel inventories and makes plutonium the preferred fuel for the FTR. These relations are illustrated in Figure 4 and 5 for a range of reactor sizes.

In Figure 4 the critical fuel density is shown as a function of core diameter for PuO₂-SS and UO₂-SS cermet cores. All calculations were made with a one-dimensional (radial) transport model with a buckling correction for axial leakage. The marked decrease in required critical inventories between the Pu and U²³⁵ cases is apparent. For a 600-l core the Pu/U inventory ratio is about 0.6.

Figure 5 represents similar data in a somewhat different form. To obtain a large number of additional data points, a fundamental-mode model was used to compute the flux-to-power ratios for a wide range of fuel compositions. This ratio is shown as a function of the fundamental-mode radius ($R = \pi/\sqrt{B^2}$).

Even though the control problem in a plutonium ($\beta \sim 0.0021$) core is more difficult than in a U²³⁵ ($\beta \sim 0.0064$) fueled core, the flux advantage of the plutonium is so large that plutonium is presently considered to be the reference fuel for the FTR. For typical FTR core sizes, a one-third power density increase in the U²³⁵ core would be required to achieve the same flux level as that of the plutonium core; stated differently, for the same power density the U²³⁵ core would have a flux level $\sim 25\%$ less than for the plutonium core. If the volumetric power density of the plutonium core is limiting, then the U²³⁵ core volume would have to be nearly doubled to achieve the same flux level as that of the plutonium core. Hence, the incentives for adopting plutonium as the reference fissile material are high.

It should be mentioned that Figures 4 and 5 are not completely consistent. The slight inconsistency is caused by the reflector moderation which, of course, cannot be incorporated in the fundamental-mode model.

B. Core Size and Power Level

The selection of core size and power level is not purely a problem of nuclear design. A variety of requirements enter into the selection process. Important among these are provision for adequate test space and required flux level.

To obtain the necessary relations among flux, power level, and core size, a series of static calculations were conducted for different cylindrical core volumes and length-to-diameter (L/D) ratios. Although the mechanical design called for a skewed core, the 4° skew angle

FIG. 1. Reactor Section through FTR.

is so small that equivalent cylinders could be used to represent the cores adequately.

The calculations showed only a very slight dependence of fuel loading on L/D ratio. More importantly, as core volume is increased, the neutron leakage is reduced, and the volumetric fuel densities decrease from about 1 kg/l for a 200-liter core to about $\frac{1}{2}$ kg/l for an 800-liter core. Hence, for a constant flux level, while the

required power increases as the volume of the core is made larger, this increase is not in direct proportion to core volume. Figure 6 illustrates these relations. Larger core volumes are generally advantageous, because they provide more test volume space. As a consequence of reduced fuel-centerline temperatures and temperature gradients, increases in system reliability, and hence core safety, can also be achieved.

FIG. 2. Schematic of TUBED Core.

The present reference core has a 600-l volume. Cores of this size are still compact enough to result in rather hard neutron-flux spectra. The neutron-flux spectrum in the center of some typical FTR cores is shown in

Figure 7 for 5, 7, 9, and 11 v/o PuO_2 cores. As one would expect, the spectrum becomes harder as the fuel volume fraction is increased. This hardening causes the fissile fuel and structural material cross sections to decrease

FIG. 3. Core Section, Initial Case.

and the fertile fuel cross section to increase. For 5 v/o PuO_2 , more than 0.7 of the total flux is above 0.1 MeV; for 9 v/o PuO_2 , 0.8 of the flux is over 0.1 MeV. Figure 8 compares the FTR spectrum with spectra from some typical fast breeder reactors.

The fuel is zoned in the present 600-l reference core to flatten the power distribution. This zoning has only a very small effect on the spatial flux distribution. Some

of the characteristics of this core are tabulated in Table 2.

III. Control Statics

Provision for adequate control of the FTR presents another important problem for nuclear design and safety. The FTR control requirements are much more stringent than for a typical breeder reactor. Require-

FIG. 4. Fuel Loading vs Core Size for Cermet Cores.

FIG. 5. Flux-to-Power Ratio vs Reactor Size with Cermet Cores.

ments as to burnup, temperature defect, and shutdown margin add to about 10% Δk . Additional control requirements would arise if low-reactivity test elements were to be accommodated. These latter requirements

would probably have to be handled by rearrangement of driver fuel rather than by the mechanical system for reactor control.

Since the concentration of fertile fuel (Pu^{240}) is quite small and the buildup of fission products has a very small effect of reactivity, estimates of fuel life can be made fairly easily. If we consider only the burnup of the fissile nuclide,

$$\delta k \sim \left\{ \frac{\eta - 1}{\eta} \right\} \frac{\delta M}{M},$$

where δk is the variation in reactivity, η the effective eta of fuel, M the mass of fissile fuel, and δM the variation in fissile fuel mass. With an $(\eta - 1)/\eta$ value of about 0.6 for a six-week operating cycle for FTR, the control margin required for burnup is of the order of $\sim 0.04 \Delta k$. This simple approach for the determination of burnup requirements has been checked with one-dimensional burnup calculations, and the agreement is quite satisfactory.

The FTR cermet cores have an overall negative temperature defect of $\sim 0.015 \Delta k$, made up of a sodium coolant and fuel expansion coefficient (see section IV).

Adding a shutdown margin of $0.045 \Delta k$ results in an overall control requirement of 10% Δk .

A. Reflector Control

The possibility of reflector control was given serious consideration during the initial FTR conceptual design phases. Reflector control has the advantage of yielding an uncluttered core design and of providing more test space in the core. It has the additional advantage of making it possible to utilize many relatively low-worth control elements in a fairly accessible area.

A series of rather detailed transport calculations were made to ascertain the margins for available reflector control. Idealized calculational models for rotating drums as well as for translating reflector control elements were formulated. A nickel/ B_4C exchange was found to give the largest control swing.

As soon as practical engineering designs were considered in more detail, the control swings calculated for the fairly idealized models tended to decrease. This decrease arises because control elements must be kept reasonable in size, cannot be spaced too tightly around the core, and cannot be placed adjacent to the core.

For FTR core sizes, practically attainable reflector control margins seem to be about $\sim 0.05 \Delta k$. Compared to the $0.10 \Delta k$ requirements, these margins are inadequate, and hence reflector control was abandoned.

B. In-core Rod Control

To achieve an adequate reactivity swing in the FTR, in-core rod control has been adopted as the preferred

FIG. 6. Volume, Power, and Power-Density Relations.

control mode. In principle, one can use either fuel rods or poison rods for this purpose. Because movable fuel rods might present complex mechanical and cooling problems, major emphasis has been placed on the analysis of poison rods.

Even though the control rods will be placed in eccentric reactor positions, evaluation of central rod worth coupled with perturbation theory can be used to obtain estimates of off-center rod worths.

One-dimensional transport calculations were used to estimate the worth of centrally located B_4C rods. In Figure 9, the central rod worth as a function of v/o B_4C is shown. A complete Na/ B_4C interchange in the central zone causes a reactivity change of approximately 0.067. This reactivity change is significantly greater than that caused by interchanging driver fuel (7 v/o PuO_2 , 43 v/o SS, and 50 v/o Na) and sodium in the central region. For the fuel-sodium interchange, $\delta k \sim 0.018$.

To estimate the worths of off-center rods, perturbation theory can be used to obtain space-dependent reactivity coefficients of the component materials. The results of these calculations are shown in Figure 10.

The present design calls for three shim and three safety rods (plus one small power-regulating rod) with a reactivity worth of 2 to 2-1/2 % Δk /rod. This appears achievable with partially enriched boron carbide rods. Under normal operating conditions, the six control rods would provide the reactor with a very large shutdown margin. To guard against gross loading errors, it is presently proposed that a Nuclear Proof Test Facility (NPTF) be operated in conjunction with the FTR. This facility would simulate the reactor with the test ele-

ments in it and would be used to preview new test elements before insertion into the FTR.

IV. Temperature Coefficients and Reactor Kinetics

A. Temperature Coefficients

In the FTR cermet cores, the sodium coolant coefficient and the fuel expansion coefficient contribute to the overall negative temperature defect. The high-leakage FTR, in contrast with large breeder reactors, has a negative sodium void coefficient because the effect of leakage variation predominates over the effect of spectrum variation. The effect of complete sodium voiding was calculated for a range of FTR core sizes and corresponding PuO_2 fuel fractions. Sodium loss caused an appreciable decrease for all cases considered. For instance, for an 80-cm-diameter core, containing initially 50 v/o Na, 7 v/o PuO_2 , and 43 v/o SS, complete sodium loss resulted in a reactivity decrease of 0.0925 Δk . Furthermore, the spatial sodium worth is positive throughout the core.

It was also found that the effect of sodium removal is almost exactly a linear function of the amount removed. Hence, it is a fairly simple matter to translate the sodium void coefficients into sodium temperature coefficients. For the reference design this coefficient is about $-1\phi/^{\circ}K$.

Another important temperature coefficient contributing to the overall safety of the core is the cermet fuel expansion coefficient. Both radial and axial core expansion coefficients were calculated. For typical FTR configurations, the axial fuel expansion coefficient is about $-0.5\phi/^{\circ}K$.

FIG. 7. Flux Spectra for Various Cermet Cores.

Based on the expansion coefficients shown above, the net temperature defect associated with sodium and fuel expansion is about 1.5% Δk .

Since the FTR, as presently envisioned, is a fully enriched, hard-spectrum core, Doppler coefficients are expected to be small. The Doppler coefficients were calculated for a series of FTR configurations by means the Russian Cross Sections Set. Positive values of $T(dk/dT)$ from $\sim 6 \times 10^{-4}$ to 4×10^{-4} were calculated for a range of PuO_2 v/o from 5 to 9. This is a rather small value, less by about a factor of 20 than the axial fuel expansion coefficient, and would therefore not appear to present a serious safety problem.

B. Response to Ramp Reactivity Insertions

The meltdown of a fuel subassembly in a central test zone could be considered a credible accident in the FFTF. This argument stems from the fact that test fuel is precisely that—test fuel—presumably with less reliable safety characteristics than driver fuel.

The reactor response to various fuel slumping rates (simulated by reactivity ramps) has been computed under highly idealized conditions. The objective was simply to show the approximate severity of a problem (fuel slumping) which is perhaps of more importance in a fuel test reactor than in a large breeder reactor.

The reactor power as a function of time is computed from the kinetic equations and the reactivity expression

$$\rho(t) = at - b[T(t) - T(o)],$$

where t is the time, a the rate of reactivity insertion, b the reactivity shutdown coefficient, and T the temperature of fuel. The time-dependent fuel temperature is obtained from the simple heat balance

$$\frac{dT}{dt} = \frac{P}{C_p} - KT,$$

where C_p is the heat capacity and K the reciprocal of the thermal time constant.

FIG. 8. Comparison of Typical Core Spectra.

TABLE 2. ZONED REFERENCE CORE

	Core Region			
	I	II	III	IV
Number of Fuel Subassemblies	3	10	11	21
PuO_2 in Cermet (70% TD particles), v/o	17.7	19.8	22.1	27.7
PuO_2 in Cermet (90% TD particles), v/o	13.7	15.4	17.2	21.6
$\phi(E > 0.1 \text{ MeV})/\phi(\text{Total})$	0.71	0.71	0.70	0.67

The results are shown in Figures 11 for reactivity ramps from 1 to 50\$/sec. A few general observations can be made from these results. First, the rate of reactivity insertion must be less than about 5\$/sec to avoid severe power excursions. The rate of reactivity insertion caused by a meltdown and gravity collapse of the upper one-fourth of a central fuel subassembly is estimated to be given by

$$a(\$/\text{sec}) \sim 450 [\delta M/M],$$

where $\delta M/M$ is the fraction of fuel in the central subassembly. Therefore, to preclude a serious power excursion in this hypothetical accident, only about 1% of the fuel inventory should be placed in a central test region. Test elements of larger reactivity worth can be handled in off-center loops.

FIG. 9. Worth of Central B_4C Rod.

FIG. 10. Radial Dependence of Poison Worth.

FIG. 11. Power Profiles for Various Reactivity Ramps.

V. Nuclear Design Alternatives

Two major alternatives of the nuclear design are receiving considerations:

1. alternative driver-fuel concepts;
2. possible use of BeO for modification of the core spectrum.

A. Alternative Fuel Concepts

Although PuO_2 -SS cermet is the preferred FTR driver fuel at the present time, alternative fueling concepts are being actively investigated. Chief among them are PuO_2 - $U^{238}O_2$ ceramics as driver fuel. The presence of U^{238} would increase the reactivity life of the core, would result in a net negative Doppler coefficient, and would increase the effective delayed-neutron fraction. Furthermore, it would offer the opportunity of attaining higher driver-fuel temperatures. On the other hand, the fuel expansion coefficient would be less than for the cermet, and the sodium coefficient might be adversely affected. The possibility of fuel rearrangement in the ceramic might also have undesirable effects on reactor safety, although it is believed that fuel relocation can be made negligible by proper design. In addition, the flux-to-power ratio in the ceramic core is less than for the cermet core. Comparisons of fuel inventories and flux-to-power ratios for the cermet and ceramic cores are shown in Figures 12 and 13.

To examine the effect of U^{238} on reactor lifetime, a zero-dimensional burnup code was used. The total fuel volume fraction (fertile and fissile) was held constant while the ratio of fertile to fissile fuel was varied. For cermet cores, containing large amounts of stainless steel

FIG. 12. Fuel Loading vs Core Size for Cermet and Ceramic Cores.

FIG. 13. Flux-to-Power Ratio vs Reactor Size for Cermet and Ceramic Cores.

(~ 40 v/o SS), the addition of U^{238} had very little effect on reactor life. If the stainless steel content can be decreased as in a ceramic core, some increases of reactivity lifetime can be achieved.

Although a cermet core does not offer any great potential for increase of reactivity lifetime by the use of U^{238} , the addition of this resonance absorber may, nevertheless, be of interest in counteracting the effects of the positive plutonium Doppler coefficient. Calculations indicate that a fertile-to-fissile ratio of about $\frac{1}{2}$ would result in an overall zero Doppler coefficient for FTR-type cores. Although this addition of U^{238} is relatively modest, it might possibly have a detrimental effect on the physical endurance of the cermet.

B. Use of BeO for Spectrum Softening

For certain test situations, it may be desirable to soften the FTR neutron spectrum. Since beryllium is an

excellent moderator, BeO could be used for this purpose. As BeO is added to the core, the required driver-fuel inventory decreases and the specific power increases. For FTR core sizes, the replacement of 10 v/o SS by 10 v/o BeO results in a fissile inventory reduction of about 30%. Since the fission cross sections increase in the softer-spectrum core, this 30% reduction results in the overall flux increase of only about 10%. However, the fast flux (that above 0.1 MeV) is reduced in the BeO core.

A possible problem that could arise with the introduction of BeO into the core is the effect on the Doppler coefficient. Since the core is fully enriched, BeO tends to increase the positive Doppler coefficient of plutonium. This increase is a factor of about three for a core with 10 v/o BeO.

VI. Conclusion

To summarize: a conceptual design for a Fast Flux Test Reactor has been formulated.

Steel cermets are presently considered to be the FTR reference fuel, because of their expected reliability and desirable safety features.

Plutonium is considered to be the reference fissile fuel, because it yields the highest flux achievable for a given power density.

In-core control with poison rods has been selected because it can supply the large control margins necessary for the operation of a test reactor.

The present design must be considered to be tentative. It is flexible enough so that many alternative approaches can be incorporated if the current investigations should show that this is necessary.

APPENDIX: Methods and Cross Sections

A. Methods

Three types of calculation routines have been used in the reactor statics studies. For broad-scale survey work, a simple fundamental-mode calculation has been used. This routine is part of the Battelle-Northwest code CRUNCH.⁽¹⁾ The same point reactor formalism has also been applied in the calculations of driver-fuel burnup by means of the PYRE⁽²⁾ code.

For most of the design calculations, particularly those involving considerable geometric detail, one- and two-dimensional transport codes DTK and DDK of Los Alamos Scientific Laboratory were used.

For the reactor kinetics calculations the kinetic code KINETICS-I⁽³⁾ was used.

B. Neutron Cross-section Sets

In the earlier calculations, the 16-group Hansen-Roach fast reactor cross-section set⁽⁴⁾ was employed. In

TABLE A-1. EFFECTIVE MULTIPLICATION CONSTANT*
(Normalized to ANL set)

Cross-section Set	PuO ₂ , v/o			
	4	5	7	9
ANL-224	1.0	1.0	1.0	1.0
Hansen-Roach	1.002488	0.9968790	0.9940673	0.9951723
YOM	1.027956	1.028288	1.028095	1.027783

* Na = 50 w/o; SS = (50-v/o Pu).

TABLE A-2. MATERIAL BUCKLING (B²) × 10⁻¹³*
(k_{eff}² = 1.00)

Cross-section Set	PuO ₂ , v/o			
	4	5	7	9
ANL-224	1.393335	1.774857	2.522777	3.271783
Hansen-Roach	1.400043	1.764797	2.497220	3.245591
YOM	1.473527	1.870461	2.647579	3.426025

* Na = 50 v/o; SS = (50-v/o Pu).

the later calculations—those more closely directed toward the Conceptual Design—the recent 22-group ANL fast cross-section set (ANL-224) was employed. For typical FTR sizes and compositions, these two sets gave very similar results. Some comparison calculations were also carried out with the original YOM set.

Table A-1 gives the fundamental-mode multiplication constants produced by the three sets for various fuel compositions. For each fuel composition, the critical material buckling from the ANL-224 set was employed. The variation in material buckling is shown in Table A-2.

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Discussion

Mr. Okrent (ANL): How much reactivity is available in the plutonium oxide-stainless steel cermet between operating temperature and failure point, wherever you deem this, as it heats up in the transient? If one is thinking in terms of a transient in which one relies on expansion of this fuel to terminate the rapid transient, how much reactivity is available before you have failed the fuel?

Mr. Hofmann: The peak temperatures at which the fuel runs is about 600°C and for the kind of ramp rate we have calculated the rise is about 100°C. In other words, we would then be up to about 700°C, and 700°C is well below melting of steel which is around 1400°C.

Mr. Okrent: As I recall, it's about 0.1 % k for 100° rise in fuel temperature. If one assumes that one can heat up 500°, let's say, that would be half a percent δk to failure of the fuel.

Mr. Hofmann: But I'm not heating up 500°.

Mr. Okrent: And you have a delayed fraction of 0.2 %. So that means that if you had a rapid reactivity insertion of about 0.7 % δk , you would have no more prompt negative feedback from the fuel, is that correct?

Mr. Hofmann: I don't really know what you are saying. The fuel is not expanding any more in the transients we studied.

Mr. Okrent: I have a different transient in mind, one related to the maximum accident. How much reactivity would a fuel test in the central loop be worth? Is there a limit?

Mr. Hofmann: For the particular accident that we calculated, the rearrangement of such fuel if it were a coolant driver fuel, which would mean an extremely reactive element, the reactivity arrangement would bring about a dollar total. What I am saying is that an element may be worth 6 dollars. The particular melting model to which I have referred is a highly idealized model. The reactivity increase is about a dollar, and it occurs in a quarter second.

Mr. Okrent: This is our point of departure. It's not clear to me why a fuel element which is worth 6 dollars or more cannot, under the high forces due to a sodium bond or due to fission gas pressure, rapidly reassemble in the center of the core.

Mr. Hofmann: It doesn't have room. We assume, of course, that the fuel tubes did not fail. If I understand you correctly, what you would like me to do is take the top quarter and shove it into the center, and take the bottom quarter and shove it also into the center. This I think is physically possible in the sense that there is enough room to do this. So you might say we have analyzed the half maximum accident which you have just postulated, because we have only looked at the dropping of the top quarter into the center, which I think is very unrealistic.

Mr. Okrent: But you have dropped it under gravity. I'm also suggesting that a sodium bond or the fission gases may be considerably more rapid in achieving this assembly.

Mr. Hofmann: The only thing I can say about that is that I don't know what will happen when this element melts. It's interesting to look at the experiments that Huebotter has done with lead and water. One of the

conclusions that he derived from physics parameters is that he had not observed any rate faster than those induced by gravity. But I realize it is not a very strong answer to your question.

Mr. Bradbury (Harwell): We have demonstrated in the case of UO_2 -stainless steel cermets that one of the main advantages to be gained from these fuels is that you can take advantage of the initial starting porosity, that is to say, with a high degree of restraint offered to the oxide particles. The fuel can be forced to swell in on itself and in this way you can delay the onset of swelling

by changing the starting porosity of the fissile particles. In this connection, could you tell me what the starting density of the Pu-UO_2 would be in your fuel, and what target burnup you envisage?

Mr. Hofmann: I think that the densities of the ceramic states have been variously assumed to range between 70 and 90 of theoretical. I think we are aiming for about 15 % total burnup over three cycles. The core would only burn up 5 % during one cycle, but one cycle would be essentially $\frac{1}{3}$ of the total fuel like and $\frac{1}{3}$ of the fuel would be shuffled.

General Discussion

Mr. Zinn (Combustion Engineering): I would like to start the general discussion by thanking all of the speakers for their efforts this afternoon.

Now, I will begin by directing a question to Mr. Hofmann. You did mention that an experiment might melt down and add some reactivity, and you also said that this would increase the power in the reactor by about 50%, but that it would not result in any serious consequences for the core itself. How about other experiments which might be in the reactor?

Mr. Hofmann (Battelle-Northwest): We have not looked at this particular question in detail. If these experiments are oxides as they might well be, the temperature would depend on what temperature the experiment is run at.

Mr. Zinn: Well, my question was a little bit of a leading one, because you have now started to answer it by limiting the experiments. I am always impressed by the great costs of loops that are put into reactor cores, because they do involve separate cooling systems and so on. I think I would be especially impressed by the cost of putting loop experiments into fast reactors, so I want to ask some questions about the purpose of the loops. Are they to use coolants other than sodium?

Mr. Hofmann: The use of coolants other than sodium has been considered possible, but I don't.

Mr. Zinn: Such as gas, for instance.

Mr. Hofmann: Such as gas loops.

Mr. Zinn: How about steam or water?

Mr. Hofmann: I have heard people talk about steam or water, but I take a dim view of them at the moment.

Mr. Zinn: In light of the fact that you have to, perhaps, think of limitations on all experiments because of the failure in one experiment triggering failures in others, which might in turn trigger the reactor into failure, and because you have to put limitations on loops, and because they cost a lot of money, I wonder if a simple high-power density reactor core without loops might not be a better facility for testing fuel.

Mr. Hofmann: I think you cannot test the great variety of fuel that you may want to test in fact.

Mr. Zinn: Is the variety of fuel that we really have to consider for fast reactors so very great?

Mr. Hofmann: Do we know now what variety of fuels will be in 1980? I think at the moment some fuels may be considered to be the preferred fuels.

Mr. Zinn: If the fast reactor business survives to 1980, hopefully, it won't matter whether this particular facility tests the fuel or not. In other words, if we

haven't gotten some operating prototypes by 1980, which are testing fuels, I wonder if we should be worrying about what happens here.

Mr. Hofmann: There is one other point. The fuels that might be tested might not only be fuels to be used in breeder reactors, but could possibly be fuels that might be used in special-purpose reactors.

Mr. Zinn: I think that is a valid point. We don't know what the special-purpose reactors will be except that we can be pretty sure that they will exist. I think our whole history shows that, so a general test facility might be very useful for that purpose. But I want to repeat that I am very impressed by the difficulty of adding experimental facilities of flexibility to a reactor that causes the size of a conference that we have here devoted to talking about safety. This is a different type of problem than we face in our general-purpose research reactor. It is a somewhat more difficult problem, and I think I would like to congratulate you and your colleagues for tackling this type of design.

Mr. Zebroski (General Electric): Since the individual fuel channels are really all loops whether they are independent or not, and they are supported at rather great distances, one end from the other, do you have the possibility of an EBR-I type of reactivity effect due to bowing compaction?

Mr. Hofmann: We don't plan on that particular compaction as in EBR-I. In fact, friction pads are put between the tubes, and the tubes are essentially slightly forced out. We assume at the moment that our design can be made such that, in fact, we cannot get lateral motion which will result in the kind of compaction with which you seem to be concerned.

Mr. Zebroski: With tolerances and differential temperature problems this is not an easy answer. The second half of the question is, if indeed the experimenters should find sympathy with Dr. Zinn's viewpoint that it is rather expensive to put their experiments in these loops, does the core have the flexibility to permit conventional insertion of fuel between grid plates without the loop appurtenances?

Mr. Hofmann: There are about nine test positions located in the reflector. I would imagine that small pieces of fuel could be put into the reflector region.

Mr. Hennig (Battelle-Northwest): One of the advantages of the skewed-tube concept is that all of the positions in the reactor are, in essence, open loop positions. Each can be loaded with whatever kind of fuel one wishes to place inside, always within the envelope.

Of particular advantage is the ability to monitor whatever tests are put in the open loops.

Mr. Zinn: The skewed-tube concept actually was very close to being used in the design of EBR-I. Alex Langsdorf of Argonne originated the design, but we gave it up because of the added expense of building it. It seemed to be an expensive way of arranging the core, but it had very many attractions, as it still has. Of course, we know it has been used in one reactor or two since then, but not fast reactors. It did have its origin in the fast reactor concept.

Mr. Chernick (BNL): I have a question directed to either Webb or Schluderberg. It wasn't clear to me whether they had done a study of the transfer function of this reactor, in oscillations and reactivity and what not.

Mr. Webb (Babcock & Wilcox): We have a sort of two-fold approach. We want our reactor to be safe, but at the same time we want the performance. I heard one comment that you could raise pressure and your voiding deficit would go down, which is true, but your performance will go down. We are trying to design

the plant so that we can insert a time delay for the actual voiding to allow us time to get in our control rods. We have three or four pretty good ideas in the present design which we believe will allow this reactor to live with some positive coefficient. On the other hand, realizing we would like to get the coefficient negative, we have looked at adding solid moderators and we have also looked at changing geometry. We find that solid moderators help without too much detrimental effect on the performance. Although we have not completed a detailed plant analysis, it is in process now. As of now, the voiding deficit is about a little over 1%, which we are sure we can reduce.

Mr. Chernick: You say you haven't gone into any detailed calculations as to how you are going to control this reactor.

Mr. Webb: We have gone into it, we are in the middle of it, right now.

Mr. Chernick: You are convinced it is stable at operation.

Mr. Webb: Yes.

Session III
October 12, 1965

**Panel on Safety of Large
Fast Power Reactors**

Chairman: D. OKRENT
Argonne National Laboratory

Secretaries: D. Meneghetti
D. A. Meneley
Argonne National Laboratory

Contribution to Safety Problems Related to a 1000-MWe Fast Power Reactor Project

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I. Safety Philosophy

Safety philosophies of different countries are strongly dependent on mentalities, laws, and on the technical know-how of each nation.

The French approach is rather empirical and pragmatic, being very much based on past experience. It is not easy to mold it in rigid patterns. Though *a priori* safety criteria are widely used, they are not fixed in a more detailed manner than possible at the present status of knowledge. They remain very often unwritten. At least formally, designers are given much freedom. They know only that they must be able to convince every scientifically and technically competent non-expert of the validity of their hypotheses. It could be said that safety is achieved by the general agreement of learned people. The Reactor Safety Sub-Committee, whose authority is absolutely undisputed, embodies this agreement.

The example of the 1000-MWe reactor project which we shall discuss is a good illustration of what precedes. The authors are confident that they will be able to convince anybody of its good safety features. Actually, on a good many points they have already succeeded; on other points much research and development remains to be done. Such studies will no doubt lead to modifications of the values given hereafter. But it should be kept in mind that many other technical and economical reasons will also modify the project, not speaking of the lessons of Rapsodie and Phenix operating experience.

II. Design Data for a 1000-MWe Reference Reactor

II.1. The economic and technical trends for this fast-neutron project are as follows:

- 1) The internal breeding ratio is to minimize long-term reactivity variations, i.e., is to be close to 1. This leads to low enrichments which, in turn, give rise to a better use of fissile material.
- 2) Radial power flattening is to be achieved by the use of two concentric zones in a variable-enrichment core.

- 3) Core height is to be as low as is compatible with a technically sound pressure drop. (At constant breeding ratio the fuel volume fraction increases when the core height is reduced; see Fig. 1).

- 4) The time-dependent variation of the maximum/mean specific power ratio is to be optimized. Thus, at the beginning of a fuel cycle specific power maximum in the external zone is higher than that in the inner one.

All these trends lead to minimize investment of fissile material.

II.2 The so-called 1000-G core,* a description of which was presented at the Detroit Meeting in April 1965,⁽¹⁾ has been designed according to the above-mentioned principles. Neutronic data pertaining to this core are given in Table 1.

III. Safety and Dynamic Parameters

III.1. Temperature Coefficients of Reactivity

The temperature coefficients of reactivity are given in Table 2. The following remarks may be made:

We assume that fuel subassemblies have been provided with buttons in the reactor midplane and that the core is tight for nominal working conditions.

The fraction of the temperature coefficient originating in the fuel dilatation has been calculated in two ways, according to whether the fuel is bound or not to its cladding. When the fuel is bound to its cladding (normally at high burnup), the dilatation is identical with that of its cladding; when the fuel is not bound to its cladding (at the beginning of irradiation), it dilates freely. The real coefficient is no doubt somewhere between these two bounds.

The sodium-dependent fraction of the temperature coefficient is practically zero for the reactor. It is itself the sum of two terms each corresponding to one zone of the reactor. The positive term is $+ 0.25 \cdot 10^{-5} (\Delta k/k)/^{\circ}\text{C}$.

* This core differs essentially from the 1000 A core (presented at the Argonne Conference, October 1963) by a fuel volume fraction increase as foreseen by C. P. Zaleski at that time.

Finally, the fraction dependent on the Doppler effect is $-0.6 \cdot 10^{-5} (\Delta k/k)/^{\circ}\text{C}$ at nominal conditions. The mean temperature weighted on the Doppler coefficient is then 1500°K [$T(\partial k/\partial T) = 0.09$]. We assume that the Doppler varies according to $T^{-3/2}$ in the solid phase and that it is zero in liquid and gaseous phases.

In this calculation the Hansen and Roach cross-section set was used. The coefficient depends very much on the cross-section set used. For instance, it is shown in Ref. 1 that, for a given reactor, the Russian cross-section set can give a 20% higher Doppler coefficient than the Hansen and Roach set.

III.2. Power Coefficient

The power coefficients are given in Table 3. In order to calculate the power coefficient due to axial fuel expansion when the fuel is free in its cladding, we assumed that the expansion of a pellet was that corresponding to its own central temperature.

The Doppler effect provides the main contribution to the power coefficient. The positive fraction of the sodium coefficient is about $0.01 \cdot 10^{-5} (\Delta k/k)/\text{MW}$, that is, about 5% of the power coefficient due to the Doppler.

FIG. 1. Pressure Drop through the Core vs Core Height and Fuel Volume Fraction, the Associated Internal Breeding Ratio being Constant (0.93).

TABLE 1. DESIGN DATA FOR 1000-G REACTOR CORE

Core Volume	5200 liters
Number of Subassemblies	about 450
Ratio of Internal Zone Volume (V_1) to Total Core Volume (V)	0.54
Core Height	100 cm
M_c (Fissile Plutonium)	2370 kg
Enrichment of Zone 1	12.7%
Enrichment of Zone 2	18.2%
Internal Breeding Ratio	0.93
Sodium Coefficient in the Core	$+0.14 \cdot 10^{-5} (\Delta k/k)/^{\circ}\text{C}$
Sodium Coefficient in the Blankets and Gap Transitions	$-0.16 \cdot 10^{-5} (\Delta k/k)/^{\circ}\text{C}$
Total Doppler Coefficient	$-0.02 \cdot 10^{-5} (\Delta k/k)/^{\circ}\text{C}$ $-0.6 \cdot 10^{-5} (\Delta k/k)/^{\circ}\text{C}$ at 1500°K
$\beta_{\text{eff}} = 330 \cdot 10^{-5} (\Delta k/k)$	$l^* = 3.6 \cdot 10^{-7} \text{ sec}$

TABLE 2. TEMPERATURE COEFFICIENTS (IN UNITS OF $10^{-5} (\Delta k/k)/^{\circ}\text{C}$) OF THE 1000-G REACTOR

			Total isothermal coefficient		
	Free fuel		-1.5		
	Bound fuel		-1.7		
	Core including absorbing rods	Gap zones	Axial blankets	Radial blankets	Total
Sodium Expansion	+0.14	-0.10	-0.01	-0.05	-0.02
Steel Expansion	+0.16	-0.04	+0.01	-0.02	+0.11
Axial Expansion of Free Fuel	-0.15				-0.15
Axial Expansion of a Bound Fuel	-0.33				-0.33
Axial Expansion of Fertile Material			-0.01	-0.00	-0.01
Doppler Effect*	-0.60				-0.60
	Before tightening		After tightening		
Supporting Grid	-0.88		-0.13		
Effect of the Buttons	0		-0.75		
Bowling	0		+0.00		
“Splaying”	-0.02		0		

* at 1500°K .

III.3. Integrated Negative Doppler Reactivity

We define the fuel rupture temperature as a temperature for which the vapor pressure of the fuel is no longer negligible, that is, about 4000°K .

We call "réserves Doppler" the integrated reactivities due to the Doppler effect between the nominal working point of the reactor and the point where the fuel rupture temperature at the fuel hot spot is reached.

This "réserve" is about \$1.1, that is, $360 \cdot 10^{-5} (\Delta k/k)$, in the case of an adiabatical temperature rise.

If we admit that the safety system operates, the Doppler-controlled step reactivity increase is

$$[(360/2) + 330]10^{-5} = 510 \cdot 10^{-5} (\Delta k/k),$$

the dollar being taken at $330 \cdot 10^{-5} (\Delta k/k)$. The slow power rise which would lead to fuel vaporization at the hottest spot of the hot channel is 44% of the nominal power, that is, 1010 MW. It can be said that under the pessimistic assumption of a clad-bound fuel $250 \cdot 10^{-5} (\Delta k/k)$ needs to be introduced in order to bring the fuel to its rupture point in the hot channel. Then, the clad hot spot will be about 790°C. This means that in order to bring the fuel to its rupture point in the hot channel one needs:

$250 \cdot 10^{-5} (\Delta k/k)$ if one admits a safety-system failure;

$500 \cdot 10^{-5} (\Delta k/k)$ introduced at a very important insertion rate if the safety system is operating.

III.4. Sodium Loss Reactivity

Figure 2 shows the reactivity due to sodium voidage beginning in the reactor midplane.

Figure 3 shows the reactivity due to sodium loss beginning in the upper axial blanket and propagating downwards.

III.5. Reactivity Balance

The reactivity necessary for reactor operation can be broken down as follows:

TABLE 3. POWER COEFFICIENTS [IN UNIT OF $10^{-7} (\Delta k/k)/\text{MW}$] FOR OPERATION AT NOMINAL POWER

	Before com- pacting		Fuel free from expansion Bound fuel		Total power coefficient	
	Core	Gap Zones	Axial blan- kets	Radial blan- kets	Total	
Sodium Expansion	+0.7	-0.7	-0.0	-0.0	-0.0	
Steel Expansion	+0.9	-0.3	-0.0	-0.0	+0.6	
Axial Expansion of Free Fuel	-7.1				-7.1	
Axial Expansion of Bound Fuel	-1.8				-1.8	
Axial Expansion of Fertile Material			-0.0	-0.0	-0.0	
Doppler Effect	-19				-19	
Effect of the Buttons	-4.8				-4.8

FIG. 2. Reactivity Effects Resulting from Sodium Loss Beginning at the Core Midplane.

FIG. 3. Reactivity Effects Resulting from Sodium Loss Beginning in the Upper Axial Blanket.

at zero power from cold to hot reactor, $740 \cdot 10^{-5} (\Delta k/k)$;

hot reactor, from zero power to nominal power, $1010 \cdot 10^{-5} (\Delta k/k)$;

fuel burnup during 6 months, $600 \cdot 10^{-5} (\Delta k/k)$;

control requirements, $140 \cdot 10^{-5} (\Delta k/k)$.

The total reactivity requirements are thus $2500 \cdot 10^{-5} (\Delta k/k)$, the total rod worth being $4800 \cdot 10^{-5} (\Delta k/k)$; the shutdown safety margin is thus $2300 \cdot 10^{-5} (\Delta k/k)$, about \$7.

IV. Safety Analysis

Accidental sequences can be divided into three groups: those including necessarily such events which positively can be avoided by design; those including necessarily such events which cannot be avoided certainly and the consequences of which can and must be limited by design; other accidents.

IV.1. The following accidents belong to the first group:

a. *Reactor instabilities.* Taking into account the strong Doppler coefficient, the experience gathered in this field since the days of EBR-I, and the proposed core structure, the stability of the reactor presents no safety problem. Space-dependent instability is not likely to occur.

b. *Complete reactor vessel voidage.* With or without a safety system, a complete voidage will necessarily lead to a generalized core meltdown. It must and can absolutely be avoided by design, whatever may be the consequences. (One has only to make sure that rupture of the reactor vessel cannot be the ultimate event of an accidental sequence.) The technical means are well-known: piping entering from above, double-wall vessels, safety tanks, and even integrated circuits.

c. *Total, generalized, and instantaneous flow stop.* It is possible to ensure by design that there will never be a total loss of flow occurring at the same time in a great number of fuel subassemblies without having been noticed in due time for scramming. For this purpose, technical means are well-known: multiple loop systems, multiplying the number of independent electrical supplies, battery-supplied emergency motors, etc.

d. *Simultaneous jamming of all safety rods.*

e. Introduction of a significant quantity of slowing-down material into the reactor.

f. Loading accident (subassembly drop into a critical reactor).

g. Shim-rod ejection, the reactor being critical (though this ejection could be part of an accidental sequence; it cannot be the initial event).

IV.2. The following accidents belong to the second group:

a. Partial and instantaneous flow stop without immediate action of the safety system. Whatever might be the care taken, one cannot guarantee that there would neither be pump jamming nor loss of electrical

power, although it seems that a coincidence of these two events is highly improbable.

b. Loading accident (subassembly drop, the reactor being subcritical).

IV.3. Unfortunately, some accidents cannot yet be included in the above-mentioned groups and are still being studied:

a. Generalized and total loss of flow. Are we able or not to assure that following an earthquake or any other act of God, the after-heat cooling of fission products will always be possible without core meltdown?

b. Complete plugging of a subassembly. Such a plugging will certainly melt down the subassembly. Unfortunately, it is difficult to incorporate this accident in the first group.

Is the propagation to other subassemblies impossible? We hope to be able to demonstrate that this event is not autocatalytic and that it will remain localized.

Nevertheless it is clear that we are still missing quantitative data on the physical characteristics of a UO_2 - PuO_2 -stainless steel-sodium double- and even triple-phase systems, or full-scale experiments substituting our lack of data.

V. Conclusion

Maximum Hypothetical Accident

In France we are not much familiar with the notion of a maximum hypothetical accident. To us the term seems somewhat self-contradictory. Does "hypothetical" mean that this accident never occurs or that it hopefully will never take place? In this case the word "hypothetical," although well-chosen, is not quite meaningful. Or does it mean that one is unable to conceive a sequence of events leading to such an accident? In this case it is difficult to understand what is its real sense. Actually, the only way which should enable us to use the word "accident" is: either it already occurred, or we are able to describe it in a credible way from the beginning to the end.

It seems more probable that "hypothetical" means here that we deal with an accidental sequence of which we do not know yet the initial event. In this case, which seems to us the only reasonable meaning, every possible step must be taken to get out of such an unfortunate situation. Actually, the accident is being studied. The worst accident we are studying now is surely the complete core meltdown following the two accidents mentioned in IV.3, namely, plugging of the fuel subassembly and complete loss of heat sink.

We believe that too much attention has been paid to a core disassembly following high-insertion reactivity

and not enough to less explosive phenomena. In a way, core disassembly disregarding any possibility of fissile material reassembly is an easy means of terminating an accidental sequence. But what happens to a subcritical molten core? Is it also impossible to imagine slightly supercritical molten-down fissile assemblies

giving rise to low-intensity repetitive nuclear excursions?

REFERENCE

1. C. P. Zaleski and D. Smidt, *Fast Reactor Core Design and Safety Studies in France and the Federal Republic of Germany*, Detroit Meeting, April 1965.

The Karlsruhe Reference Design of a 1000-MWe Sodium-cooled Fast Breeder Reactor*

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A. Introduction

As has been described earlier,⁽¹⁾ the present phase of the Karlsruhe Fast Breeder Project, which extends to 1967, is aimed towards the investigation of the feasibility of 1000-MWe fast breeder reactors. One principal coolant being considered is sodium. The purpose of the present Karlsruhe study,⁽²⁾ called Na-1, of a 1000-MWe sodium-cooled breeder reactor was to serve as a reference design. On the basis of this reference design, competition between breeding, safety, and economy is being explored as quantitatively as possible. In particular, the impact of safety on breeding and economy is being studied. A first set of results was presented by Smidt.⁽³⁾

The proper choice of a design basis accident (DBA) and its impact on capital costs is being explored now and will serve as the basis of study Na-2. The forthcoming Na-2 study will be a thorough safety evaluation.

In this paper are described the present Na-1 design and its safety features with respect to incidents, the maximum hypothetical accident (MHA), and the DBA.

B. A Description of the Karlsruhe Na-1 Reference Design

A detailed description of the Na-1 design has been given.^(2, 4) The descriptions should be considered as a background for this chapter. The table in the Appendix gives the most important data of the Na-1 design.

1. Choice of Fuel

The fuel for Na-1 consists of mixed oxides of UO_2 and PuO_2 . The design objective was to arrive at fuel-cycle costs below 1 mill/kWh. This can be met without difficulties by the use of the oxides.⁽⁵⁾ The advantages of using other types of fuel, such as the carbides, would lie in the areas of breeding, including the aspect of lowering fuel-cycle or capital costs. We will touch that point later. The choice for $\text{UO}_2\text{-PuO}_2$ as a fuel is

* This paper has been prepared within the framework of the association EURATOM-Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

also consistent with the Karlsruhe approach of looking for steam cooling, which excludes the use of the carbides. Another advantage is that the technology of UO_2 as a fuel is available whereas that for UC is less developed and somewhat more difficult. All these have led to the use of $\text{UO}_2\text{-PuO}_2$ for the first large-scale fuel development at Karlsruhe.

One further argument for the oxides should be mentioned. Suppose that λ is the heat conductivity of the fuel in the rod of diameter R and α is the heat conductivity in the gap. Then the "effective" heat conductivity λ_{eff} between the center of fuel and the cladding is given by

$$\lambda_{\text{eff}} = \lambda \frac{2\alpha R}{\alpha R + 2\lambda}$$

If $\alpha R/2\lambda$ is small, then $\lambda_{\text{eff}} \rightarrow (\alpha R)(\alpha R/2\lambda)$. For a fast reactor fuel rod with carbide fuel, the value of λ_{eff} is small for three reasons: 1) R of a fast reactor rod is small (the situation is different for thermal reactors); 2) the problem of inner corrosion between fuel and clad is more serious for the carbides than for the oxides, and affects the value of α ; 3) the high value of λ in the case of the carbides emphasizes the difficulties of getting a comparable conductivity $\frac{1}{2}\alpha R$ in the gap.

2. Choice of Cladding and Structural Materials

In four recent 1000-MWe studies, austenitic stainless steel, with preference given to Types 316 and 304, has been chosen as cladding materials. In the Na-1 study we took Incoloy 800 as a reference point for advanced Incoloy types. On the other hand, stainless steel 16-13 (CrNi) may be used as an alternative for high creep strength and might possibly be used in the Na-2 design. These two types of alloys seem to us to be more promising than Types 316 and 304. We expect these relatively low-nickel alloys to have, on the one hand, an acceptable corrosion behaviour with respect to mass transfer and, on the other, not to be harmful with respect to the neutronics. The neutronic evaluations with Incoloy 800, with its larger nickel content, showed that there is no great neutronic penalty for the use of such alloys.

The sodium temperatures are lower than in the American studies. The mean sodium outlet temperature is 580°C (instead of 600–650°C as in the US studies). If the hot-channel factor is included, the maximum outlet temperature of sodium in our study is 630°C. The mean inlet sodium temperature of the steam generator then amounts to 560°C, thus leading to a steam temperature of 540°C. The sodium outlet temperature of the steam generator was taken as 360°C; the inlet temperature into the core will be 430°C.

Having fixed these temperatures, one now can make a proper choice of the structural materials. The first choice is, of course, austenitic steel on the hot side of the loops; X8 CrNiNb 1613 is the only stainless steel which is allowed for high-temperature use in Germany. But for economic reasons we would prefer Type 304 or 316 and will discuss this question with our supervising authorities. We are also considering niobium-stabilized ferritic steel, 10 CrMoNbNi 910, advocated by the INTER-ATOM group. We expect further economic gains by use of this steel. Furthermore, we think it would be the most economic solution to use normal unstabilized ferritic steel 10 CrMo 910 for all cold parts of the loops with temperatures below 450°C. Even the reactor vessel might be manufactured with this relatively cheap steel. Also this cheap ferritic steel may be used for the concentric in- and outlet tubes to the heat exchangers.

The most difficult part of the sodium circuits with respect to materials are surely the tubes of the steam generator for which we have no simple solution at hand. We think of a combination of normal ferritic steel for the evaporator section and of austenitic steel for the superheater part of the steam generator. (In this point the data of our study are misleading. Not an alternative but a combination of the austenitic and ferritic steel is intended.)

3. The Core Design

Originally a number of rather unconventional core designs were considered, for example, spherical fuel elements of canned oxide fuels. Ultimately, however, we arrived at a design with conventional pins in an hexagonal subassembly (see Fig. 1). The core has two zones of two enrichments optimized for power flattening and of the same volume. It also gives automatically a good Doppler coefficient ($7 \cdot 10^{-6}$ at 1400°C) because the center zone has the lower enrichment. The sodium void effect is acceptable. After considerable parametric studies, the height-to-diameter (H/D) ratio was chosen to be $\frac{1}{3}$ (it did not appear to be necessary to go to more extreme values or other extreme solutions in order to enhance leakage). A H/D value of $\frac{1}{3}$ does not decrease the Doppler coefficient too strongly and also

does not influence the fabrication costs of the fuel cycle too much. As will be pointed out below, a core of a low H/D value may lead to somewhat lower values of the kinetic energy release in case of a major accident.

The fission gases are given to a gas plenum at the lower end of the core. Therefore we have a vented to plenum scheme (the aspects of a vented to coolant scheme have been evaluated by Sommer and Smidt⁽⁶⁾). There seem to be no major economical advantages but an improvement of safety.

The radial blanket has also two regions. In the outer region uranium metal is used in order to reduce the size of the radial blanket (40 cm), but because of the fuel cycle this appears not to be a good solution. This leads to the core arrangement shown in Fig. 1A.

The reason for having the plenum for the fission gases at the lower end is based on the lower sodium temperature. This leads to an 80-cm length for the plenum, saving about 25 cm. The height of the subassembly is thereby reduced, as is also the pressure drop of the sodium along the subassembly, therefore requiring somewhat less structural material. In addition, the thermal stresses in the subassembly are lower because the plenum does not participate in the displacement of the upper parts of the subassembly. The coolant fraction (α) in both core zones is 50%. The principal argument for that choice was the rather low value of the pressure drop in the core (4.25 kg/cm²) which, in turn, reduces the necessary volume fraction for the structural materials (to 10%). The variation of the coolant fraction has already been discussed.⁽³⁾

The fuel subassemblies are fixed in a lower grid plate; the holddown is hydraulic, the direction of coolant flow upward. Among other reasons, the direction of natural convection and the pressure of the cover gas lead to the choice of upward flow.

Much attention was paid to the question of refueling (see Fig. 2A–C). There are three loading devices in the region above the core; each covers $\frac{1}{3}$ of the core and blanket area. Normal operation takes place under sodium and makes use of power steering. As long as everything is in order, quick loading and unloading is easily achieved. The principle of power steering immediately detects a mechanical failure. Now it is possible to lower the level of the sodium pool and to have optical access to the upper core surface by a periscope device. Lowering of the sodium level is possible because the sodium pool above the core is a mixing chamber. Presently 90% of the outcoming flow of the core is taken away from the outlet nozzles by thin vertical plates; only 10% of the flow reaches the outlet point directly. This decreases the effect of temperature shocks to the walls containing the sodium. Also the vertical plates act as walls in case the sodium level is lowered;

FIG. 1. Fuel Subassembly.

in this case a sort of under-flow hot cell with optical access by a periscope device is established. The unloaded subassemblies are placed in transfer baskets at the outer perimeter of the radial blanket; from there they are taken away, after a cooling-off period of 100 days, by a rather simple device.

The use of three loading devices leads to very short interruptions for refueling. As long as more than one loading per year appears to be necessary, this may be of great importance for the question of availability. In our case we have a rating of 1 MWt/kg fissionable material and a three-batch loading scheme leading to refueling periods of 196 days. This is reasonable but not in coincidence with the yearly maintenance cycle; therefore very short interruptions for refueling are desirable.

It is probably more important to look for refueling periods of one year rather than for high ratings if they are associated with short refueling periods. The fuel-cost benefit for such high ratings may be obtained only if a power refueling appears possible. This possibility should not be excluded for future reactor studies, however.

A refueling period of one year could increase the economic potential of the plant. Such a period can be obtained with high values of the excess reactivity. In most cases this would hurt breeding and economy except when the internal breeding ratio is somewhat larger than one.

In our case the excess reactivity for bridging a burnup of $\frac{1}{3}$ (100,000) MWd/tonne is 2.4%. This value is the sum of plutonium burnup (0.75%) and fission product poisoning (1.65%). The internal breeding ratio is 0.89 (inner zone 0.556; outer zone 0.334).

The nuclear core calculations were based on an equilibrium composition of plutonium isotopes.⁽⁷⁾ This equilibrium composition is such that only that amount of freshly bred plutonium from the blankets goes to the core which is necessary for making up the plutonium burnup in the core. In all probability this will not be the scheme for Na-2. It is intended to have a mixed core-blanket aqueous reprocessing scheme instead. After one refueling period, $1/n$ of the core and $1/2n$ of the radial blanket would be mixed together in the process line. If the internal breeding ratio is in the neighbourhood of one, this leads to one unique plutonium isotope composition at all stages of the fuel cycle and there would be no plutonium holdup in the radial blanket.

4. The Design of the Primary Circuit (see Figures 3, 4, and 5)

The primary circuit is of the loop type and has a cover gas of argon. With a large reactor, a pool type

FIG. 1A. Division of Reactor Core into Zones.

a, innere Spaltzone (Zone 1); b, aussere Spaltzone (Zone 2); c, innere Brutzone (Zone 3); d, aussere Brutzone (Zone 4); e, untere axiale Brutzone; f, obere axiale Brutzone; g, Spaltgesspeicher.

might lead to serious problems, such as unforeseen flow patterns, thermal stresses in the structural plates for the pumps and the heat exchanger, radial expansion of the components in the pool with respect to the concrete shield plug, difficulties of having easy access for maintenance and for heavy instrumentation, and others. A solution of the loop type can be better overlooked and the design is more straightforward. But it is not excluded that ultimately the pool type is the right choice.

The loop type of solution for Na-1 provides two parallel circuits, that is, two intermediate heat exchangers are incorporated. Each circuit has two pumps in parallel, making a total of four. A higher number of heat exchangers does not seem advisable because this component should be a reliable one and a higher number of heat exchangers could perhaps even increase the failure probability. In the same sense, the use of four pumps is assumed to be optimum. By suspending the heat exchanger together with the pumps, radial expansion is provided, a good feature. All components of the primary circuit are in chambers of a concrete structure. The heavy motor drives for the sodium pumps are above the main floor, so that they are borne directly by the concrete structure; the pumps themselves are directly connected to the intermediate heat exchanger. The reactor tank itself (see Figures 6 and 7) has a double wall in order to guarantee that the sodium level is always above the reactor core. Convection from the reactor vessel to the intermediate heat

FIG. 2A. Transfer through Interlock of Fuel Transport Container into the Reactor Vessel.

FIG. 2B. Preparation of the Reactor for Fuel-element Exchange.

FIG 2C Fuel element Exchange under Sodium

FIG 4 Vertical Section of the Primary Cell

Fig. 7. Horizontal Section of the Reactor.

exchanger provides a coolant flow large enough to pick up about 7% of the nominal heat production. As a first-order approximation, it was assumed that there will be no main primary pipe failure. Together with the cooling capability by natural convection this eliminates the necessity for an emergency cooling system. As we will see later on, the Na-1 design does not make too pessimistic assumptions in order to study the capital cost potential of this kind of a reactor. The systems and safety analysis of Na-1 may well lead to the requirement that there be an emergency cooling circuit for the coolant of the reactor vessel of the Na-2 study.

All main pipes are coaxial. Therefore it is possible to have all outer pipe walls and all the containers at the same temperature, namely, that of the sodium entrance, 430°C. On the other hand, this leads to great difficulties in having gate valves at the exit of the reactor vessel. With the intent of avoiding a main pipe break, no gate valves were provided in the Na-1 design. This is being subject to a careful re-evaluation for Na-2.

The reactor tank is located eccentrically in the containment building in order to have a small diameter (28 m) for the reactor building.

The secondary circuit leaves the reactor building. Steam generators and turbines are outside.

Advantage can be taken of the fact that there is a low pressure drop in the primary circuit (5 kg/cm²). This, together with the rather low constant overall temperature (430°C) for vessels, containers, and the outer wall of the main pipes, leads to a possible use of perhaps a cheap ferritic material for these components. The 20-MWe KNK-reactor at Karlsruhe is designed along this line of arguments.

5. The Neutronic Evaluation

The neutronic evaluation was done in several steps. First, a parametric survey was made by calculating a large number of one-dimensional—one-core-zone reactor configurations. The results of these one-dimensional reactor calculations were checked against 8 two-dimensional—one-core-region reactors which fit thermodynamically in the scope of the design. The one-dimensional calculations at this stage were made with a slightly modified 16-group YOM set; for the 8 two-

dimensional calculations, a condensation into 5 groups was necessary for reasons of computer time. In the process of condensation, different weighting spectra were used for the core, and the axial and radial blankets. These spectra were obtained by one- and two-dimensional calculations. The results obtained after the condensation were cross checked and appeared to be satisfactory.

In the second step, optimization of the two-region core was accomplished. Again the 16-group YOM set in a slightly modified version was used. The optimization was done with respect to power flattening obtained by the use of proper enrichment in the two zones. This optimum appeared when the two core regions were of the same volume. These core dimensions and enrichments were used for the reference reactor. As mentioned before, the isotopic composition of plutonium was that of an equilibrium core with separate core-blanket management.

With these first data for the reference reactor at hand, a careful neutronics calculation for the reactor was made. The results were first calculated with a one-dimensional code and then cross checked against a 5-group two-dimensional S_2 calculation. The agreement was quite good. Thereafter the calculation was repeated with the 26-group Russian set, because this set had all materials of interest available as self-shielded, temperature-dependent group constants, including those for the fission products. The details of the flux distribution, in particular in the radial blanket, were also considered.

The third step was to calculate the power and energy coefficients. The Doppler coefficient was calculated by means of the Karlsruhe code DCP (Doppler Coefficient by Perturbation), using as spectrum the results calculated with the Russian 26-group constants set (ABN). The neutron lifetime and β_{eff} were calculated on the basis of the 16-group modified YOM set. The sodium void effect was calculated again, using a two-dimensional code, "Twenty Grand," and 6 neutron groups coming from a condensation of the Russian 26-group set. The results were compared with the corresponding one-dimensional results. The difference between the two maximal sodium void effects was less than 10% (compare Tables 1 and 2). One should note

TABLE 1. SODIUM VOID EFFECTS (ONE-DIMENSIONAL CALCULATION BY USING THE ABN-SET)

	Void ⁽¹⁾	Void ⁽¹⁺²⁾	Void (overall)	Void (max)
Δk	0.01032	0.00641	0.005445	0.011747
ρ	\$2.87	\$1.78	\$1.655	\$3.27

(1) and (2) referring to zones 1 and 2; (max) to $r < 118$ cm.

TABLE 2. SODIUM VOID EFFECTS (TWO-DIMENSIONAL CALCULATIONS WITH A 6-GROUP SET COMING FROM A CONDENSATION OF THE ABN SET)

	Void ⁽¹⁾ *	Void ⁽¹⁺²⁾	Void (overall)	Void (max)
Δk	0.00654	0.00375	-0.00479	0.01108
ρ	\$1.8104	\$1.037	\$-1.326	\$3.066

* See Table 1 footnotes.

the difference in sign for the overall effect. This shows that calculational difficulties still exist.

Much attention was paid to proper calculation of burnup. The basis was a model for batch loading. Local groups of n subassemblies were formed. Each subassembly of these local groups belonged to a different batch. Unloading of a subassembly occurs in the neighbourhood of 100,000 MWd/tonne. If the number of batches is n , the average burnup in the local groups varies only for 100,000/ n between two successive refueling steps. A choice was made to have $n = 3$ in the inner zone and the inner region of the outer zone, and $n = 4$ in the outer region of the outer zone. This leads to a fuel lifetime of 3.196 d and a reactivity change of 0.7% due to plutonium burnup; if fission products are included it will be 2.5%. The choice of $n = 3$ and $n = 4$ in the outer zone of the core instead of $n = 3$ in the whole core increased the average to maximum burnup by 6%.

Finally, reactivity values were calculated on the basis of a perturbation theory for the interchange of a subassembly of the outer with inner zone and for the sodium voiding of the central subassembly. The results are summarized in Table 3. Values have been published,⁽²⁾ but the calculation for the reference design is being improved continuously. New results have been calculated with the KFK 26-group set of constants.⁽⁹⁾ This set has the same group structure as the ABN set but starts from different microscopic data and uses a typical spectrum of a sodium-cooled 1000-MWe reactor as a weighting spectrum. The main changes in the results are the following:

- critical mass is increased by about 6%;
- the Doppler coefficient is decreased by about 25%;
- the maximum sodium void reactivity value increases from about \$3 to about \$7.

6. The Accident Analysis

The accident analysis for the Na-1 design is now under way. A first version has been prepared.⁽²⁾ First results are reported by Smidt *et al.*⁽³⁾

The basic approach for the accident analysis arose from an overall view as to the possibilities of accidents. We have four basic types of accidents:

- a) loss of coolant flow;
 - b) accidental reactivity ramp insertions;
 - c) major breaks in the pipes containing sodium;
 - d) plugging of the central (or an inner) subassembly.
- a) The accident due to loss of coolant flow happens if all four pumps fail, as would occur if the regular power supply *and* that of the emergency drive fails. If, in addition, as a third coincidence, the safety system fails, the analysis indicates that one has a time interval of 80 sec before sodium begins to boil. After this

TABLE 3. THE EFFECTS OF FUEL BURNUP FOR VARIOUS NUMBERS OF BATCHES*

Number of batches in the inner and outer zone of the core	Δk_{eff}		Average to maximum burn-up**	Time difference between successive refueling steps
	Without fission products	Fission products included		
Inner Zone Outer Zone $n = 3$	0.76%	—	79%	196 d
Inner Zone Outer Zone $n = 3$	0.74%	2.5%	85%	
Inner Region $n = 3$				
Outer Region $n = 4$				

* Calculation by using the ABN set.

** Averaged over the radius r .

time interval a sodium voiding accident is to be expected.

One can argue about the probability of a simultaneous mechanical failure. In that event only one further conditioned event, namely, that of the failure of the safety system, has to happen. But counting the simultaneous mechanical failure of all four pumps and, in particular, that of the safety system is a picture which is too rough, because these events are in themselves multiconditioned. In agreement with other assumptions concerning the mechanical design (see Sect. 4 above) we assume that there will be no such simultaneous mechanical failure. This leads to the conclusion that in the two conditioned cases of pump failure (regular and emergency drive failure) the safety system has to act within 80 sec.

b) Accidental insertions of reactivity can happen at the beginning of a sequence of events or as the consequence of sodium voiding or core compaction. Sodium voiding and core compaction lead to high values of the associated ramp rates (10–100 \$/sec), whereas reactivity insertions at the beginning of a sequence of events lead to low values of ramp rates. For example, the loading accident for a central subassembly gives \$0.7 in 350 msec, that is, 2 \$/sec. Again, sodium voiding of the central subassembly leads to a change of \$0.07 in, say, 100 msec, resulting in a ramp rate of 0.7 \$/sec. The size of an accidental reactivity insertion by a control rod runaway depends on the particular design, but will always be at ramp rates below 1 \$/sec.

In the analysis of reactivity insertions the class of ramps between 0 and 20 \$/sec was considered. It is useful to make two distinctions. If a reactivity input is below \$1, the resulting excursion is limited as to reactivity value; it is largely the total reactivity value and not so much the ramp rate which determines the size of the excursion. If the reactivity is larger than \$1,

it is in the region beyond \$1 the ramp rate and not the final reactivity value which determines the size of the excursion. The other distinction is that between low ramp rates leading to sodium boiling before fuel starts melting or vaporizing, and ramp rates leading to fuel melting or vaporization before sodium starts boiling. The limiting ramp rate for this distinction is a function of the reactor properties, in particular of the Doppler coefficient and the average fuel temperature. In certain cases melting of the cladding instead of sodium boiling can be the relevant criterion.

The reactivity insertion which the reactor can accept without action of the safety system is \$0.25. These are rather weak insertions. For all other insertions stronger than that (reactivity value and ramp rate), scram by a safety system is required. Therefore the logical question is now: What kind of a safety system is required? The detailed studies of Smidt *et al.*⁽³⁾ give the answer for the Na-1 design in the form of a parametric study. The result is that for a delay time in the safety system of 50 msec and a realistic shutdown reactivity versus time characteristic having a 150 \$/sec ramp rate in its linear part and having a Doppler coefficient of 6×10^{-6} at 1400°K, a permitted accidental reactivity ramp of 10 \$/sec is allowed if the beginning of melting is the limit. The value of 150 \$/sec assumes, of course, that all shut-off rods are in action (10 rods at \$1.5 each). A delay time of 50 msec is a rather high value and means that more or less conventional shut-off systems can be applied. If the delay time would be 10 msec, about 14 \$/sec instead of 10 \$/sec could be handled. The influence of the Doppler coefficient is limited here. The reason is that its action reduces the net reactivity to values close to \$1 if the ramp insertion has led to reactivity values larger than \$1. Before prompt criticality is reached, the Doppler is of very small influence because the large periods of the power-level increase do not change the fuel temperature during that period. If $A = T(dk/dT)$ is the Doppler constant, ΔT the permissible fuel temperature interval in the case of an excursion, T_0 the fuel temperature at normal operation, and r_s the accidental ramp rate, the time t_s to fuel melting or vaporization is given by

$$t_s \sim \frac{1 + A \ln \left(1 + \frac{\Delta T}{T_0} \right)}{r_s},$$

Only if $A \ln [1 + (\Delta T/T_0)]$ gives a significant contribution compared to unity does the Doppler have a stronger influence on the characteristics of the safety system. That is the case for carbide fuel at low rod power. For a 50-msec delay time, a Doppler coefficient of 4×10^{-6} leads to 9.3 \$/sec, a Doppler coefficient

of 8.5×10^{-6} to only 10.5 \$/sec, permissible accidental ramp rates. The function of the Doppler coefficient is therefore to serve as an inherent safety mechanism against supercritical fast reactivity insertions and particularly to guarantee stability. The impact of the uncertainties in the nuclear calculation of the Doppler coefficient on the design of the safety system is limited.

One important conclusion to be drawn from these arguments is that an unconventional safety system with very short delay times (1 msec or so) is required only in the case of accidental reactivity ramps much larger than, say, 20 \$/sec. But it is difficult to think of such accidental ramp rates in the beginning of a sequence of events. Only sodium voiding of the core could give larger values, but according to arguments sodium voiding is only a consequence of accidents which are accompanied by a failure of the safety system. We therefore conclude that for the safety system a conventional version of it is sufficient. This differs from the earlier Karlsruhe thinking.

Whether an accidental ramp-reactivity insertion is simple or complex is a subject for a more detailed safety discussion, but it is of no importance here in this context.

c) Major breaks of piping carrying sodium between the reactor vessel and the intermediate heat exchanger interrupt the circuit of natural convection for a primary circuit of the loop type. Such a break would require shut down and emergency cooling in the vessel.

According to the arguments of Sect. 4, such a major break of a pipe is excluded in the Na-1 study by definition, although it may be considered in the Na-2 study. The reactor vessel itself has a double wall, and therefore a break is definitely excluded.

d) Before considering an accident leading to a major sodium voiding of the core, the accidental plugging in the central subassembly shall be considered. If such a plugging takes place, the response of the neutron flux might be very small because of the low reactivity value of sodium voiding of the central subassembly. No reaction of the safety system therefore takes place, and the sodium in the central subassembly might boil. Smidt⁽³⁾ has emphasized that, e.g., in the case of the Na-1 design at 28 % of normal flow, boiling of the sodium occurs, but already at 22 % of the normal flow unstable flow conditions may occur, thus keeping the coolant in the channel. This leads to the beginning of a more serious sequence of accidental events which will be discussed below. One can argue that boiling can perhaps be detected acoustically and therefore be used as an initiation of a scram signal. As long as the safety system works this kind of an accident might therefore be prevented.

But it is also possible that the sodium superheats and

does not boil.⁽³⁾ This can possibly not be detected by instrumentation. Thereafter sudden desuperheating may well lead to destruction of the central subassembly and its neighbours, thus leading to major core damage and ending in a serious core explosion. This will be discussed below. This difficulty of an undetectable plugging and superheating accident is an unresolved problem. In order to attack this problem much more has to be known on the mechanism of two-phase flow and superheat, both theoretically and experimentally. Fischer and Häfele⁽¹⁰⁾ and Friz⁽¹¹⁾ have made a first step in this direction.

Along the same lines one also has to fear a break of the cladding at the fission gas plenum. This might propagate and result in a plugging of the coolant channel. This danger is somewhat emphasized in the Na-1 design, in which the fission gas plenum is at the lower end; putting it on the top of the fuel pin probably does not change the situation drastically.

We realize that all considerations of this chapter with the exception of the plugging and superheating accident do not lead to any larger accident in case the safety system works; in particular we realize that the size of the sodium void effect did not influence our considerations. The mechanism of the plugging and superheating accident therefore requires further thorough investigations. But the accidents of types a-d lead to major damage in case of a simultaneous failure of the safety system. The sequence of events leading to such major damage all involve sodium voiding of major parts of the core. This leads to the maximum hypothetical accident.

7. The Maximum Hypothetical Accident (MHA)

Assume a reactivity ramp insertion as discussed under b) in the last section. The accidents discussed there have such a low value for rate of insertion that sodium boiling starts before fuel melting ends and vaporization of the fuel starts. In the case of the Na-1 design, the limit for these reactivity ramps is 3 \$/sec. We also assume that the safety system fails to react. Now it is not sufficient to assume that boiling begins at the upper end of the core where the sodium void effect is negative. According to the argument of Smidt,⁽³⁾ we have to assume unstable flow conditions. In this case voiding starts at the center of the core at the place of the largest positive void effect. If one wants to evaluate the ramp rate associated with sodium voiding, the situation is as follows:

The original calculation of the maximum integral void effect in the Na-1 reference design gives a maximum value of \$3 for sodium voiding; the latest results indicate a higher value, close to \$7. In the analysis of this accident the uncertainty in the nuclear calculation

of the void effect appears for the first time to be of relevance. If one wants to continue to evaluate the maximum ramp rate, a second uncertainty appears, namely, that of the time scale for ejection of the sodium. Here the full scope of the two-phase flow of sodium appears. As Fischer and Häfele show,⁽¹⁰⁾ for low amounts of gas in the two-phase mixture one has to expect a rather low value of the velocity of sound (easily as low as 10 m/sec). This means that all kinds of shock-wave complications have to be expected in the pattern of flow. The time scale is probably uncertain to a much higher degree than the reactivity value of voiding. Both uncertainties combine if the ramp rate is calculated. Presently we bridge this difficulty by assuming simply a ramp rate of 100 \$/sec.

After the original initiating excursion one finds that, in addition to the boiling of sodium, the fuel melts. The FORE analysis indicates that the hotter part of the fuel is already molten when the boiling starts. If the molten fuel touches the can, the heat resistance in the gap disappears and the temperature of the cladding is suddenly increased by about 500°C. The immediately following formation of bubbles of sodium vapor is consistent with the above-mentioned second uncertainty of the time scale for sodium voiding. This formation of bubbles of sodium vapor means that there might be a sodium-steam explosion of the BORAX type.

After the melting of the can fuel, compaction inside and outside of the original fuel element could start. This would lead to another reactivity ramp. Both ramps, that of sodium voiding and that of fuel compaction, would add if the two involved mechanical processes would not interfere. They would lead to a nuclear excursion and subsequent explosion. Superimposed is the sodium-steam BORAX-type explosion.

We have not yet a clear idea about the patterns of interactions of these two processes. Here is another open problem of great importance.

It is useful to realize the difference between this kind of an analysis and that for a small reactor with metallic fuel elements, like EFFBR or EBR-II. In the latter case we have a negative void effect. Any displacement of somewhat larger amounts of sodium shuts the reactor down; the two ramps involved there subtract and don't add as above. The metal fuel melts easily and drops to the bottom of the core, perhaps forming a second critical mass. This second critical mass is not located at the place of the first critical mass, that is, the core. The excursion in this second critical mass is known as a second excursion and constitutes the MHA. In our case, the accidental excursion occurs at the place of the original core and the BORAX type of sodium-steam explosion is superimposed; the ramps add and there is no time for a quiet meltdown.

The plugging and superheating accident for the central subassembly can develop a similar final picture. Sudden desuperheat destroys the central subassembly and its neighbours. The probable interruption of the coolant flow there leads to boiling of sodium (or to superheat of sodium, which in turn would increase the area of subassembly destruction even further by a repetition of this mechanism). The ejection of sodium from a somewhat greater number of subassemblies leads now to a reactivity ramp of a relevant height and could initiate the scram rods. But here we have to assume that, even if the safety system works, the safety rods don't drop into the core because of mechanical distortions there. Now the sequence of events develops as above.

The amount of buildup of kinetic energy in an excursion of a given ramp rate was calculated by Böhm⁽¹²⁾ and later by Küsters. They used a Bethe-Tait approach involving a spherical model for the supercritical mass. It is realized that extremely flat cores of, say, $H/D = 1/6$ may better be treated as a slab model, which should give smaller energy releases. This will be done.

In the Bethe-Tait type of calculation there exists another uncertainty, namely, the uncertainty as to the equation of state for a realistic fuel, which is a mixture of UO_2 - PuO_2 and fission products containing fission gases to a various degree.

At Karlsruhe originally an equation derived from the measurements of Ackermann⁽¹³⁾ were used. This led to a rather quick pressure buildup and limited the energy release nicely. A larger energy release is obtained by the use of the Wolfe-Meyer equation of state.⁽¹⁴⁾ A safer approach is obtained by the use of the latter equation of state. The Doppler coefficient is included in the calculation. This results in a remarkable reduction of the energy release. The total energy release and that of kinetic energy for different values of the Doppler

TABLE 4. ENERGY RELEASE FOR $\alpha_R = 500$ \$/SEC.

Doppler Coefficient	$\Phi_0(n/cm^2\text{-sec})$	E (kg TNT)
-8×10^{-6}	10^{10}	256
	10^{14}	48
	10^{18}	114
-4×10^{-6}	10^{10}	539
	10^{14}	84
	10^{18}	280
-1×10^{-6}	10^{10}	5 474
	10^{14}	2 848
	10^{18}	882
0	10^{10}	29 814
	10^{14}	18 112
	10^{18}	8 830

TABLE 5. ENERGY RELEASE FOR $\alpha_R = 100$ \$/SEC.

Doppler Coefficient	$\Phi_0(n/cm^2\text{-sec})$	E (kg TNT)
-8×10^{-6}	10^{10}	34
	10^{14}	7.4
	10^{18}	17
-4×10^{-6}	10^{10}	79
	10^{14}	85
	10^{18}	85
-1×10^{-6}	10^{10}	43
	10^{14}	284
	10^{18}	239
0	10^{10}	6 019
	10^{14}	4 196
	10^{18}	2 055

coefficient at 2500°K, the initial flux, and two ramp rates are given in Tables 4 and 5.

The Na-1 reference design has a Doppler coefficient between 6×10^{-6} and 8×10^{-6} at 1400°K; at 2500°K this leads to values of 3.3 and 4.5×10^{-6} , respectively. What to use for the correct value of the Doppler coefficient and the initial flux constitute principal difficulties in calculating the energy release. With a value of $4 \cdot 10^{-6}$ for the Doppler the energy release corresponds to that from 85 kg of TNT for 100 \$/sec and $\Phi_0 = 10^{14}$ $n/cm^2\text{-sec}$, and at 500 \$/sec we have, for $\Phi_0 = 10^{10}$ $n/cm^2\text{-sec}$ an energy release corresponding to 539 kg TNT.

A major experimental effort is in preparation at Karlsruhe in order to investigate the equation of state experimentally.

8. The Design Basis Accident (DBA) and the Design of the Containment

About a year ago, when the Karlsruhe reference design, Na-1, neared its completion, the uncertainty of calculating the energy release in case of a MHA was larger than it appears today. So it was decided to accept in case of the Na-1 design the philosophy that the safety system does not fail. Reporting on the results of the third Geneva Conference, W. Häfele and P. Engelmann⁽¹⁵⁾ pointed out that this philosophy is accepted by many fast reactor groups. The mechanism of the plugging and superheating mechanism had not yet been detected, a fact which excluded the MHA as a DBA. The DBA is therefore a large sodium fire.

The steel container is designed to withstand a maximum pressure of 2.5 atm. But it is highly improbable that this pressure builds up because only a fraction of the sodium can burn, for all the compartments containing the primary circuit have a nitrogen atmosphere; only in the case of a major explosion, which is

excluded here, could all the sodium be burned. The diameter of the containment building (see Fig. 8) is only 28 m, fixed by the size of the primary circuit. The height is determined by the installation and maintenance or repair procedures for the components of the primary circuit. At the inside of the steel containment a layer of concrete protects against temperature of a fire and mechanical damages.

The Na-1 reference design thus approaches the two-containment solution of the SEFOR reactor. The DBA is therefore a nuclear explosion corresponding to that of 90 kg TNT. It will be contained in the compartments of the concrete structure of the primary loop. The second container, the steel shell in the case of SEFOR, shall withstand the pressure of an overall sodium fire. Therefore it will not be too difficult to extend the Na-1 reference design to a DBA of the SEFOR type. To exclude the MHA as a DBA is therefore a less severe assumption. In the forthcoming weeks an effort will be made to evaluate the capital cost potential of such change of philosophy. Thereafter the basis for the DBA of the Na-2 design will be selected.

9. *Capital Costs of the Na-1 Reference Design, Its Areas of Greatest Potential for a Reduction, and Its Competition with Breeding and Safety.*

One of the objectives of the Na-1 reference design was to get a rough idea of the costs and their distribution to use as a guide for further research and development.

The fuel costs are $0.3 \text{ DPf/kWh} = 0.75 \text{ mill/kWh}$ at a price for plutonium of $10 \text{ \$/g}$. This includes interest for inventory, as well as costs for refabrication, reprocessing, and the first core. It also includes the sale of bred material. The capital costs are $462.6 \times 10^6 \text{ DM} = \116.0×10^6 or $\$116/\text{kWe}$. The $462.6 \times 10^6 \text{ DM}$ are the sum of $262.6 \times 10^6 \text{ DM}$ for the conventional part = 58% of the total capital costs. $200.0 \times 10^6 \text{ DM}$ are assumed to be the costs for the reactor part, that is, 42% of the total sum. But the actual capital cost evaluation led to a value of only $116.96 \times 10^6 \text{ DM}$; $83.05 \times 10^6 \text{ DM}$ are considered to be a "contingency" due to general uncertainties in the area of costs for large components. If this "contingency" is excluded, the total sum is only $380.0 \times 10^6 \text{ DM} = \$95/\text{kWe}$ with 68% for the conventional part and only 32% for the reactor part.

The amount of $116.95 \times 10^6 \text{ DM} = \$29 \times 10^6 = 100\%$ will serve as the reference value for comparisons. $\$11 \times 10^6$, or 38%, is for the pumps; all the devices of the cooling circuits provide $\$19 \times 10^6$, or 65%, of the total costs of the reactor part. The reactor itself gives only $\$5 \times 10^6$, that is, 17%.

The reactor building should be in the order of $\$6 \times 10^6$.

A change from oxide to carbide fuel was considered.⁽³⁾ There are two ways to use the better properties of the carbides: It is possible to use the same rod power and to enjoy the higher breeding ratio, both internal and external, and the lower average temperature only, or to operate the reactor at higher rod power. Smidt's analysis indicates that in the second case, a higher cooling flux of sodium is required. This leads to higher pressure drops in the primary circuit and, therefore, to more expensive pumps and more structural material for the subassemblies, which in turn lowers the breeding ratio. The higher cost for the pumps nearly outbalances the gain in fuel-cycle costs if they are capitalized over a period of 15 years at 9.7%. Either way of use of the carbides instead of the oxides results in nearly the same cost reduction of about $20 \times 10^6 \text{ DM} = \5×10^6 .

The components of the coolant circuits are the area with the largest potential for possible cost reduction. In the Na-1 study we used cheap ferritic steel wherever it was possible. Up to now the cheap stainless steels of type 304 or 316 are forbidden for high-temperature use. If, however, their use were permitted, the thermal efficiency could be increased by perhaps 5%. This could lead to a decrease of 0.2 mill/kWh at an 80% load factor. One should realize here, that already a decrease of about 0.3 mill/kWh is the incentive for the light water superheat development. The penalty for the change from the ferritic to the cheap stainless steel would amount to only 0.03 mill/kWh. If, on the other hand, we continue to be forced to use a X8 Cr-Ni-Nb 16-13 type of stainless steel, gain and loss of costs may just cancel each other. The optimization of costs requires further attention.

Evaluation of the cost situation leads to the following conclusions:

a) The carbides have a cost advantage of over 5% when it is assumed that the fuel-cycle costs are capitalized over 15 years at 9.7%. This advantage is large enough to be interesting, but it is not so large that the Karlsruhe group would follow a parallel development of both oxides and carbides. The development of the carbide fuel is a meaningful second step. The use of carbide also gives higher breeding and, with the same rod power as used for the oxides, the advantage of a higher inherent safety. Cost advantage, safety, and breeding all tend in the same direction.

b) Among the more direct reactor costs, those components required by the use of sodium have the largest potential cost. In particular, the pumps are very expensive so that a well-targeted development program for pumps is advisable. Also, the questions of materials to be used in the circuits and components containing sodium are of interest, especially in terms of the background of present engineering regulatory rules in

FIG. 8. Vertical Section of the Reactor Building.

Germany. Serious consideration must be given to the use of cheap ferritic steel, particularly with respect to thermal efficiency.

c) The reactor building of the Na-1 design costs 6 % of the whole plant. A more complicated building of the two-container principle able to withstand explosion of, say, 100–200 kg TNT, is being further investigated. It has been estimated that its use would increase the cost from 6 to 7 or 8 % of the total. Therefore this does not result in a heavy competition between safety and economics.

d) The reactor itself involves 5 % of the cost. Therefore any complication which appears to be necessary for safety or breeding again does not penalize the economics.

Generally one can say that the often-cited competition between economy, safety, and breeding does exist, but is not too heavy. In the case of fuel *all* three aspects are *favourably* affected by the use of the carbides.

10. Conclusions

1) A 1000-MWe fast breeder reactor cooled by sodium appears technically feasible.

2) The safety properties of such a reactor do not constitute a major problem. Accidental reactivity insertions below \$0.25 can be accepted in the Na-1 reference design without the action of the safety system. More severe reactivity insertions require the use of a rather conventional safety system (about a 50-msec delay time before mechanical movement starts).

3) The Doppler coefficient allows for the use of such a rather conventional safety system and guarantees overall stability. The actual size does not have too large an effect on the design characteristics. If the safety system works, the size of the sodium void effect does not have a large feedback on the design.

4) Within the realm of a working safety system, the recently detected plugging and superheating accident of a central subassembly requires further attention.

5) If the safety system can fail, major accidents are possible. Then the size of the sodium void effect is of great importance. Its positive sign leads to an addition of the ramp rates due to sodium voiding and fuel compaction. Both, the nuclear excursion and a sodium-steam explosion superimpose. The Doppler coefficient decreases remarkably the release of kinetic energy.

6) Work should be done in the area of:

- a) the nuclear evaluation of the sodium void effect;
- b) two-phase flow of sodium;
- c) sodium-steam explosion and the mechanism of core destruction;
- d) a realistic equation of state for an oxide fuel.

7) The expected fuel-cycle cost for the Na-1 reference design is 0.7 mill/kWh. The capital costs are \$100/kWh, the breeding ratio is about 1.4; the doubling time (of a population of fast breeder) is about 7 years. This is very satisfactory.⁽¹⁶⁾

8) The choice of fuel is oxide. Carbide could lead to a decrease of the order of 5 % of the total costs (over 15 years). The development of a carbide fuel is therefore meaningful as a second fuel development. Carbide fuel cannot be used in a steam-cooled design.

9) The components of the cooling circuits make up nearly 65 % of the cost of the more direct reactor part, the pumps alone providing 38 %. Careful development of cheap, large components because of the use of sodium is in that sense important. An optimization of cost between sodium outlet temperature (that is, thermal efficiency) and capital costs requires further attention.

10) The principle of large fast breeder reactors is to have fuel cycle costs below 1 mill/kWh. Therefore, large-scale fuel testing to a burnup of 100,000 mWd/tonne as a problem of long testing times is of great importance.

APPENDIX

Data of the Karlsruhe Reference Design Na-1

1. General Data

Reactor Type	Heterogeneous Breeder	Fast
Reactor Power		
Thermal (MWt)	2,500	
Electrical (MWe)	1,000	
Fuel	UO ₂ -PuO ₂ at 87% of theoretical density	
Fertile Material	UO ₂ and U metal	
Coolant		
Primary	Na	
Secondary	Na	
Tertiary	H ₂ O	
Total Breeding Ratio	1.385	
Doubling Time ($\frac{1}{\lambda}$ out-of-pile inventory)—for a Population of Fast Breeders (yr)	7.2	

2. Reactor

2.1 Reactor Core

2.1.1 Construction and Composition

Form	Upright Cylinder
Total Diameter (mm)	3,766
Total Height (mm)	1,755
Fuel Region	
Diameter (mm)	2,860
Height (mm)	955
H/D Ratio	1/3
Thickness of Blanket:	
Radial (mm)	400
Axial (mm)	400

Total Number of Zones in Radial Direction (numbered from the Center outward):	4		
in Fuel Region	2		
in Radial Blanket Region	2		
Total Number of Cells:	397		
in Fuel Region	229		
in Radial Blanket Region	168		
Form of Cell	Hexagonal		
Width over Flats (mm)	178.5		
Overall Length of Subassembly (mm)	3,790		
Weight of Fuel Subassembly (kg)	315		
Weight of UO ₂ -Blanket Subassembly (kg)	427		
Weight of Metallic Uranium in Blanket Subassembly (kg)	587		
Thickness of Subassembly Cladding (mm)	4		
Diameter of Fuel Pin (mm)	6.7		
Diameter of Blanket Pin (mm)	11.3		
Cladding Thickness of Fuel Pin (mm)	0.35		
Volume Ratio of Cladding to Fuel	0.25		
Cladding Thickness of Blanket Pin (mm)	0.6		
Volume Ratio of Cladding to Fertile Material	0.25		
Cladding Material	Incoloy 800		
Number of Fuel Pins per Subassembly	331		
Number of Blanket Pins per Subassembly	169		
Fission Gas Plenum (below) (mm)	800		
Maximum Fission Gas Pressure in Fuel Pin (kg/cm ²)	70		
<i>Fuel Region</i>	<i>Zone 1</i>	<i>Zone 2</i>	
Outer Diameter (mm)	2,053	2,860	
Number of Cells	118	111	
Volume Fraction			
Coolant (v/o)	50.0	50.0	
Structural Material (v/o)	19.6	19.6	
Fissile Material (v/o)	3.2	4.1	
Fertile Material (v/o)	27.2	26.3	
Number of Fuel Subassemblies	105	105	
Number of Control Rod Subassemblies	12	6	
Fraction of Total Coolant per Fuel Cell	0.465	0.465	
Fraction of Structural Material per Fuel Cell	0.121	0.121	
<i>Radial Blanket Region</i>	<i>Zone 3</i>	<i>Zone 4</i>	
Outer Diameter (mm)	3,213	3,766	
Number of Cells	60	108	
Number of Blanket Subassemblies	60	108	
Volume Fraction			
Coolant (v/o)	29.8	29.8	
Structural Material (v/o)	21.9	21.9	
Fertile Material (v/o)	48.3	48.3	

2.1.2 Thermodynamic Data

Thermal Power			
Core (MWt)	2,410		
Blanket Axial (MWt)	48		
Blanket Radial (MWt)	42		
Coolant Inlet Temperature (°C)	430		
Max Outlet Temperature (°C)	630		
Avg Outlet Temperature (°C)	580		
Max Coolant Velocity (m/sec)	6.6		
Pressure Loss			
in Core (kg/cm ²)	1.25		
in Axial Blanket (kg/cm ²)	1.05		
in Fission Gas Plenum (kg/cm ²)	1.05		
Max Fuel Temperature (°C)	2,412		
Max Heat Flux (W/cm ²)	334		
Hot Channel Factors			
Coolant Heating	1.33		
Heat Transfer Coefficient	1.25		
Rod Power	1.24		
Hydraulic Diameter in Fuel Region (mm)	6.8		
Ratio of Avg to Max Power			
Radial	0.82		
Axial	0.80		
		<i>Zone 1</i>	<i>Zone 2</i>
Max Rod Power (W/cm)	566	539	
Ratio of Avg to Max Power			
Radial	0.897	0.745	
Axial	0.80	0.80	

Constants

Max Heat Transfer Coefficient between Cladding and Coolant (W/cm ² ·°C)	8.4
Thermal Conductivity of Cladding (at 600°C) (W/cm·°C)	0.21
Heat Transfer Coefficient between Fuel and Cladding (W/cm ² ·°C)	0.75
Thermal Conductivity of Fuel (W/cm·°C)	0.032

2.1.3 Physics Data

Critical Mass (Pu-239 + Pu-241) (kg)	2,015
Total Neutron Flux at Core Center (n/cm ² ·sec)	10 ¹⁶
Effective Neutron Lifetime (sec)	3.38·10 ⁻⁷
Effective Fraction of Delayed Neutrons	
(%)	0.3548
(\$)	1.0
Worth of a Fuel Subassembly at Core Center, Δk/k	2.49·10 ⁻³
(%)	0.7
Doppler Constant	
$A_{Dopp} = T[(1/k)(dk/dT)]$	-1.19·10 ⁻²
Doppler Coefficient, (1/k)(dk/dT) (°K ⁻¹)	-8.5 ·10 ⁻⁶
Greatest Possible Reactivity Worth due to Partial Loss of sodium, Δk/k	0.01108
(%)	3.06

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Safety and Economic Characteristics of a 1000-MWe Fast Sodium-cooled Reactor Design*

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the chain of improbable events required. A concept of reactor containment was then designed and an analysis made to prove its adequacy for even the hypothetical accident. Finally, the plant economics were determined, with emphasis on the effect of safety assumptions on the fuel-cycle costs. The possible effect on fuel-cycle costs of uncertainties in physics data and changes in limits of fuel performance were also determined.

2. SUMMARY OF SAFETY CHARACTERISTICS

2.1 Safety Criteria

Let us first discuss, for a moment, what safety criteria are and what use we shall make of them. Safety criteria are principally useful in scoping studies and are ultimately superseded by detailed hazard analyses. But they serve to define problem areas and accident conditions which must be considered. With this appreciation, then, the criteria are as given below.

Safety Criteria

1. A negative power coefficient of reactivity is required under all credible circumstances. In addition, the local power coefficients of reactivity must be negative everywhere in the core to ensure stability of the power distribution.

2. In case of sodium loss from core and axial blankets, the blankets shall contribute sufficient negative reactivity so that the overall effect on reactivity will be zero or negative.

3. Core-design provisions shall prevent radial propagation from one subassembly to another of local formations of coolant voids.

4. The integrated negative Doppler reactivity from operating power level to the point of fuel rupture is required to be large compared to credible outside sources of *rapid* reactivity insertion.

5. The performance of mechanical devices ultimately used to shut down the reactor shall be sufficiently rapid to avoid any damage, except minor local-

1. INTRODUCTION

It is the purpose of this paper to present a 1965 concept of a 1000-MWe mixed plutonium-uranium oxide-fueled reactor, devoting particular attention to its safety characteristics. The approach has been to update safety criteria and redefine operating limits for fuel, cladding, and coolant in the light of present knowledge. Within these bounds we have selected a preliminary reference reactor core, coolant system, and containment design based on previous parametric studies. Once the design was established, hazard analyses were performed to predict the dynamic response of the plant to credible plant accidents. The hazards analyses were extended to include the more severe accidents which were deemed hypothetical because of

* Work performed under Contract AT(04-3)-189, Project Agreement 10, between U. S. Atomic Energy Commission and General Electric Company.

ized fuel damage, following any credible combination of individual sources of reactivity addition.

6. The design of the reactor, including its control, coolant, and refueling systems, shall be such as to limit all possible sources of reactivity addition to a magnitude of the order of that required to bring the reactor to its hot operating condition.

7. The estimated maximum potential explosive release of energy due to sodium voiding or core meltdown and compaction will not exceed the equivalent of ~ 1000 lb TNT.

These criteria are essentially identical to those used previously in GEAP-4418,⁽¹⁾ with the exception of criterion number seven.

2.2 Significance of the Criteria

1. The existence of negative local power coefficients everywhere in the core assures stability of the power distribution as well as a net negative power coefficient under all credible circumstances.

2. For most cases, coolant loss will occur from the upper end of the subassemblies first. In general, then, with this criterion satisfied, coolant voiding will not produce consequential positive reactivity effects. Local coolant voiding could, in severe accidents, begin near the core midplane and produce a positive effect. This local condition would progress in a very short time over the entire subassembly (or subassemblies), and hence from a safety point of view, it is essential that the overall effect be small or negative. In effect, this criterion tends to limit actually expected voiding patterns way below the most reactive one.

3. The third criterion is designed for minimization of gross coolant voiding during severe power excursions or local flow blockage when propagation could compound the accident. In effect, this is an admonition to the core designer to design the channels properly.

4. Criterion four is aimed at limiting the effect of *rapid* reactivity insertions; it sets, for example, the maximum reactivity in control rods.

5. Criterion five sets requirements of inherent negative feedbacks as well as requirements for mechanisms and instruments.

6. Criterion six establishes the indispensable reactivity change so that it can be used as a yardstick for measuring reactivity inputs from other sources. The cold-to-hot reactivity change measures the strength of normal feedbacks. If the excess reactivity becomes very large, then insertion of a small fraction of it might overwhelm the normal reactivity feedbacks. For example, this criterion requires a limitation on excess reactivity in the core and hence may affect refueling times.

7. Criterion seven requires that reactor design limits

the sodium voiding effect and sets a lower limit on the Doppler effect. Based on Bethe-Tait analysis, the criterion will be satisfied by a negative Doppler effect, $T(dk/dT)$, no weaker than -0.004 with sodium out of the reactor.

Once we have established the safety criteria and selected a reactor in accordance, we must decide which reactor accidents we will analyze.

2.3 Classification of Accidents

2.3.1 Credible Accidents

... be greater than the foregoing by a factor of 10 (i.e., a credible accident) to set an outer bound on explosive energy that the reactor can produce. A reactivity excursion was coupled with failure to scram and loss of flow so that sodium loss and meltdown occurred.

We have continued to consider the dynamic characteristics of fast reactors after loss of sodium as important. We have designed this reactor for control of the maximum pattern for inserting total void reactivity. This implies a certain pessimism about the ability to forecast the pattern of voiding. This is certainly the case at this time and may be so even after we have completed the necessary experimental programs on two-phase flow, etc., in sodium. Further, once you contemplate core voiding, no matter what the reactivity coefficient, you are facing a meltdown situation, and we have philosophical difficulties about taking the former seriously without the latter. So we also consider meltdown. This philosophy has led to a

high-leakage core with addition of beryllia to improve the Doppler effect after loss of sodium.

We believe that loss of coolant, and hence core melt-down, can be made incredible and that eventually the designer will be released from these serious limitations. However, in the current work, we have tried to see how good a reactor can be designed even if you are forced to believe the unbelievable. This design is not a mere exercise because public acceptance of the first large fast reactors may well require designs which meet the consequence of the meltdown accident. In this context, then, we must strive to understand the implications of voiding in sodium-cooled reactors. The first reactor of this class will establish failure statistics of control drives and demonstrate independence of failure, thereby permitting subsequent assertion that failure to scram is impossible.

If you accept a loss-of-coolant accident, serious engineering attention to containment of a major explosion is necessary. In addition, problems of later re-assembly of the core and the long-term removal of decay heat add cost and other complications. We have adopted a double containment system of a low-leakage concrete vault and a steel sphere because at this time this system is the most predictable and reliable. You will recognize that as the SEFOR containment concept. Perhaps eventually a low-pressure building with filtering capability may replace the low-leakage steel sphere.

2.4 Design Implications

Once the safety criteria are established, the designer is next faced with passing judgment on present uncertainties in the basic physics information and methodology. We have attempted to select a model which, while not an extreme of pessimism, can be expected to improve with time. The physics model differs from that of the previous design (GEAP-4418) by using lower fission and capture cross sections for Pu-239 above 100 keV, higher capture cross sections in iron and nickel, higher elastic energy-transfer cross sections for oxygen, and higher inelastic energy-transfer cross sections for U-238. These result in a deterioration of the overall neutron economy.

We are aware that it is possible to achieve high-leakage cores by using geometries, compositions, and arrangements other than the flat pancake which we used. Some of the alternative arrangements seem to show somewhat better fuel-cycle costs. However, the differences are within the error of physics methods. We believe that the comparative simplicity of the physical and mechanical design, as well as the dynamic analyses, more than justified continuation of this approach. Further, it was useful to have a design

easily comparable with the previous one, so we could understand the trends caused by new safety criteria and new physics methods.

3. SUMMARY OF DESIGN

3.1 Design Criteria

The criteria used to select the core and coolant designs were developed with emphasis on safety and assurance of technical feasibility. The operating and transient design criteria mostly concern fuel performance.

3.1.1 Operating Limits

Fuel-operating limits have been selected to avoid axial fuel movement due to melting, and to prevent fuel-cladding mechanical interaction through control of the average fuel density (gap plus fuel pellet density).

1. The peak temperature limit requires that fresh fuel at the hot-spot-overpower condition shall be below the melting temperature.

2. The average fuel density has been selected so that differential fuel-cladding expansion is accommodated by an appropriate gap. At the fuel hot spot the differential expansion will require a minimum gap between the combination of the largest fuel-pellet diameter and the smallest cladding inside diameter. A statistical combination of these manufacturing tolerances is used to satisfy the design criteria.

In addition, a specified minimum value of the gap is required for fabrication. The combination of dimensional tolerances with the larger of either differential thermal expansion or the fabrication requirements determines the fuel-cladding gap. For the current design these criteria resulted in a smeared fuel density of 88%. With the average 94% dense fuel, the nominal diametral gap is 0.008 in. The 94% is set by one of the transient limits as well as by consideration of ease of reproducible fabricability.

3. Cladding operating limits were established at 1250°F, not including an overpower allowance. The value was selected on a conservative basis because of the apparent limit of 1300°F established by the Sodium Mass Transfer Program.⁽²⁾

We have adopted the vented-to-coolant concept for this reactor. In this fuel concept the limitations of cladding stress become secondary as long as fuel-cladding mechanical interactions are minimized. Cladding strain is limited to a one percent diametral change at end of life. Creep criteria are of secondary importance. The one percent allowance for cladding strain should take care of the statistically rare combination of extreme tolerances plus fuel swelling.

4. Corrosion must be limited to that amount which will leave a nominal thickness of 7 mils of unaffected cladding thickness at end of life. This is considered to be the minimum thickness capable of surviving the normal reactor environment. The corrosion allowance is based on the best available data at the hot-spot temperature (excluding overpower allowance), including a large safety factor.

5. Reactor sodium generally would be maintained at a plugging temperature near 250°F to minimize corrosion.

3.1.2 Transient Limits

These criteria are designed to guard against core damage as a result of credible accidents and abnormal operating conditions. They place further conditions on fuel design. They also serve to define the point beyond which core damage might be expected, a point of importance to hazard analyses.

1. The fuel transient limit has been selected so that no fuel can become completely molten, thereby putting a limit on the possible extent of fuel movement. The heat of fusion, which absorbs as much energy as a 1200°F temperature rise, is important during a transient because it results in a thermal-arrest period during which the peak fuel temperature does not change significantly. The criterion is based on the assumption, for which we have some evidence, that during short transients fuel which is below melting or molten only in the center maintains its position within the fuel pin.

2. The average fuel-pellet density has been specified at 94% to minimize fuel-cladding interaction during a transient. During a transient which causes the center-most fuel to reach the molten condition, the volume change due to melting is thereby taken up within the fuel and, theoretically at least, does not exert a force on the cladding. This criterion requires that the percentage of porosity in the as-fabricated fuel be equal to about half the volume change on melting and that porosity in the semi-molten fuel be free to accommodate the fuel expansion. This criterion applies specifically to fresh fuel at reactor startup, where fuel hasn't had time to redistribute radially.

3. The assumed transient failure point of the cladding and coolant has been selected conservatively at 1600°F. Cladding ultimate strength is down by a factor of three from normal and is decreasing rapidly at this temperature.

3.2 Reactor Design

The reactor is a 1000-MWe sodium-cooled, mixed plutonium-uranium oxide-fueled plant with a thermal rating of 2500 MW. Sodium at the reactor outlet tem-

perature of 1100°F is used to produce steam at 3500 psia and 1000°F. The tank-type style of reactor plant was maintained. The reactor coolant is circulated by four pumps and gives up its heat to secondary sodium coolant in six intermediate heat exchangers. The reactor is fueled with vented-to-coolant fuel which means that some 35% of the long-lived fission gases are normally in the argon cover gas.

3.2.1 Core Description

The core, blanket, and reflector arrangements are shown in Fig. 1. The core is a pancake 16 in. high with an equivalent diameter of 11.5 ft. Axial blankets, 18 in. thick, are placed above and below the core. Radially a moderating reflector, 7.3 in. thick, surrounds the core.

3.2.2 Nuclear Design

The core is divided into two radial zones of equal volumes. The same fissile enrichment is used in both zones, but the BeO content is varied from 10.5 v/o in the central zone to 3.6 v/o in the outer zone. The resulting power flattening is further enhanced by a BeO radial reflector which surrounds the core. A radial blanket outside the reflector was dropped as uneconomical.

The radial power profile is shown in Fig. 2. Because there is a higher volume fraction of fuel in the outer zone than in the central zone, the power density distribution (power/unit core volume) and the specific power distribution (power/unit fuel volume) are not the same. The midcycle radial peak to average power density is 1.22, with the peak at the inner edge of zone 2. The ratio of peak to average specific power is 1.23, with the peak at the center of the core. The zone 1 and zone 2 power split is about 55%–45% and changes only slightly during operation. The thin core with 18-in. blankets results in a very flat axial power distribution in the core region. The peak-to-average ratio is 1.11. The core-blanket power split at midcycle is 84%–16% and continuously shifts toward the blankets during operation.

A parametric study gave an early indication that larger fuel volume fractions than in our previous design were required to give acceptable neutron economy and that a core thinner than the previous 24-in.-high reference core was required to produce a net negative reactivity effect with sodium voiding of core and axial blanket. The thin 16-in. core was selected, not only because it results in a negative total sodium loss reactivity effect, but also because it yields a relatively low maximum reactivity change ($\$1.60 \Delta k$) due to voiding from the core center only. The spatial sodium voiding reactivity is shown in Fig. 3.

The BeO incorporated in the core softens the

FIG 1 Core, Blanket and Reflector Arrangement

FIG 2 Radial Power Distribution at Midcycle

FIG 3 Reactivity Effects of Partial Sodium Voiding

TABLE 1 COMPARISON WITH 1964 DESIGN

	GEAP-4418	Current Design	
Core Height	24 in	16 in.	
Core Diameter	11.65 ft	11.5 ft	
		Zone 1	Zone 2
% Fuel	25.4	34.8	42.4
% Sodium	47.9	36.5	35.9
% Steel	18.8	17.5	17.5
% BeO	7.5	10.5	3.6
% Control	0.40	0.7	0.6
Enrichment Supplied			
Zone 1	15.7	17.9	17.9
Zone 2	18.0		
Zone 3	20.2		

neutron spectrum sufficiently to provide a substantial (0.006) negative Doppler effect [$T(dk/dT)$] even with all sodium removed. The BeO, and particularly its concentration near the center of the reactor, also contributes to the low sodium loss effect mentioned previously. The high volume fraction of fuel results in reasonably good neutron economy and low inventory of fissile material; the breeding ratio is 1.18 with an internal conversion ratio of 0.63 in the core. At the midpoint of an equilibrium fuel cycle the average

fissile enrichment in the core is 15.3%. The total fissile inventory (Pu-239, Pu-241, U-235, and including fissile material in the blankets) at midcycle is 2470 kg, resulting in an average specific power of 1010 kW/kg fissile material.

3.2.2.1 Composition. Salient features of the core are given in Table 1 in comparison with the previous design.⁽¹⁾

3.2.2.2 Reactivity and Power Coefficients. The main reactivity coefficients are shown in Table 2 compared to the previous design.⁽¹⁾

The differences reflect core geometry and composition effects as well as different cross-section input and, in some instances, different calculational methods.

The overall power coefficients for the present design are given in Table 3. The coefficients given are for the whole core and blanket, and reflect the effect of relatively small changes in power level about the normal operating condition. The net local power coefficient is negative everywhere in the core and thus safety criterion §1 is satisfied.

3.2.2.3 Control. The reactivity control requirements are shown in Table 4. The control description is shown in Table 5. The plan view of the core shown in Fig. 4 shows the control layout. The system consists of 64 top-entry tantalum poison rods. Tantalum, clad in stainless steel, was selected because its cost over the life of

the plant appears to be about half that of B_4C and the rate of burnup is one-tenth that of B_4C . The higher thermal conductivity of tantalum, as well as its compatibility with stainless steel to 1500°F, were also important considerations.

The 3.7% Δk (\$10.6) worth of total swing was calculated for a six-month equilibrium fuel cycle at an 80% load factor. This reactivity change can be compensated by one-half of the control rods (shim rods). The remaining half of the control rods serve as scram rods and are withdrawn from the core during full power operation (maximum withdrawal rate $\sim 1\text{¢/sec}$). The control rods are cooled in the main coolant stream by one percent of the total coolant flow. The rods would be designed with a hold-down provision so that they could not become missiles or be forced from the core.

For accident analyses, a control rod insertion rate was calculated from a one-g drop from 4 in. above the core to 4 in. below the core. The rod displacement was found to be essentially a ramp with an effective delay of 0.2 sec with completion in 0.4 sec.

TABLE 2. CORE COMPARISON — REACTIVITY AND NUCLEAR PARAMETERS

	GEAP-4418	Current Design
Doppler Effect $T'(dk/dT)$		
Sodium In	-0.01	-0.0072
Sodium Out	-0.008	-0.006
Sodium Void		
Core and Axial Blanket	\$-0.2	\$-0.2
Core Only	\$+2.5	\$+1.1
Maximum	\$+4.25	\$+1.6
Percent Fission below 9 keV		
Zone 1	27	22
Zone 2	25	14
Zone 3	23	
Mean Fission Energy (keV)		
Zone 1	110	115
Zone 2	128	208
Zone 3	139	

TABLE 3. REACTIVITY POWER COEFFICIENTS
(At Operating Temperatures)

	$\Delta k/k$ per $\Delta P/P \times 10^3$
Prompt	
Doppler Effect	-4.35
Fuel Axial Expansion	-1.93
Delayed	
Sodium Density Reduction	+0.02
Steel Axial Expansion	+0.05
BeO Axial Expansion	+0.00
Core Radial Expansion	-1.44

TABLE 4. CONTROL REQUIREMENTS
($\$1 = 0.35\% \Delta k$)

Shim Control		
Burnup (Equilibrium Refueling Cycle)	\$10.6	3.7%
Scram Control		
Cold to Operating at "Zero" Power	\$1.4	0.5%
Zero to Full Power	2.3	0.8
Reserve Control for Safety and Shutdown Margin	6.9	2.4
Total	\$21.2	7.4%

TABLE 5. CONTROL DESCRIPTION

Control Material	Tantalum
Control Scram Worth	3.7% (\$10.6)
Control Rod Diameter	3.75 in.
Control Length	24 in.
Control Rod Drive Rate	20 in./min
Number of Scram Rods	32
Number of Burnup Control "Shim" Rods	32
Average Control Rod Worth	0.12% (\$0.34)
"Effective" Control System Delay Time	0.2 sec
Total Scram Time	0.4 sec

3.3. Fuel

3.3.1 Description of Fuel Pin

The objective of the design is to relieve the pressure buildup due to fission gas without permitting ingress of sodium into the fuel while meeting the fuel design criteria. A single fuel pin is shown in Fig. 5. The fuel consists of oxide pellets stacked in stainless steel cladding. The total length of the pin is about 6¼ ft including 1¼ ft of core, 3 ft of axial blanket, and 2 ft of vent. The fuel-pin description is given in Table 6. A core loading consists of about 121,000 fuel pins.

The BeO moderator is similarly contained in stainless steel cladding of the same size as the fuel. A core loading consists of about 22,000 BeO pins.

A summary of other fuel pin specification details is given in Table 7. These fuel and moderator pins are arranged in two core zones and in two types of sub-assemblies as follows:

Zone	No.	Kind	Fuel Pins	BeO Pins
1	34	Full Subassembly	420	126
	107	Corner Cutout	376	114
2	53	Full Subassembly	503	43
	88	Corner Cutout	452	38

The cladding material is to be fully annealed AISI type 316 stainless steel tubing with an ID of 0.245 ± 0.002 in. and a wall thickness of 0.010 ± 0.001 in.

The fuel design satisfied the design criteria in that

FIG. 4 Core Layout

FIG. 5 Fuel Pin

the density selected should prevent clad expansion during the credible accident transient. The fuel-cladding gap assures against fuel interaction due to thermal expansion. The worst case of differential fuel-cladding expansion is 3.6 mils. The gap required for fabrication was assumed as 3 mils, so expansion is the controlling

factor. Fission products-induced swelling is accommodated by the low smeared density and large gap allowed for other reasons.

The large tolerances specified are considered practical from the fabrication standpoint and do not restrict fuel performance.

The vented fuel design with a "diving bell" vent has been used as the reference cladding design. The vent allows continuous release of long-lived fission product gases while retaining the significant fission products and fission product daughters. This low-pressure design removes the fission gas pressure stress from the cladding. The fuel vent device is described in a separate paper by G. L. O'Neill *et al.* (see paper in Session VIA).

Thermal stress is also insignificant, amounting to only 14,000 psi for 10-mil cladding at the hot spot. Thus, the vented-fuel cladding design is determined by the requirement of a reasonable end of life thickness.

The hot-spot corrosion rate based on the Sodium Mass Transfer Program is about 1 mil during the life of the fuel. If an uncertainty factor and an allowance for the ferrite layer which forms are applied, an effective maximum loss of 3 mils of cladding is predicted. It was next assumed that about 7 mils is an adequate end-of-life thickness, so 10 mils was selected as the as-fabricated thickness.

3.3.2 Performance of Fuel Pin

Fuel performance is shown in Table 8 compared with the previous design.⁽¹⁾ The initial fissile loading is 2230 kg in core and 87 kg in blanket for a fissile specific power of 1079 kW/kg fissile supplied.

The hot-spot linear power was determined in accordance with the criterion that no fuel should be higher than the melting temperature even at 10% over-power. The fuel at the peak linear power of 24.2 kW/ft has a center temperature of 4928°F (the estimated melting point of the mixed oxide fuel). The ability of the fuel to operate under these conditions is dependent upon the formation of a central void in the hot-spot fuel. Because central voids are formed rapidly, low power operation for a few hours during startup with fresh fuel is required. This is normal practice and does not seem a handicap.

Credit for the center void is expressed as a geometric correction factor. In the current design a center void of nearly 56-mil diameter (assuming a gap conductivity of 1500 BTU/hr-ft²-°F) gives a 20% increase in allow-

TABLE 6. DESCRIPTION OF FUEL PIN

Fuel	UO ₂ -PuO ₂ Sintered Pellets
Enrichment (Initial)	17.9% fissile Pu/(Pu + U)
Fuel-pellet Diameter, Cold	0.237 in.
Cladding Material	AISI Type 316 SS
OD × Thickness	0.267 × 0.010 in.
Pin Length	77.25 in.
Fission Gas Vent Length	24.0 in.
Ratio of Pitch to Pin Diameter	1.13
Pin Spacers	Spiral wire wrap

TABLE 7. SUMMARY OF FUEL-CLAD SPECIFICATIONS

Fuel Composition	24.3% Pu; 17.88% Pu-241 + Pu-239
Stoichiometry — Oxygen/Metal	1.97–1.98
Theoretical Density at STP	11.09 gm/cc
Theoretical Density at Core Avg Temp of 2670°F	10.52 gm/cc
Fuel-pellet Density	94 ± 2% theoretical
Cold Smeared Density	9.75 ± 0.32 gm/cc; 87.9 ± 3.3% theoretical
Hot Smeared Density	9.39 ± 0.31 gm/cc; 89.3 ± 3.3% theoretical
Fuel Pellet OD	
Cold	0.237 ± 0.004 in.
Hot	0.241 ± 0.004 in.
Clad ID	
Cold	0.245 ± 0.002 in.
Hot	0.247 ± 0.002 in.
Fuel Clad Diametral Gap	
(As Fabricated)	8 ± 4.5 mils
(Core Avg Temp)	6 ± 4.5 mils
(Hot Spot Temp)	4.5 ± 4.5 mils

TABLE 8. PERFORMANCE OF FUEL PIN

	Present	GEAP-4418
Core Average Linear Power, kW/ft	12.4	10.4
Hot-spot Peak Linear Power, kW/ft	24.2	22.7
Core Average Specific Power, kW/kg Mixed Oxide	148.6	151
Fissile Specific Power, kW/kg Fissile Material	1079	1,000
Average Burnup, MWD/T	100,000	100,000
Hot-spot Fuel Center Temperature, °F	4930	4700
Core Average Fuel Temperature, °F	2670	2400

able linear power. This is possible when considering fuel densification alone and has often been observed experimentally.

Fuel conductivity was compiled from both out-of-pile and recent in-pile data. Data used was for 20% PuO₂-80% UO₂ which was assumed appropriate for the current mixed-oxide composition (24% Pu). The data used result in a total $\int_0^{t_c} kd\theta$ to melting of 94 W/cm.

Other fuel performance details are given in Table 9. Peaking factors used in the design are given in Table 10.

3.4 Subassembly

A core subassembly (see Table 11) is shown schematically in Fig. 6. The core is comprised of 282 hexagonal channels which provide axial and lateral

TABLE 9. FUEL PERFORMANCE

	Core (Average)	~ Nominal Peak	Hot Spot
kW/ft	12.4	20.0	24.2
ID Heat Flux (BTU/hr-ft ²)	659,000	1,056,000	1,280,000
OD Heat Flux (BTU/hr-ft ²)	610,500	977,000	1,180,000
T_{Na} (°F)	962	962	1,025
ΔT_{Na} Film (°F)	30	49	59
ΔT_{clad} (°F)	42	67	86
ΔT_{gap} (°F)	440	704	855
T_c (°F)	1,474	1,782	2,025
$\int_0^{T_s} kdT$ (W/cm)	35.0	37.5	41.3
Sintering Diameter $d_{1800^\circ F}$ (in.)	0.140	0.178	0.220
Void Diameter d_v (in.)	0.034	0.044	0.056
F , Center Void Correction Factor	0.92	0.89	0.83
$\int_{T_s}^{T_c} kdT$ (W/cm)	30.0	46.9	52.5
$\int_0^{T_c} kdT$ (W/cm)	65.0	84.4	93.8
T_c (°F)	3,866	4,595	4,928

TABLE 10. PEAKING FACTORS

Axial (Zone 1)	1.10
Radial (Beginning of Life)	1.28
Burnup	1.12
Overpower Allowance	1.10
Hot-spot Uncertainty	1.12
Total Hot Spot	1.38
Total	1.94
Estimated γ -Heat Out-of-Fuel	5%

The fuel pins are held axially by two end grids whose webs are spaced to retain the fuel and BeO pins. The two end grids are joined by a center post which easily accommodates the differential radial and axial thermal expansions between the fuel pins and the post. The fuel and BeO pins are spaced by a spiral wrap on every pin. To take up the tolerances and maintain support, as well as for channeling the coolant past the fuel. Nose pieces, attached to the lower end of these channels, locate and support the subassemblies by mounting into the lower grid. The channel wall is designed to withstand a 40-psi differential pressure and requires a wall thickness of 0.139 in. Slotted channels permit this design by equalizing the pressure differential above the upper blanket.

a tight pin array, the subassemblies are subdivided into six bound groups of fuel pins which are held tight within the channel by springs.

The fuel and shield sections of the fuel are designed so the shield section may be removed before shipment. The shield rods are 2.2 in. in diameter and 13.5 ft long.

3.5 Core and Structure

3.5.1 Thermal Hydraulics

Core thermal hydraulic values are summarized in Table 12. The coolant inlet temperature and temperature rise are 800°F and 300°F, respectively, giving a bulk outlet temperature of 1100°F, which is considered a practical upper limit at this time. These values are the same as in the previous design.

The temperature rise across the core was found with corrections for all forms of coolant dilution. In every subassembly the coolant flow is orificed to maintain the same maximum coolant exit temperature. The radial power gradient within the subassemblies contributes 8.4% dilution. Fuel burnup contributes 2.2% dilution. An estimated 1% of the coolant is required to cool control rods, and an estimated 5% was used to cool the radial reflector and thermal shield. The total dilution is about 17.7%, calculated as follows:

$$\text{Dilution} = \frac{1 + \text{power dilution}}{1 - \text{flow dilution}}.$$

The subassemblies are designed to reach a peak temperature rise of $300 \times 1.77 = 353^\circ\text{F}$. The hot-channel factor of 1.29 results in a maximum temperature rise of 456°F. The average core temperature rise is 319°F.

The required coolant flow is 94.5×10^6 lb/hr of which 94% goes through the core subassemblies. The total effective coolant flow area is 3760 sq in., giving a core average coolant velocity of 18.3 ft/sec.

A velocity peaking factor of 1.15, derived from the radial power distribution, results in a peak velocity of 21.0 ft/sec in the inner subassembly of the outer

TABLE 11. SUBASSEMBLY DESCRIPTION

Hexagonal Subassembly Width across Flats	7.718 in.
Channel Wall Thickness	0.139 in.
Interchannel Spacing	0.125 in.
Lateral Pin Restraint	spring
Axial Pin Support	end grids
Fuel Pins/Full Hexagon	
Zone 1	420
Zone 2	503
BeO Pins/Full Hexagon	
Zone 1	126
Zone 2	43
Nose-piece Diameter	4.0 in.

FIG. 6. Fuel Subassembly.

zone. These subassemblies are unorificed, thus setting the core pressure drop. The resultant pressure drop is 40.5 psi due to fuel pins, 16.5 psi due to the spiral wire wrap, and 6.4 psi due to entrance and exit losses. Pressure drop through the rest of the primary loop is 30.5 psi, giving a total pump head of 93.9 psi. Assuming a 75% efficient pump and motor, this requires 1.25% of the net plant output to drive the primary pumps.

3.5.2 Core Support, Holddown, and Surrounding Structure

The 282 core subassemblies, 63 reflector subassemblies, and radial neutron shielding are supported on the lower grid structure. This structure consists of two circular grid plates separated by 3 ft of internal grid work.

Concentric holes in both plates accept the circular nose pieces of the subassemblies. The weight of the subassemblies is supported at a conical seal face on the top grid plate. This grid structure is the coolant inlet plenum, coolant entering side holes in the nose pieces. Close fits of the nose pieces in the lower grid plate pro-

vides a hydraulic holddown, the area between the lower grid plate and the reactor vessel being opened to the primary tank.

The subassembly tightener grid is lowered over the top of the subassemblies and tightens the subassemblies laterally until the spacer pads at the core elevation are in contact. Cone-shaped guide-ins for all subassemblies and guide tubes lead to a simple cylindrical fit pulling the subassemblies together and aligning the control-rod guide tubes. Control-rod guide tubes protrude above this tightener grid, but the subassembly locations are provided with stops to prevent a subassembly from lifting out of the core.

Shielding is provided between the reactor vessel and the moderating reflector subassemblies to attenuate the neutron flux on the vessel wall and to provide thermal shielding between the outlet sodium and the primary tank sodium.

3.6 Reactor Tank and Coolant System

The main sodium tank holds some 560,000 gal of sodium. During normal operation the tank sodium will be at the reactor inlet temperature of 800°F. During refueling the temperature will be 350–400°F. The tank is a vessel one inch thick, 52 ft in diameter, and 47 ft high. The tank structure would be built to withstand about 10 psig.

The reactor core structure is contained in an inner reactor pressure vessel which, along with all other components of the primary coolant system, is submerged in a large tank of sodium as shown in Fig. 7. Four 56,000-gpm, vertically mounted, centrifugal sodium pumps and six, counterflow, 15,000-ft², intermediate heat exchangers provide the primary coolant system, and these are arranged in a circle around the

TABLE 12. THERMAL HYDRAULICS

Inlet Temperature	800°F
Reactor Outlet Temperature	1100°F
Orifice Design Temperature	1153°F
Hot-channel Exit Temperature	1256°F
Reactor-coolant Flow	94.49×10^6 lb/hr
Core Average Coolant Velocity	18.3 ft/sec
Core Peak Coolant Velocity	21.0 ft/sec
Estimated Worst Clad Corrosion	3.0 mils
Subassembly Pressure Drop	66.5 psi
Primary Loop Pressure Drop	96 psi

FIG 7 Vertical Cross Section through Containment Building

vessel. The pumps take suction from the large tank and deliver sodium into the vessel through check valves and 30-in pipes to an inlet plenum below the reactor. From the plenum the flow is up through the core past the neutron shielding to an outlet region, then through 30-in lines to the six heat exchangers.

This arrangement has been improved over that in the previous design by replacing the massive duct structure for the sodium with loops, and by supporting the reactor vessel and heat exchangers from the large tank wall instead of the top, which already must support the refueling cell. The intermediate heat exchangers, pumps, check valves, and reactor head are removable. Neutron shielding and any blast protection are also contained inside the tank. The blast shield surrounds the primary vessel, and stretch bolts would be used to hold the refueling "head" in place during any accident.

The sodium-purification units are also in the tank.

The cold traps, plugging indicators, and pumps would be inserted through shield plugs just like the pumps and heat exchangers. Figure 8 shows an arrangement around the central reactor vessel.

A safety vessel would be provided outside of the large sodium tank, thereby assuring against loss of sodium. The space between the two vessels could then be monitored continuously.

The safety vessel would be surrounded by a massive concrete structure which might possibly be lined to eliminate any chance of ground water collecting. A sump and removal system for water could also be provided.

If one admits to core disassembly and meltdown accidents, as is postulated for this paper, the reactor tank must be designed to withstand the impact resulting from such an accident, to prevent reassembly of the molten core, and to remove the decay heat.

As previously mentioned, capability of pressure con-

FIG. 8 Plan View of Reactor Tank.

tainment, a blast shield, and head holddown can be incorporated into the design. Further, core meltdown pans are shown which are protected by a blast mat.

The accident which destroys the core probably must be assumed to destroy the heat exchangers, so the continuous removal of decay heat could be a problem. It is proposed that until such design or analyses prove otherwise, a separate, blast-protected heat exchanger can be provided for removal of decay heat.

4. SAFETY ANALYSIS

4.1 General Considerations

This section describes the dynamic behavior of the reactor core under accident conditions, and evaluates the effectiveness of the control system and overall reactor design in meeting the safety criteria specified in Section 2.1 under accident conditions previously discussed in Section 2.3.

Response of the reactor to credible accidents was analyzed by use of FORE,⁽³⁾ a digital computer code. The maximum hypothetical accidents were analyzed by means of a modified version⁽⁴⁾ of the Bethe-Tait

approach for analyzing core disassembly. Initial conditions for the disassembly calculations were obtained from FORE. Because of the difference in the Zone 1 and 2 compositions, a core-averaged composition was used to describe the core.

All calculated values of reactivity coefficients were conservatively modified by a factor of safety of 1.5 to account for uncertainties in reactivity coefficients. For credible accidents, a sodium-in Doppler effect of -0.0048 was used. In the case of the maximum hypothetical accidents, a sodium-out Doppler effect $[T(dk/dT)]$ of -0.004 , as well as the calculated value -0.006 , was used. The normally prompt, negative coefficient from axial fuel expansion was assumed to be zero, more for consistency with past practice than from conviction. Other reactivity coefficients employed in the analyses were those from the radial and material expansions.

4.2 Credible Accidents

4.2.1 Control Rod Ejection

Between the time at which positive reactivity is inserted and control rods begin to move, the reactor

behavior depends primarily on the Doppler effect to limit the severity of the accident. The full-power operating conditions correspond to the lowest Doppler feedback in normal operation because the Doppler effect is approximately inversely proportional to the absolute fuel temperature. Also, energy required for core failure is normally a minimum at full core power. Thus, the core is most vulnerable to reactivity insertions at full power. The ejection of the most reactive control rod was assumed at full power. Worth of the most reactive control rod is 60 cents. An instantaneous ejection of this rod was assumed.

The resulting maximum temperature rise during this extreme transient was less than one-fourth of the transient equivalent temperature rise of 1200°F for which the fuel has been designed. Only a negligible rise in cladding and coolant temperature occurred.

4.2.2 Loss of Pumping Power

Coolant coast-down resulting from an instantaneous loss of pumping power has been considered. A flow coast-down model was used, taking into consideration the inertia of the pump motors (without flywheels) and natural convective circulation of the sodium. It was assumed that the secondary system maintained the core inlet temperature at 800°F. Scram was assumed to be initiated when pump power was lost.

Fuel temperatures were found to decrease almost immediately. The maximum rise of coolant temperature during the entire coast-down period was less than 300°F, leaving the hot-spot coolant temperature below the design maximum of 1600°F.

4.2.3 Local Flow Blockage

Blockage of a single subassembly by debris or gas entrainment was also analyzed as a credible accident. Damage to the subassembly itself was not of major importance; rather, the sodium voiding effects and eventual local fuel slumping were of principal concern.

Both the voiding and slumping can produce positive reactivity additions. Accompanying the voiding can be a relatively large pressure pulse which has the potential of radial propagation unless design provisions are made against it.

Core calculations have shown that the combination of the most extreme voiding pattern and the complete melting and slumping of fuel from the upper half into the lower half of the core in one of the central subassemblies results in less than 40 cents of reactivity. Since this reactivity, and obviously the reactivity insertion rate, is less than that of the control rod ejection just discussed, this accident will be of no consequence to the rest of the core with respect to reactivity addition when followed by a normal scram.

There is a need for development of an adequate method for prevention of, or detection of, a local channel blockage.

Pressure within the channel due to the formation of sodium vapor was estimated to be of the order of 300 psi, based on sodium vapor pressure at the maximum temperature of the coolant. Extreme deformation of the hexagonal channel into a cylinder of equivalent volume and then ultimate rupture of this shape requires an innerchannel pressure of approximately 750 psi, if the channel material is near the boiling point of sodium. This, of course, assumes that the adjacent channels offer no restraint. Complete circular deformation of the channel would cause a maximum interference of approximately one-fourth inch with an adjacent channel. This interference would be localized and probably accommodated by local deformation in an adjacent channel without seriously disrupting coolant flow.

When this is considered as well as the fact that pressures of the order of 750 psi would be required to cause such a phenomenon, radial propagation of a local blockage would not occur.

4.2.4 Fuel-assembly Drop-in during Refueling

The refueling accident was analyzed in terms of a series of pessimistic assumptions, e.g., a fuel assembly falling into the core under conditions where the reactor is just subcritical due to a combination of previous errors. This would require overenrichment of many subassemblies, combined with failure of the instruments to detect the condition. The model for reactivity addition during refueling was taken as an outer-zone subassembly inserted into the center of the core. The rate of insertion was taken as that due to free fall starting 20 ft above the top of the core. The decelerating effect of the sodium was ignored. This resulted in a positive reactivity insertion of \$1.70 at approximately \$46/sec. It was assumed that the power is 10^{-9} times rated, and that the reactor coolant was at 400°F.

This accident was analyzed under conditions of shutdown coolant flows of 10 and 15% of full flow and no provisions for mechanical shutdown. In the former case, core power began to level out near 850 MW, but the maximum temperature of the coolant in the hot channel exceeded the 1600°F criterion in 4.8 sec. With 15% full flow, core power approaches 1170 MW with the main coolant temperature reaching 1600°F in approximately 10 sec. Fuel temperatures in both cases were well below the design maxima.

These results indicate that the core could survive this severe accident if some reserve control remained. Also, it is important to note that the rate of coolant flow has a significant effect on the time scale, that is, the time required to reach coolant temperature limits

is lengthened when the flow is increased. This allows more time for the response of reserve control.

4.3 Hypothetical Accidents

As previously discussed, accidents in which the control system does not function are considered hypothetical. In such accidents the core will be destroyed, and the question of primary interest is the containability of the accompanying energy release.

Two types of accidents starting at full reactor power were considered: a rapid reactivity insertion leading to sodium voiding at full coolant flow, and an abrupt loss of flow followed by gross fuel melting and core compaction. (In the tank reactor model selected, a guillotine-type pipe break will *not* cause this accident.) In both accidents it was assumed scram did not occur, although there is a considerable difference in the two cases between the time the accident begins and disassembly occurs. It is conceivable that the accident due to rapid reactivity insertion occurs at times of the order of the delay time of the scram system, whereas the abrupt loss-of-flow accident requires several seconds or from ten to twenty times the scram time. In addition to the assumption of no scram, it was assumed that only Doppler and disassembly feedbacks are present.

In the case of the rapid reactivity insertion, reactivity was inserted at the rate of \$2/sec until excessive fuel vapor pressure was generated in the central channel, causing rupture of the fuel cladding. At this point it was postulated that the sodium was expelled from the core in the worst possible manner with an expulsion time of 0.1 sec, thereby producing a ramp insertion of \$16/sec. This caused a prompt-critical excursion which was terminated by disassembly. A center fuel temperature of 6500°F was chosen as the point at which vapor generation was sufficient to breach the cladding. The time required to reach this temperature was of the order of one second. Either flow blockage of several channels or ejection of several control rods must occur to produce a \$2/sec ramp over this time span.

For the abrupt loss-of-flow case it was assumed that either the most reactive control rod was ejected or local flow blockage occurred in a central channel, producing local sodium voiding and fuel compaction. After reaching the corresponding overpower condition, coolant flow was stopped, which initiates sodium voiding, and progressive radial melting and compaction of the fuel. It was assumed that as the fuel melts it compacts to 100% theoretical density under the influence of gravity.

Energy-release calculations were made with a modified version of the Bethe-Tait approach for

FIG. 9. Maximum Energy Available for Work vs Doppler Effect.

analyzing core disassembly. Energy distribution, precursor concentrations, core power, and reactivity obtained from FORE were used for initial conditions in the disassembly calculations. The equation of state for the fuel employed in the analysis was that of Meyer and Wolfe.⁽⁵⁾

The resulting energies available for work are shown in Fig. 9 for the two models as a function of Doppler effect. Figures 10 and 11 show the reactivity during these accidents.

In summary, the worst of these accidents (the loss of flow) produces 1030 and 640 MW-sec of available work, assuming isentropic expansions for $-[T(dk/dT)]$ of 0.004 and 0.006, respectively. If the results of the recent KIWI test⁽⁶⁾ are extrapolated for these core conditions, the available work is approximately 170 and 106 MW-sec, respectively.

4.4 Containment

A double-containment system has been provided in the current design because this system is the most predictable and reliable at this time. The containment features are shown in Figure 12.

FIG. 10. Reactivity during Sodium Voiding Accident.

The outer container is a steel containment vessel which will contain 20 psig. The leakage rate for the sphere was assumed to be one percent of the contained volume over a 24-hr period at 20 psig.

The inner container is the primary sodium tank. As previously stated, the tank is equipped with a safety vessel and placed in a massive concrete structure. The roof of the tank and all penetrations would be designed to withstand about 10–15 psig positive internal pressure and would normally be held at 0 to 2 psig. During refueling the pressure would be varied ± 5 psi.

Because oxygen inleakage, and fission product and plutonium outleakage must be controlled, all floor penetrations and moving shafts would be equipped with seals designed for very low leakage at normal pressures of the cover gas. In order to test the adequacy of this design, these seals were assumed to leak 20 v/o of the cover gas per day at a differential pressure of 10 psig. Containment of the maximum hypothetical accidents discussed in this paper was then considered and the radiological implications considered.

The large tank of sodium is considered capable of condensing the vapor ball resulting from the hypothetical burst before a significant contribution is made to the cover gas pressure by the vapor pressure of any core materials. One could design the system so that any such vapor ball would be expelled downward into the sodium tank. A transient pressure would be developed by a short-lived displacement of sodium, then a general heating of the vapor space due to the presence of high-temperature sodium. The total energy release in the accident would generate only a few degrees increase (averaged) in the tank (1688 MW-sec/ $^{\circ}$ F rise). However, the decay heat can bring the tank contents to boiling in a few hours without cooling provisions.

The vapor space pressure (including the gas space in the interconnected gas surge tank), which developed as a result of increase of tank sodium temperature, was calculated on the basis that a 50-MW emergency cooler was used. The pressure resulting from the temperature pulse was used as input to a double-containment leakage program and the leakage rate computed.

The results if a 50-MW emergency cooler is used indicate a peak differential pressure of 9 psi between

FIG. 11. Reactivity during Loss-of-Flow Accident.

FIG. 12. Reactor Containment.

the inner and outer containments. The differential pressure between the outer containment sphere and a constant atmosphere increased to 2 in. of water after 10 hr, then fell to zero in about a week.

Plutonium and strontium leakage from this incident was calculated by assuming that a plutonium aerosol was present in the reactor vapor space. The plutonium concentration of the aerosol was taken as 0.264 gm/m³ of argon based on a specific gravity of about 8. The resulting total leakage of plutonium to the atmosphere was 0.1 gm over the week, assuming complete mixing in the outer containment. Strontium was assumed, conservatively, to escape in the same ratio as it exists with plutonium in the core (at 100,000 MWD/T). Some 0.001 gm of strontium would escape.

The release of about 0.1 gm of plutonium and 0.001 gm strontium does not represent a serious radiological hazard in the context of a maximum accident result. Furthermore, the aerosol would settle and deposit on surfaces before it is released. In any event, calculations

at a typical reactor site show these values to be at least one order of magnitude below acceptable accident limits.

Protection against sodium fires is provided by this design. Because the tank itself is designed to contain the hypothetical accident, the sodium is expected to be covered with an inert atmosphere throughout any incident. Small fires resulting from sodium escape from failed seals, etc., would be easily contained without pressurization of the outer containment, which would withstand approximately 20 psig in any event.

5. ECONOMICS

5.1 Fuel-cycle Costs

The fuel-cycle data important to cost calculations are given in Table 13. The refueling scheme is based on a six-month refueling cycle at 80% load factor. Note that the six-month refueling cycle is a limit imposed by safety criterion 6.

TABLE 13. FUEL-CYCLE DATA

Six-month Refueling Cycle, 80% Load Factor Four-batch Loading in Zone 1, 5-batch in Zone 2 Average Fuel Burnup	
Zone 1	115,700 MWD/T
Zone 2	105,500 MWD/T
Fissile Inventory at Midcycle	2470 kg fissile
Core Fissile Enrichment at Midcycle	15.3%
Total Breeding Ratio	1.18
Core Internal Conversion Ratio	0.63
Doubling Time at 30% Out of Reactor Inventory	13.4 yr

TABLE 14. FUEL-CYCLE COST

	Mills/kW-hr
Fabrication	0.343
Inventory	0.425
Depletion	-0.197
Recovery	0.200
First-core Capitalization	0.101
Total	0.872

The initial fissile enrichment is 17.9%. Fresh blanket material is depleted uranium of 0.3% U-235. The isotopic composition of the plutonium supplied to the core is calculated on the assumption that plutonium discharged from the core and blankets is mixed uniformly for recycling. The batch-refueling schemes yield nearly equal average burnup in the two zones with an overall average of about 110,000 MWD/MT.

Fuel-cycle costs based on 10% inventory charges, \$10/gm plutonium, shipping half-way across the USA, and \$17,000/day reprocessing charges per the AEC "standard" plant are given in Table 14.

5.2 The Effect of Uncertainties in Nuclear Data

The configuration and other important characteristics of a fast reactor core design are strongly influenced by the nuclear parameters of the fuel and other core materials. It is important therefore to have some assessment of the effect of uncertainties in the basic nuclear data.

An extensive survey was carried out to identify those uncertainties in nuclear data which were expected to be of significance from the point of view of fast reactor core design (see paper by Greebler *et al.*, in Session I). In this presentation, the discussion will be restricted to the effects of only those changes in nuclear parameters which appear to be of greatest importance, and for which strong experimental evidence exists.

The nuclear design calculations for this 1000-MWe

Fast Ceramic Reactor used a conservative set of nuclear data. There is experimental evidence to suggest that calculations using these data predict:

1. a neutron spectrum which is too hard (quantitative information is given in the paper by Greebler *et al.*);
2. a positive Doppler contribution from Pu-239 which is too large.

The significance of these predictions is shown in Table 15. If the nuclear data are adjusted in line with this experimental information, the design calculations show that at least 50% of the BeO, which was included in the core to soften the neutron spectrum and to increase the Doppler coefficient, can be removed and still meet the Doppler and sodium-loss criteria of the reference core. The fissile inventory required decreases by about 3%, and the breeding ratio increases from 1.18 to about 1.25. This results in a decrease in the fuel-cycle cost of about 0.1 mill/kW-hr. If one assumes a favorable combination of nuclear data within the limits of uncertainties reported by Greebler, and furthermore, if the safety criteria are relaxed to allow a calculated Doppler effect $[T(dk/dT)]$ of -0.004 (with sodium out), and a positive total sodium loss effect between \$1 and \$2, all of the BeO can be removed from the core. This leads to a 10% decrease in fissile inventory, a breeding ratio around 1.4, and a fuel-cycle cost of 0.3 mill/kW-hr less than that calculated for the reference core.

5.3 Effect of Uncertainties on Current Fuel Performance

Since construction of a prototype fast oxide-fueled reactor has begun in the USSR, and since immediate commitment of a prototype is under serious consideration in the UK, we thought it would be a useful exercise to design a 1000-MWe reactor as we would if we had to commit ourselves tomorrow morning. In this event we would have to design with additional safety factors to cover the inadequacies of our present information on fuel properties. We therefore assumed:

- a. a 10% decrease in thermal conductivity of the fuel to allow for possible later unfavorable data;
- b. a reduction in the fuel melting point of 250°F, based on the possibility of noncongruent melting of mixed oxide;
- c. an increase in overpower allowance from 110% to 120%, to give added weight to errors in instrumentation;
- d. no credit for central void formation in fuel;
- e. an uncertainty factor of 1.2 for gap and sodium film coefficients.

These assumptions result in the core described in Table 16 when the core specific power is maintained approxi-

TABLE 15. EFFECT OF UNCERTAINTIES IN NUCLEAR DATA

	Sodium-out Doppler	Total Loss of Sodium	Percent Fissile	Breeding Ratio	Fuel-cycle Cost or Cost Improvement
Reference	-0.006	\$-0.20	15.3	1.18	0.872 mill/kW-hr
Favorable Data, Remove 50% BeO	-0.006	0 or slightly +	15.0	1.25	-0.1 mill/kW-hr
Highly Favorable Data, Remove All BeO	-0.004	\$+1.60	13.7	1.40	-0.3 mill/kW-hr

TABLE 16. CONSERVATIVE CORE COMPARISON

	Inches		
Fuel Pin			
Tube OD	0.210	(0.267)	
Tube Wall Thickness	0.010	(0.010)	
Pin Length	77	(77)	
Pellet OD (Cold)	0.181	(0.237)	
Vent Length	24	(24)	
Subassembly			
Hexagonal across Flats	7.7	(7.7)	
Wall Thickness	0.17	(0.139)	

	Zone 1	Zone 2	Total
Core Totals			
Fuel Pins	99,124	112,246	204,370
BeO Pins	27,684	9,272	36,956
Total	119,808	121,518	241,326

TABLE 17. "CONSERVATIVE CURRENT" FUEL PERFORMANCE

Core Average Specific Power	7.5 kW/ft (12.4)
Hot-spot Peak Linear Power	15.2 kW/ft (24.2)
Core Average Specific Power	151 kW/kg oxide (149)
Fissile Specific Power	1076 kW/kg fissile U + Pu (1076)
Hot-spot Fuel Center Temperature	4670°F (4928)
Core Average Fuel Temperature	2037°F (2670)
Average Burnup	100,000 MWD/T (100,000)

mately constant. Values in parentheses are for the core discussed previously.

Fuel-pin performance was changed by these assumptions to that described in Table 17. By use of the same costing assumptions as before, the fuel-cycle costs for this core were calculated (see Table 18). These are a conservative limit on expected costs.

The incentive for the oxide-fueled fast reactor for immediate commitment is still considerable, even if less than we expect to attain at the conclusion of investigations presently underway. The attainment of high burnup remains an important assumption. Information supporting the 100,000-MWD/T figure used

TABLE 18. "CONSERVATIVE CURRENT" FUEL-CYCLE COSTS

	Mills/kW-hr
Fabrication	0.421 (0.343)
Inventory	0.431 (0.425)
Depletion	-0.175 (-0.197)
Recovery	0.2 (0.2)
First Core Capitalization	0.125 (0.101)
Total	1.002 (0.872)

Again, the values in parentheses are those given previously.

TABLE 19. CONSTRUCTION COSTS FOR 1000-MW FAST OXIDE REACTOR
(thousands of dollars)

Item	
Land	\$ 220
Site Improvements	3,990
Controls and Instrumentation	2,970
Turbine-Generator Building	8,200
Turbine-Generator Unit	27,000
Condenser for Circulating-water System	7,500
Accessory Electrical Equipment	8,750
Feedwater System and Piping	11,800
Subtotal "Non-nuclear"	\$ 70,430
Reactor Equipment	16,100
Cover-gas System	850
Primary Sodium Pumps and Drives	3,280
Intermediate Heat Exchangers	4,150
Secondary Sodium Pumps and Drives	2,740
Secondary Sodium Piping and Valves	610
Steam Generators	19,800
Subtotal "Nuclear Steam Supply"	\$ 47,530
Reactor Building	\$ 7,100
Total Plant Cost	\$125,060

in the calculation is meager, and possibly a conservative present design would also back off to 50,000 or 75,000 MWD/T.

5.4 Capital and Total Power Costs

Capital and operating costs were not revised in this work. A similar plant was estimated in 1964 to cost

\$125/kW, which is equivalent to 2.5 mills/kW-hr at 80% plant factor and 14% per annum. These costs included all construction, overhead, and direct costs, as well as contingency and interest during construction. The estimate is repeated in Table 19 for completeness. Operating and maintenance costs were also estimated previously at an annual cost of \$2,157,000, which amounts to 0.34 mill/kW-hr at 80% plant operating factor.

Thus, total power costs for this reactor are:

	mills/kW-hr
Capital	2.5
Operating	0.34
Fuel Cycle	0.87
	—
Total	3.71

Thus, the incentive to develop this reactor concept re-

mains strong and essentially unchanged from the previous evaluation.

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Conceptual Design and Preliminary Accident Analysis of a Sodium-cooled, Carbide-fueled, Large Modular Fast Reactor

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I. Introduction

This paper presents the conceptual design and some of the safety characteristics of a large, modular, sodium-cooled fast breeder reactor power plant. The reactor concept is essentially the same as that developed in reference (1) for the USAEC. Certain aspects of that design have been revised to produce a higher-power-density reactor⁽⁶⁾ which is more nearly consistent with private ownership of fuel and commercial utility practice.

One of the advantages of the modular concept is that by the addition of modules the thermal power rating can be extended, in units of a module, to any desired level. This flexibility in the concept removes all apparent size restrictions confronting fast reactor cores. The modular concept can easily be prototyped through the use of a single module for a demonstration plant. A second advantage is that the safety coefficients, particularly the sodium temperature and voiding, can be adjusted to any desired values through changing the dimensions, height, and diameter of the individual modules to obtain the necessary neutron leakage. In addition, the neutron coupling between modules can be made, within limits, to be any desired value through the freedom existing in adjusting the radial blanket thickness between modules.

The CEX (neutron-controlled expansion) fuel assemblies used in reference (1) were omitted from the core design as this supplementary axial-expansion safety device is not yet developed. A sodium-bonded carbide-fueled core may have a predictable expansion characteristic as the carbide fuels have physical properties, thermal conductivity, and expansion coefficients quite similar in values to those of metals.

To establish the expansion characteristics of carbide fuels, a study of the behavior of these fuels under irradiation, up to 100,000-MWD/T burnup, is required. If carbide fuels are shown not to have predictable expansion characteristics, then the stack of active fuel pellets can be divided among compartments and the fuel will move with the cladding. The

clad is stainless steel whose expansion behavior is well known and should provide sodium-bonded carbide-fueled cores with a dependable axial-expansion characteristic during transient conditions. The sodium bonding increases the fuel-clad conductance sufficiently to give a time constant less than 0.5 sec for the heat being generated in the fuel to reach the clad. The clad thus has a response fast enough to be a major contributor in terminating a power excursion. Thus in any case adding the CEX safety device would improve the axial-expansion behavior of the core.

The sodium outlet temperature of the reactor has been lowered from 1200°F in reference (1) to 1100°F. The 1100°F level is sufficiently high to achieve steam temperatures in the range from 950 to 1000°F. This reduction in sodium outlet temperature permits a reduction in clad temperature while simultaneously increasing the reactor temperature rise and the total power. The increase in total power with the same fuel assembly reduced the inventory of fuel per unit of output. The lower temperature level reduces the requirements of the IHX and steam-generator materials. Concurrently, the pump size is reduced, the cost of heat exchanger apparatus decreased, and the reliability of the fuel clad is increased.

The number of control rods has been increased to reduce the hazard of a multiple accident culminating in control-rod expulsion, and to permit refueling on approximately an annual basis (instead of semi-annually) to improve the commercial acceptability of the plant.

Potential accidents to this reactor have been reviewed, and five events or chain of events have been chosen as representative of the more severe disturbances to which the system could be subjected. The response of the reactor to these disturbances has been determined by means of digital power-excursion computer programs. The analysis of these results indicates that the behavior of the reactor should not imperil the public or the operating staff, nor even result in economically severe damage to the plant. The

most severe accident analyzed is a flow blockage to one fuel assembly. This is severe because of the difficulty in detecting such a small perturbation in the coolant flow until severe damage has been done to that one assembly. Even this case would not result in damage to the remainder of the core unless a large fraction of the safety rods refused to move in response to reactor over-power, over-temperature, and neutron-level and neutron-period scram signals, a very improbable event.

II. Reactor Description

The reactor is a revision of the conceptual design developed in reference (1) for the USAEC. It consists of seven distinct regions or modules separated by a graphite reflecting region and coupled by the neutron flux. Each module contains a core region approximately 34 in. in diameter and 72 in. tall. Surrounding the cores are fertile, axial, and radial blankets. The core layout is illustrated in Fig. 1.

The thermal design of the core has been revised to increase the power density in order more nearly to optimize the fuel-cycle cost for private ownership of fuel, 10% annual charge, and commercial acceptability. In this revised design,⁽⁶⁾ the sodium outlet temperature is reduced from the 1200°F in reference (1) to 1100°F. The increase in power density raises the combined thermal power rating of the seven modules from 2500 MW(t)⁽¹⁾ to 3440 MW(t), approximately 500 MW(t)

per module. The electrical output rating becomes 1375 MW(e), assuming a 40% plant efficiency.

The reduction in coolant outlet temperature, while permitting a 30% increase in the specific power density, kW/kg of fissile material, allows a reduction in the clad surface hot-spot temperature from 1400°F to 1300°F, with a simultaneous increase in the reactor coolant temperature rise from 220°F to 250°F. The fuel-rod average and maximum linear powers are 15.6 kW/ft and 38 kW/ft, respectively. The core average fuel temperature is approximately 1400°F whereas the maximum fuel centerline temperature is 2430°F. The thermal calculations are based upon a fuel thermal conductivity at operating conditions of 10.5 BTU/hr-ft-°F. The core pressure drop is approximately 100 psi. Table 1 summarizes the thermal and hydraulic characteristics of the core.

Each core consists of 37 fuel assembly positions; one (the center position) is occupied by a safety rod. Each of the 36 hexagonal fuel assemblies contains 127 fuel rods, of 0.300-in. OD, on a triangular pitch with 0.426-in. centers. The cladding is stainless steel, 10 mils thick. The fuel material is 90% dense, stabilized plutonium-uranium carbide. The fuel pellets are sodium bonded to the clad. The fuel rods are vented to the sodium coolant. The numerical data for the mechanical design are tabulated in Table 2. A more detailed description of the reactor mechanical design is given in Appendix A.

FIG. 1. One-quarter Reactor Core Section.

TABLE 1. REACTOR THERMAL DESIGN DATA

<i>Power</i>	
Total Thermal Output, MW(t)	3440
Estimated Electrical Output, MW(e)	1376
Thermal Output of Core per Module, MW(t)	417*
Thermal Output of Axial Blanket per Module, MW	11*
Thermal Output of Radial Blanket per Module, MW	64*
Total Flow rate, 10 ⁶ lb/hr	20.7
Bypass Flow, Estimated at 10%	2.1
Power Density, kW/liter of Core	390
Specific Power, kW/kg of Core Metal	117.8
Average Linear Power, kW/ft	15.6
Maximum Linear Power, kW/ft	37.8
<i>Heat Transfer</i>	
Total Heat Transfer Area, ft ²	14,210
Average Heat Flux, Btu/hr-ft ²	678,000
Maximum Heat Flux, Btu/hr-ft ²	1,640,000
Total Flow Area, ft ²	20.58
Average Coolant Velocity, ft/sec	30.3
Average Mass Velocity, 10 ⁶ lb/ft ² -hr	5.58
<i>Temperatures, °F at Rated Power</i>	
Reactor Inlet	850
Reactor Outlet	1100
Average Channel Rise (10% Bypass)	275
Average Channel Outlet	1125
Hot Channel Outlet	1293
Maximum Clad (Outside)	1326
Maximum Fuel Center	2430

* Time-averaged values (estimated).

The nuclear reactivity coefficients were calculated by means of improved methods developed since the issuance of reference (1). The new sodium-coefficient calculations are based on two-dimensional studies. The Doppler coefficient is based on resonance-integral calculations involving higher plutonium isotope and fission product effects than were used in reference (1). The Monte Carlo technique used in reference (1) has been improved and if used would give a Doppler coefficient 25% higher than the resonance-integral method. Table 3 summarizes the pertinent nuclear design data.

There are 49 control rods, 7 per module. The central rod in each module is a safety rod, and the six peripheral rods in the first row of radial blanket assemblies are used for power regulating and shim control. The total worth of the 49 rods is 6.9% *k* against an estimated reactivity requirement of 6.3% *k* (see Table 3).

III. Fuel-cycle Cost

Fuel-cycle analyses of the Westinghouse modular fast breeder reactor designs have been placed on a

more realistic basis than was directed by the USAEC during the four conceptual design studies.⁽¹⁻⁴⁾ For example, a 10% annual charge was assumed to apply on all working capital and inventory charges rather than the 4¾% directed by the Commission in 1963.

TABLE 2. REACTOR MECHANICAL DESIGN DATA

<i>Core Modules</i>	
Number of Modules	7
Number of Fuel Assemblies/Module	36
Number of Blanket Assemblies/Module	48
Number of Reflector Assemblies/Module	18
Number of Control Assemblies/Module	7
<i>Fuel Rods</i>	
Pellet Diameter, in.	0.264
Total Fuel Height, in.	72.0
Clad Thickness, in.	0.010
Clad Outside Diameter, in.	0.300
Fuel Material	(U, Pu)C (mod.)
Fuel Material Density, % Theoretical	90.0
Thermal Bond Material	Na
Clad Material	316 L
<i>Axial Blanket Rods (Integral with Fuel Rods)</i>	
Blanket Length each End, in.	12.0
Blanket Material	UC
Blanket Material Density, % Theoretical	90
<i>Fuel Assemblies</i>	
Type of Rod Array	triangular
Fuel-rod Pitch, in.	0.426
Type of Can	hexagonal
Can Width across Flats, in.	5.104
Fuel Rods per Assembly	127
Fuel Assembly Length (Approximate), in.	132

TABLE 3. NUCLEAR DESIGN DATA

Equilibrium Cycle Enrichment, %	~15
Doppler Coefficient, $T(dk/dT)$	-0.0034
Axial Expansion, $k/\%$ Elongation	-0.0049
Sodium Coefficient, $k/^\circ\text{F}$ (Isothermal Average)	~0
Estimated Reactivity Requirements, % <i>k</i> :	
Cold to Hot	1.3
Fuel Depletion and Fission Products	1.9
Shutdown Margin (Minimum)	3.0
Load Following	0.1
Total	6.3
Control Rod Worth, % <i>k</i> :	
Control Rods, 7 in All, Total Worth =	2.0
Peripheral Rods, 42 in All, Total Worth =	4.9
Total for Reactor	6.9

TABLE 4. MODULAR-CORE FUEL-CYCLE COST BASED ON OYSTER CREEK METHOD⁽⁸⁾

	ANL Normalization (AEC Fuel Ownership)	AEC Design	HPD Design (Today's physics)
Working Capital	0.39	0.88 ^(c, d, f)	0.53-0.63 ^(c, d, f)
Fabrication	0.40	0.31	0.2-0.3
Processing	0.21	0.27 ^(e)	0.25-0.28 ^(c)
Plutonium Credit ^(d)	(0.39)	(0.60) ^(a)	(0.45-0.5)
	0.61	0.86	0.53-0.71

^(a) Based on a breeding ratio of 1.57 as reported in WCAP-3251-1.

^(b) Latest revised physics calculations gave a breeding ratio of 1.46.

^(c) Fuel (including bred material) fabrication, and processing at 10.4%.

^(d) Plutonium at \$10/gm (fissile) as nitrate.

^(e) Includes processing at \$21,150/day and NFS processing rates, shipping, and reprocessing losses at 1%.

^(f) Capacity factor at 88%, typical of first fifteen years for Oyster Creek Plant.⁽⁸⁾

Further, estimates of the plutonium value have been carefully developed as a function of the commercial and technical status of the industry.⁽¹⁷⁾

The fuel-cycle costs based on private ownership of fuel and commercial utility practice are given in Table 4. The design reported in reference (1) and normalized by the Argonne National Laboratory⁽⁵⁾ is given in the first column. The fuel-cycle cost for this design based on present commercial-utility practice⁽¹⁶⁾ is given in the second column. The fuel-cycle costs for this high-power-density design is given in the third column. The reduction in the plutonium credit reflects a reassessment of the breeding gain obtained by the use of more sophisticated analytical tools and which was found to decrease by about 20%.

This shifting of commercial-utility ground rules places strong emphasis upon obtaining higher specific power and results in the technical changes to the thermal design noted in reference (6). The more realistic appraisal of the commercial ground rules has led to an increase by a factor of 2 or 3 in the fuel-cycle cost over that reported in reference (1), which was based on AEC ground rules.

IV. Plant Description

The reactor plant design utilizes separate components to maximize accessibility for maintenance. Three heat transfer loops are projected for this plant. The general arrangement of a loop is presented in Fig. 2. This permits temporary operation at two-thirds ca-

capacity with one component out of service. The entire primary system is doubly contained in an inert gas atmosphere to minimize the consequences of a primary sodium leak.

The plant layout stresses accessibility and ease of maintenance. These two factors contribute significantly to long-term plant safety. High accessibility permits thorough routine inspection and increases the probability of locating small or incipient problems before they become major problems or incidents. Ease of maintenance permits more routine maintenance to be accomplished and increases the probability that maintenance or repair work will be successful. Significant features of the plant design are discussed in more detail in Appendix B.

Each of the three loops consists of a circulating pump, the single intermediate heat exchanger, connecting double-walled piping, and instrumentation for operational control of the system. The pump suction is connected to the intermediate heat exchanger shell, while pump discharge is connected to the reactor vessel top inlet. The top discharge from the reactor vessel is connected to the intermediate heat exchanger. All system equipment that contains sodium is heat-traced. All primary-system equipment is located inside the primary reactor containment. The environment around all primary-system equipment will be nitrogen or oxygen-depleted air to prevent sodium oxidation and radioactivity dispersal in the event of a leak.

The reactor vessel is of double-walled construction, consisting of a primary vessel and a secondary vessel, and is fabricated entirely from Type 304 stainless steel. The primary vessel provides containment for the reactor core and serves as the support member for the core and core barrel. The radial gap between the primary and secondary vessels is selected so that if drainage of sodium from the primary to the secondary vessel occurs, the core will remain submerged in sodium. During power operation this gap is filled with stagnant nitrogen. At shutdown, the sodium within the vessel is heated by circulating hot nitrogen between the two vessels.

The shield-plug thickness is 5 ft and consists of the following materials listed from top to bottom: 4 in. of carbon steel; 12 in. of stacked 3 w/o boronated graphite block; a layer of carbon steel shielding (not a structural or pressure-containing component); a type 304 stainless steel structural member; 12 in. of boronated graphite block; a type 304 stainless steel structural and pressure-containing member; and 24 in. of blast-absorbing crushable material. The total minimum thickness of the four steel plates is 11½ in. Nitrogen is provided for plug cooling in order to main-

Fig. 2. Reactor Building Elevation.

tain the upper plug surface and seal at temperatures below 130°F.

The control-rod drives, of a rack and pinion type, are located on the top of the upper plug and are completely enclosed by a gas-tight shell. Power, control, and gas-supply lines form helical coils which are suspended from the ceiling of the refueling cell. This arrangement makes it possible to remove the top plug from the reactor, hoist it up, and move the plug to its storage position without disconnecting any cables or gas-supply lines.

The three primary variable-speed pumps for circulating sodium are double suction, vertical, centrifugal units of the free surface type. All parts of the pumps in contact with sodium are stainless steel except the bearings and other special parts. Each pump is equipped with a smaller auxiliary motor connected to a separate power supply to insure adequate minimum sodium flow in the event of loss of power.

The intermediate (sodium to sodium) heat ex-

changers are of the vertical shell-and-tube type with the primary sodium flowing through the shell side of the heat exchanger, entering and leaving through two diametrically opposed horizontal nozzles. Intermediate sodium flows through the tubes. The shell side contains a free sodium surface covered by argon gas to prevent oxidation and to minimize thermal stresses in the tube sheet. The argon space contains a sodium-overflow line to accommodate volume surges.

The primary hot cell is a gas-tight structure enclosed by 6-ft-thick concrete shield walls lined with stainless steel. Periscopes and lead glass windows are provided in the walls and ceiling for viewing critical operations. Provisions are made to heat the cell walls during refueling operations to prevent condensation of sodium vapor.

A shield door closes off one end of the cell, providing a maintenance area for the refueling machine. With the door closed air may be admitted to the maintenance area.

The reactor is refueled by a remotely controlled refueling machine operating in the shielded, inert atmosphere cell. The refueling machine accomplishes all the fuel-handling operations, from the introduction of new fuel assemblies into the cell to the final loading of the spent fuel into the transfer cask. In addition to handling fuel, the refueling machine also removes and replaces reflector elements and control rods.

The refueling machine is a remotely controlled rectilinear crane that carries a vertical telescoping mast. Two cooling systems are provided with separate gas supplies. Automatic controls switch from one system to the other in the event of a system failure. The forced-cooling system must operate continuously during transfer of a spent core assembly. The design provides a method of checking the cooling system immediately after a fuel element is withdrawn from the sodium. If cooling gas is lost during a transfer, a fuel element could reach the boiling point of sodium in 5 min.

Spent fuel assemblies and control rods are stored in a sodium-filled tank located below the cell floor. Pipes extend through a top cover plate and shield down into a tank in which sodium coolant is circulated. This provides storage space for more than a full core load of fuel and blanket assemblies. Because sodium is used as the coolant, the storage pit can also be utilized for storing partially spent fuel during maintenance operations. In addition, the heat-rejection system and heat capacity of the spent-fuel storage pit are used to remove decay heat during shutdown from the reactor.

V. Description of the Primary-system Safety Features

The design of the Westinghouse Fast Breeder Reactor incorporates many design and safety features that will enhance safe operation. Inherent safety features include a negligible sodium temperature coefficient and a negative Doppler coefficient of reactivity. The mechanical design is such that structural thermal expansion (radial and axial) contributes a negative feedback to the power coefficient.

Each module has one safety rod in the center. The seven of these together are worth 2% to the total reactor. Each module also has six identical peripheral control rods, three of which are used as safety rods and three as regulating rods. All 21 peripheral safety rods from the seven modules together are worth 2.45% to the total reactor, and all 21 peripheral control rods together are worth 2.45% to the total reactor. This provides a total shutdown margin in excess of 3% cold, and sufficient operating reactivity to permit an-

nual refueling. One rod-drive power supply is provided for each module, which is capable of activating only one rod at a time. Thus the maximum number of rods, one rod in each module, which can be simultaneously actuated is seven. All rods can be scrammed simultaneously with a release time of 200 ms (similar to Fermi and EBR-II).

Positive reactivity due to partial voiding of sodium, although highly unlikely in the modular-core concept, can be controlled by rod scram action. The time between initiation of a power excursion and the beginning of sodium boiling is sufficient to permit the insertion of fast-acting control rods for all accidents studied. Further investigations of core voiding must be made to understand fully the interaction and time dependency of not only Doppler and sodium temperature coefficients, but also the dynamic behavior of sodium under abnormal conditions of boiling, two-phase flow, expulsion, and voiding.

The reactor vessel is doubled-walled so that leaks in the reactor vessel cannot drain the core and leave it without cooling. The inlet and outlet nozzles to the reactor vessel are located above the core, so that leaks anywhere in the primary system cannot siphon the core dry. The primary piping is of double-wall construction with sodium-leak detectors located at the low points of the containment.

Blanket assemblies have smaller lower nozzles than fuel assemblies, so that fuel assemblies cannot inadvertently be inserted into blanket positions.

The shield plug is equipped with 2 ft of energy-absorbing material on the lower face to absorb partially the high-energy release in the event of the core-meltdown accident. In addition, strong hold-down latches are located at the top of the plug to prevent the plug from becoming a missile.

The reactor protection system will detect potentially unsafe trends and conditions, and initiate corrective action. The reactor will scram automatically when an unsafe condition develops. The functions connected with the primary system which will automatically scram the reactor are listed in Table 5. Shutdown capability is provided by control rods that are spring assisted into the core. The reactivity shutdown margin is greater than 3.0% for the core with sodium.

A safety margin is provided by placing the reactor and its entire primary coolant system in reinforced concrete cells, which are backed up by a low-leakage steel containment shell. The two containment barriers will be designed to withstand conditions more severe than any that are expected to occur. The structural integrity of both containment barriers, reactor vessel, and piping will be provided by con-

forming to all applicable design and structural codes, laws, and regulations of the appropriate regulating bodies.

The primary-system coolant pumps are equipped with flywheels attached to the rotor shaft of the drive motor. This provides inertia to the system to obtain a pump-coastdown characteristic giving 60% of full flow after 5 sec.

A coolant loss due to primary-system rupture would, by itself, cause no increase in pressure within the containment barriers as the reactor operates near atmospheric pressure, 15 psig, with temperatures at least 500°F below the boiling temperature of sodium at atmospheric pressures. Mechanisms that could increase the temperatures and pressures within the containment barriers are: (1) a nuclear power excursion giving a rapid release of energy, and (2) a chemical reaction of sodium with air or water vapor. The effect of a chemical reaction between sodium and air within the concrete cells is minimized by maintaining an inert atmosphere, nitrogen, within these cells. The mechanical design of the concrete cells provide protection in the improbable event of a nuclear power excursion.

VI. Accident Analyses

A review of the reactor design yielded a list of the major accidents, credible and incredible, which can be postulated to occur to the reactor given the necessary conditions. Table C-1 in Appendix C contains the resultant list along with preliminary engineering safeguards for controlling each event. The accidents are grouped into the following four categories:

1. fuel-handling mishaps;
2. start-up mishaps;
3. full-power mishaps;
4. sodium spills and fires.

By proper system design and the application of the suggested preliminary engineering safeguards, all the accidents listed can be controlled and prevented from leading to gross disassembly of the core. A sequence of events potentially leading to core meltdown and the resulting energy release is discussed later. A controlled accident may result in some degree of core damage which will not present a hazard to the personnel or to any part of the plant outside of the primary cooling system. During a controlled accident the control system functions properly and scrams the reactor. To obtain an uncontrolled accident an accumulation of mechanical failures, including failure to scram, with possible loss of administrative control are necessary.

Events initiating a power excursion do not occur in-

TABLE 5. LIST OF PRIMARY-SYSTEM ABNORMALITIES CAUSING SCRAM AND/OR ALARM

1. Source flux level low ^(a)
2. Log count-rate level high
3. Log count-rate period short or detector voltage low ^(b)
4. Log <i>N</i> —period short or detector voltage low ^(b)
5. Power range above fixed level or detector voltage low ^(b)
6. Any control rod unlatched
7. Any control rod not "Full Down" ^(a)
8. Reactor inlet coolant flow low or rate of flow change high
9. Reactor outlet coolant temperature high or rate of change high
10. Primary pump power interruption
11. Primary system coolant level low
12. Reactor cover gas pressure high
13. Cell isolation trip (access open)
14. Manual scram
15. Reactor "noise" due to boiling or voiding in a single fuel subassembly
16. Mismatched sodium outlet temperature from the various assemblies

^(a) Effective only prior to reactor operation; bypass when control power is available.

^(b) During startup, any two out of three channels will cause scram and alarm.

stantaneously because all processes have a finite time dependence. Because of the time element associated with an accident, it is within engineering reality to control the worst accident, sodium voiding, by fast-acting control rods capable of introducing several percent reactivity in a rather short time, i.e., less than 400 ms from release to full insertion. If a power excursion is terminated without gross fuel melting and collapse of the core, the accident will have been controlled and the large energy release associated with core compaction will have been prevented.

The characteristics of the proposed fast breeder reactor were analyzed for inherent stability and the consequences of several accidents selected from the first three categories in Table 5, which were considered most likely to result in core damage. The analyses which were performed concerned:

1. inherent stability;
2. dropping a fuel assembly into a just subcritical core;
3. expulsion at 100% thermal power of a regulating control rod;
4. loss of pump power at 100% thermal power;
5. pipe rupture at 100% power in one of three loops;
6. local flow blockage;
7. fuel slumping;
8. core meltdown.

The analyses of the above accidents were performed for an isolated core representing the center module

TABLE 6. REACTIVITY COEFFICIENTS FOR MODULAR CORE

	Pessimistic		Nominal		Optimistic	
	Clean Core	End of Core Life	Clean Core	End of Core Life	Clean Core	End of Core Life
Doppler						
a. $T(dK/dT)$	-0.00287	-0.00254	-0.00383	-0.00338	-0.00459	-0.00423
b. $(dK/dT) \times 10^6/^{\circ}\text{F}$	-1.57	-1.39	-2.1	-1.85	-2.52	-2.31
Sodium						
(dK/dT) Average	-0.5×10^{-6}	$+0.5 \times 10^{-6}$	-1.0×10^{-6}	0.0	-1.5×10^{-6}	-0.5×10^{-6}
Axial Expansion						
a. $H(dK/dH)$	-0.487	-0.438	-0.541	-0.487	-0.595	-0.536
b. $(dK/dT) \times 10^6/^{\circ}\text{F}$	-3.896	-3.504	-4.33	-3.896	-4.76	-4.288

FIG. 3. Sodium Voiding k_{eff} as Function of Radius Voided.

of a multiple-coupled core system. A modular core designed on the basis of an isolated core would be conservative. In particular, the results of the analyses for dropping a fuel assembly and for expulsion of a control rod would be pessimistic for a coupled core system because of the neutronic cross-coupling effects. In a modular core there is information which indicates a reactivity insertion into one module is divided between all modules as some function of the cross coupling.

The first five analyses were performed with the W-FORE digital computer code developed at Westinghouse. This code is similar to the FORE⁽⁹⁾ code. All reactivity feedbacks are responsive to temperature changes in the core. The Doppler effect varies inversely with fuel temperature. The core is divided into five axial segments. The sodium temperature coefficient is given a spatial distribution with core height. The axial expansion is a function, by an input control option, of the change in fuel or clad temperature.

The range of reactivity coefficients, Doppler, so-

dium, and axial fuel expansion, for the clean core and the end-of-life core, 66,000-MWD/T burnup, used in the analyses are given in Table 6. All coefficients remain negative throughout core life. The effect of the change in isotopic composition of the fuel and fission products is to cause all coefficients to become less negative by a factor of 1.14 for Doppler, and 1.1 for axial fuel expansion. The optimistic and pessimistic values were obtained by applying estimated calculational error tolerances to the nominal values.

The reactivity added upon voiding the core is shown in Fig. 3. In the clean core, complete voiding removes approximately 1% in " k ," whereas for the end-of-life core the effect of the fission products is to cause a positive reactivity addition of 1.54% in " k " upon voiding. In the accidents analyzed, sodium voiding did not occur and thus voiding appears to be highly improbable in the modular core design.

VI.1 Core Inherent Stability

Local Inherent Stability. The local stability due to some local disturbance, such as flow starvation, was analyzed by use of the W-MABI computer code. The end-of-life core, 66,000-MWD/T burnup, was selected for the analysis as it represents the worst condition. The core was voided out to a radius of 24.5 cm, and the resultant power profiles are shown in Fig. 4. As the sodium density decreases in a local region, the power density in the affected region decreases. Any local disturbance thus does not propagate into a large local power excursion.

Overall Inherent Stability. The reactor response to a small reactivity insertion such as would be introduced at full power for a change in power level was studied to assess the overall inherent stability characteristics of the core. The reactivity insertion used was 10 cents in the form of a ramp of 10 cents per second. This insertion rate was dictated by the numerical stability of the computer code and represents a greater insertion ramp rate than would be designed into a con-

tol system. The reactivity rate of insertion in a typical control system would be in the order of 0.05 to 0.10 cent per second.

The analyses were performed for a clean core and

FIG. 4. Local Inherent Stability: End-of-Life Core Radial Power Profile.

an end-of-life core. The fuel axial expansion coefficient was set equal to zero. The rise in power with time is shown in Fig. 5. In a clean core, the new equilibrium power is at approximately 101% of full power, whereas with fission products the equilibrium power is 105% of full power. The new equilibrium power is reached at approximately 20 sec after the start of the reactivity insertion in the latter case. The fuel, clad, and coolant temperatures in the hot channel as a function of time are given in Table 7. The increases in temperature are 193°F for the fuel center, 74°F for the fuel surface, 68°F for the clad, and 56°F for the hot channel coolant outlet.

The results shown in Fig. 5 and Table 7 are optimistic, for the fast breeder being presented embodies variable-speed pumps in the primary system in order to hold a constant core temperature rise above 50% power. Except during the time lag for accelerating the pumps, the negative sodium temperature coefficient effect would be minimized. With this condition the equilibrium power would be higher than shown in Fig. 5.

VI.2 Refueling Accident: Dropping Fuel Assembly into a Just Subcritical Core

This accident assumes a loss of administrative control followed by a mechanical failure. Administrative control is lost during calculating the shutdown reactivity and the core is just subcritical with a center fuel assembly missing. Then, as the last fuel assembly is being lowered into the core, a mechanical failure causes the latch mechanism to release the fuel assembly, allowing it to fall into the core.

FIG. 5. Overall Inherent Stability. Reactor Power Response to a Change in Power Level.

TABLE 7. MAXIMUM TEMPERATURES (°F) IN HOT CHANNEL FOR REACTIVITY INSERTION OF 10¢/SEC (TOTAL INSERTION 10¢) AT FULL POWER

Time, sec	Clean Core: Nominal Sodium and Doppler Reactivity Coefficients				Core End-of-Life: Nominal Sodium and Doppler Reactivity Coefficients			
	Coolant	Clad	Fuel Surface	Fuel Center	Coolant	Clad	Fuel Surface	Fuel Center
0.0	1175	1238	1274	1793	1175	1238	1274	1793
1.03	1207	1274	1318	1917	1211	1279	1324	1929
2.08	1218	1289	1331	1948	1226	1298	1342	1971
3.1	1218	1290	1332	1949	1228	1301	1344	1977
4.0	1218	1290	1332	1949	1228	1302	1345	1978
5.0	1218	1290	1332	1949	1229	1303	1346	1980
6.01	1218	1290	1332	1949	1229	1304	1347	1982
7.02	1218	1291	1332	1949	1230	1304	1347	1983
8.1	1218	1291	1332	1949	1230	1305	1348	1984
9.1	1218	1291	1332	1949	1231	1305	1348	1985
10.0	1218	1291	1332	1949	1231	1306	1348	1986

The maximum worth of a fuel assembly is 75¢. The fuel assembly is assumed to fall freely into the core. Under this assumption, 0.615 sec is required for complete insertion, a 6-ft travel. Including drag forces during the fall lengthens the insertion time to 0.7 sec. The reactivity addition is taken to be proportional to insertion instead of the usual "S"-shape distribution.

The core is assumed to be at a source and/or decay heat power level of 100 W, a flow at 9% of full flow, and a coolant temperature of 400°F. The clean-core pessimistic reactivity coefficient (see Table 6) were assumed for this analysis.

The rise in power as a function of time is shown in Fig. 6, and the hot-channel maximum temperatures for coolant outlet, clad, and fuel, are presented in Table 8. At 17 sec the power is 60 MW and rising on a slope which appears to be approaching a maximum. The maximum fuel and coolant temperatures at 17 sec are 936°F and 887.3°F, respectively.

VI.3 Control-rod Expulsion at Full Power

There are seven control rods in the isolated module. Four, each worth 1.2% in k , are safety rods and three, each worth 80 cents, are shim rods for load follow and burnup. In the analysis it was assumed that the rod to be expelled from the core is a shim rod in the fully inserted position. Expelling a control rod is a highly unlikely or even an incredible accident, as a sequence of events must happen. In expelling the control rod, it is assumed that the control-rod drive mechanism or the control-rod extension shaft breaks and that the full pressure drop in the core, 100 psi, acts on the control rod. Under the conditions

assumed, the control rod will be expelled at an initial acceleration of 100 ft/sec² (3 g's), neglecting fluid drag forces. In the analysis, the initial acceleration rate was held constant for the full core height, 6 ft. The rod is fully out of the core in 0.35 sec.

The core was assumed to be at full power and flow. In the analyses the pessimistic reactivity values for both the clean core and the end-of-life core were used. The core power as a function of time is shown in Fig. 7, and the maximum temperatures for coolant outlet, clad, and fuel in the hot channel are given in Table 9. In both cases analyzed, the reactor power after the initial power spike approached an equilibrium power of 200% of full power for the clean core and of 212% of full power for the end-of-life core. The maximum coolant outlet and fuel center tempera-

FIG. 6. Reactor Power upon Dropping a Fuel Assembly Worth 75 Cents. Initial Power Is 100 W, Initial Temperature Is 400°F.

TABLE 8. MAXIMUM TEMPERATURES (°F) IN HOT CHANNEL FOR DROPPING FUEL ASSEMBLY ACCIDENT. ASSEMBLY WORTH IS 75 CENTS. ACCELERATION IS 32 FT/SEC². INITIAL POWER IS 100 W.

Time (sec)	Clean Core: Pessimistic Sodium, Doppler, and Expansion Reactivity Coefficients			
	Coolant	Clad	Fuel Surface	Fuel Center
0.0	400.0	400.0	400.0	400.0
5.0	400.2	400.2	400.2	400.5
10.0	490.2	492.9	494.8	527.7
15.0	816.6	819.6	821.5	867.7
16.0	857.3	862.0	864.5	902.9
17.0	887.3	893.2	896.3	936.0

tures were 1565°F and 2933°F, respectively, for the clean core, and 1610°F and 3046°F for the end-of-life core. Thus an isolated module can absorb this accident without the occurrence of even local core damage.

The results for control-rod expulsion are considered to be pessimistic for the modular systems. As previously mentioned, for a modular-core design existing information indicates a reactivity insertion into one module will be divided between all modules as some function of the cross coupling. Thus the module affected would feel only a fraction of the full reactivity insertion.

VI.4 Loss of Primary-pump Power at Full Reactor Power

The loss of primary-pump power is a credible accident for all nuclear reactors. Again, the total loss of all pump power is a highly unlikely or even an incredible accident as the pumps are supplied by more than one power supply. The safety and integrity of the core is strongly dependent on the flow-decay characteristics of the primary-system pump. Because of this importance, the primary pumps are assumed to be equipped with a high-inertia flywheel attached to the rotor of the drive motor to obtain the flow-decay characteristic curve given in Fig. 8.

The core conditions at the time of loss of electrical power to all three primary pumps were taken to be 100% power and 100% flow. The reactivity coefficients (Doppler, sodium, and axial expansion) used were the pessimistic values for both the clean core and the end-of-life core. As all the reactivity coefficients are negative, the decay in the reactor power (see Fig. 9) follows the pump flow but with a time delay. For example, at 5 sec in the clean core, the flow is 60% of full flow and the reactor power is 822.6% of full power. The maximum coolant outlet and fuel temperatures after 5 sec were 1367°F and 1927°F, respectively, for the clean core, and 1372°F and 1936°F, respectively, for the end-of-life core (see Table 10).

These results, although performed for an isolated core, represent a modular-core design, for all modules in a loss-of-pumping-power accident would be affected simultaneously.

VI.5 Rupture of Primary-coolant Piping in One of Three Loops

The rupture of the primary-coolant piping can occur, but it is a highly unlikely accident. In the analysis of the pipe-rupture accident, it was assumed that the rupture occurs at the pump discharge in one of the three primary loops. The rupture was taken to be a clean break with the end of the pipe shifting in a manner so that the coolant discharged

FIG. 7. Reactor Power for Expulsion of a Control Rod Worth 80 Cents at Full Power. Acceleration Rate of Expulsion Is 100 Ft/Sec².

TABLE 9. MAXIMUM TEMPERATURES (°F) IN HOT CHANNEL FOR CONTROL-ROD EXPULSION ACCIDENT. ROD WORTH IS 80 CENTS. ACCELERATION OF EXPULSION IS 100 FT/SEC². INITIAL POWER IS FULL POWER

Time (sec)	Clean Core: Pessimistic Sodium, Doppler, and Expansion Reactivity Coefficients				Core End-of-Life: Pessimistic Sodium, Doppler, and Expansion Reactivity Coefficients			
	Coolant	Clad	Fuel Surface	Fuel Center	Coolant	Clad	Fuel Surface	Fuel Center
0.0	1175	1238	1274	1793	1175	1238	1274	1793
1.00	1458	1558	1650	2684	1479	1581	1678	2742
2.09	1496	1619	1697	2766	1527	1655	1737	2845
3.05	1510	1638	1715	2798	1545	1679	1760	2886
4.02	1522	1653	1730	2828	1559	1696	1778	2919
5.09	1533	1666	1744	2855	1572	1712	1795	2952
6.06	1541	1676	1755	2875	1582	1725	1809	2978
7.01	1548	1685	1764	2893	1590	1735	1819	2998
8.00	1555	1692	1772	2908	1598	1744	1829	3015
9.10	1561	1699	1780	2923	1605	1752	1838	3033
10.0	1565	1705	1786	2933	1610	1759	1845	3046

FIG. 8. Pump Flow during Coastdown, High-inertia Flywheel Attached to Rotor of Drive Motor.

FIG. 9. Reactor Power for Loss of Primary-Pump Power Accident. Initial Power, P_0 , Is 100% Power. Initial Flow Is 100% Flow.

freely from both ends of the break into the pump cell. The pumps in the other two loops continued to operate with the flow being divided according to the pressure drop between the core and the ruptured pipe. The pumps were assumed to lose suction at 3.1 sec once the level of the sodium pool above the core was lowered to the outlet nozzle, allowing the cover gas to enter the pump intake. The core flow with time is shown in Fig. 10 for the above assumptions.

The core conditions at the time of the pipe rupture were taken to be 100% power and 100% flow. The reactivity coefficients (Doppler, sodium, and axial fuel expansion) used in the analyses were the pessi-

mistic values of Table 6 for both the clean core and the end-of-life core. As in the loss-of-primary-pump-power accident, the reactor power (see Fig. 11) follows the core flow but with a time lag. Up to the time, 3.1 sec, of losing pump suction, the maximum temperatures in the hot channel occurred between 2.0 and 2.6 sec (see Table 11). The maximum coolant outlet and fuel center temperatures were 1661°F and 2018°F, respectively, for the clean core, and 1694°F and 2066°F, respectively, for the end-of-life core. The pressure at the core outlet was approximately 25 psia, giving a sodium boiling temperature of 1650°F. This is a conservative boiling temperature as the liquid superheat required to initiate vapor formation is neglected. Thus, at worst, a very small amount of boiling may occur in the hot channel, but the reactor should be scrammed when boiling begins.

TABLE 10. MAXIMUM TEMPERATURES (°F) IN HOT CHANNEL FOR PUMP-COASTDOWN ACCIDENT

Time (sec)	Clean Core: Pessimistic Sodium, Doppler, and Expansion Reactivity Coefficients				Core End-of-Life: Pessimistic Sodium, Doppler, and Expansion Reactivity Coefficients			
	Coolant	Clad	Fuel Surface	Fuel Center	Coolant	Clad	Fuel Surface	Fuel Center
0.0	1175	1238	1274	1793	1175	1238	1274	1793
1.10	1230	1289	1320	1921	1233	1292	1323	1925
2.10	1271	1330	1359	1924	1271	1330	1359	1930
3.00	1300	1358	1386	1921	1304	1362	1390	1928
4.05	1336	1392	1419	1920	1341	1397	1425	1928
5.00	1367	1422	1448	1927	1372	1427	1454	1936

FIG. 10. Decay of Core Flow for Pipe-rupture Accident in One of Three Loops.

FIG. 11. Reactor Power for Primary-pipe-rupture Accident in One of Three Loops. Initial Power, P_0 , Is 100%; Initial Flow Is 100%.

VI.6 Local Flow Blockages

Coolant flow to a fuel assembly could be lost by blocking physically the inlet nozzle or by plugging with sodium oxide. If the inlet nozzle is blocked by buildup of sodium oxide, it is highly probable that the buildup would be uniform in all fuel assemblies rather than in a single fuel assembly. This would be manifested by a gradual increase in the core pressure drop, which would be detectable by differential-pressure monitors. Therefore, analysis for local loss of coolant flow is taken to be the instantaneous loss by mechanical blockage of flow to one fuel assembly, since the block-

ing of more than one fuel assembly is a highly unlikely occurrence.

After losing flow to one fuel assembly, the temperature of the stagnant coolant rises until the boiling point is reached and voiding occurs. This takes place in approximately one second. The clad and fuel temperatures continue to increase, and clad melting occurs before the carbide fuel reaches its melting point, 4200°F. If it may be assumed that the fuel stack remains as a column, the fuel melting temperature will not be reached until approximately 3.5 sec. Thus, as the clad melting occurs considerably before fuel melting, the fuel will remain in solid state rather than in a molten state when fuel compaction occurs in the blocked fuel assembly. The reactivity addition

TABLE 11. MAXIMUM TEMPERATURES (°F) IN HOT CHANNEL FOR PIPE-rupture ACCIDENTS. INITIAL POWER IS FULL POWER

Time (sec)	Clean Core: Pessimistic Sodium, Doppler, and Expansion Reactivity Coefficients				Core End-of-Life: Pessimistic Sodium, Doppler, and Expansion Reactivity Coefficients			
	Coolant	Clad	Fuel Surface	Fuel Center	Coolant	Clad	Fuel Surface	Fuel Center
0.	1175	1238	1274	1793	1175	1238	1274	1793
0.50	1334	1364	1398	1906	1344	1370	1407	1905
1.02	1528	1544	1558	1903	1548	1560	1575	1925
1.52	1633	1671	1680	1962	1655	1705	1693	2022
2.07	1661	1699	1717	2004	1697	1757	1750	2059
2.57	1640	1686	1712	2018	1694	1738	1734	2063
3.05	1612	1680	1702	2016	1674	1723	1748	2066

upon fuel compaction is less than one dollar. The reactor will, therefore, remain on a delayed power period, and a scram will prevent further melting of the core.

VI.7 Fuel Slumping

Fuel slumping and core collapse appear to be highly unlikely accidents in the modular core as fuel melting did not occur in any of the accidents analyzed. But if it may be assumed that by some unknown manner fuel melting does occur and the fuel slumps uniformly throughout the core within the clad, filling all the void space and increasing its density from 90% to 95%, the reactivity addition in the isolated module would be \$18. Then if the upper half of the core folds over into the bottom half, the total reactivity insertion including slumping within the core would be \$75.

VI.8 Core Meltdown

The energy release during core compaction was analyzed by a modified Bethe-Tait approach with Doppler feedback as used in the "WEAK EXPLOSION" code. The equation of state for PuC used in the analysis is given in reference 21. The collapse time of the upper half of the core into the lower half, assuming free fall, was 0.3 sec. The worth of such an incident was found to be about \$75, thus yielding a reactivity insertion rate of \$250/sec. The total energy released for the isolated module was approximately 2100 MW-sec.

VI.9 Summary of Accident Analyses

An isolated core of the same size as a module in the modular core has good safety characteristics. The accidents analyzed for an isolated core are sum-

TABLE 12. SUMMARY OF ACCIDENT ANALYSES

Analyses Performed	Clean Core at Beginning of Life	Core at End of Life (66,000 MWD/T) with Fission Products
1. Reactivity Coefficients a. Doppler b. Axial Expansion	Reference (see Table 5)	Decreases by 14% Decreases by 10%
2. Complete Core Voiding	Removes 1% in "k"	Add 1.54% in "k" during voiding
3. Core Inherent Stability at 100% Power and 100% Flow a. Local Stability b. Overall Stability to a Change in Power Setting (10% in one second)	Good Good—Reactor power increases by 1%. Equilibrium power after 10 sec	Good Good—Power increases by 5%. Equilibrium after 20 sec.
4. Dropping of Fuel Assembly Worth 75¢ into Just Subcritical Core. Initial Power is 100 W and Flow is 9% of Full Flow	After 17 sec, power is 60 MW and is approaching a steady-state value	Not applicable as fuel assemblies are being removed at this condition
5. Shim Control-rod Expulsion. Rod Worth 80¢ at 100 ft/sec ² . 100% reactor power and flow	Equilibrium power is 200% after 10 sec. Sodium boiling and fuel melting do not occur. Terminate excursion with <i>over-power</i> scram	Equilibrium power is 212% after 10 sec. Sodium boiling and fuel melting do not occur. Terminate excursion with <i>over-power</i> scram.
6. Loss of Primary-pump Power at 100% Reactor Power and 100% Flow	Reactor power decays as flow, but at a lesser rate. Excursion can be terminated by loss of pump power scram	Reactor power decays as flow, but at a lesser rate. Excursion can be terminated by loss of pump power scram
7. Local Flow Blockage of a Single Fuel Assembly	Sodium boiling occurs in 1 sec. Clad melts before fuel melts. A scram actuated by a noise analyzer required to terminate excursion before blocked assemblies incur gross damage	Sodium boiling occurs in one second. Clad melts before fuel melts. A scram actuated by a noise analyzer required to terminate excursion before blocked assemblies incur gross damage
8. Pipe Rupture	Reactor power decays as flow, but at a lesser rate. Excursion can be terminated by low system pressure or low flow scram	Reactor power decays as flow, but at a lesser rate. Excursion can be terminated by low system pressure or low-flow scram.

marized in Table 12. In each of the accidents analyzed, the reactor power obtained, or was approaching, an equilibrium power below that required to cause even local core damage. The reactor could have been scrammed in each accident. Although the interaction of a multimodule was not analyzed in this study, it is reasonable to predict that it would have safety characteristics better than the single module analyzed because the neutronic cross-coupling effect divides among all modules any reactivity insertion occurring in one module. The results of the control-rod expulsion accident and the dropping of a fuel assembly for the single module are pessimistic compared to a multiple-module core. The other accidents, pipe rupture and loss of pump power, would essentially be identical for the modular core as all modules are affected simultaneously in the same manner. Of the accidents analyzed for the isolated core, the loss of flow to a single fuel assembly has the highest probability of leading to collapse and compaction of fuel. A local loss of flow is the most difficult accident to detect as the reactor power does not change before voiding occurs. A noise-analyzer instrumentation system for boiling would be one method of detecting this accident and initiating reactor scram before fuel compaction occurs (see Reference 20). If the loss of flow to a single fuel assembly propagates to complete core meltdown, a minimum of 8.5 sec is available for scramming the reactor before the entire core is voided. The meltdown energy release calculated by the Bethe-Tait method is approximately 2100 MW-sec for the isolated core.

The pipe-rupture and the loss-of-primary-pump-power accidents without scram could lead to core meltdown if the reactor was also on a fast period approaching 100% power prior to the accident. The power overshoot that would occur during these accidents would be sufficient to carry the hot-channel coolant outlet temperature above the boiling point. Because of the large superheat capabilities of sodium, this might not cause vapor formation in the hot channel. This approach to core meltdown requires a multiplicity of events to occur and therefore is highly unlikely to occur.

VII. Combination of Accidents Leading to Core or Fuel-assembly Meltdown

The core-meltdown accident appears to be a highly unlikely accident in the modular-core concept. Of the major accidents selected and studied, the loss of flow at 100% power to a single fuel assembly in the end-of-life core without scram is the accident having the highest probability of leading to core meltdown. This

accident is the most difficult to detect by means of the usual instrumentation, flow, pressure drop, coolant temperatures, etc., and would require a noise analyzer to initiate a signal upon boiling to scram the reactor.

The time history without scram of a core-meltdown accident resulting from the loss of flow to a single fuel assembly is shown in Fig. 12. After the fuel assembly is physically blocked at the inlet, the reactor power remains constant, raising all temperatures in the blocked assembly. The stagnant coolant in the blocked assembly begins to boil at the core center-line in 0.9 sec. The formation of vapor at the center-line may expel out of the top of the core the coolant above the center section. The coolant in the lower portion of the assembly is boiled off in 1.5 sec. Voiding of one fuel assembly inserts 0.02% in k . Fuel melting and collapse of the blocked fuel assembly begin after 3.5 sec, adding sufficient reactivity to raise the reactor power to approximately 270% of full power. The time of collapse is 0.3 sec, corresponding to free fall over a distance of 3 ft. After collapse of the single assembly, the fuel and coolant temperatures in the unblocked portion of the core are increased to the point of voiding the entire core after 8.5 sec. Voiding the entire end-of-life core adds 1.54% in " k " to the system. The reactor power then rises on a prompt period, and fuel melting and collapse of the core begin after 10.6 sec. The energy release for the meltdown of the entire core is approximately 2100 MW-sec.

The loss of flow to a single fuel assembly in a module of the modular core may not lead to core meltdown. In the modular core, the effectiveness of

FIG. 12. Histogram of Core Meltdown Resulting from Loss of Flow to a Single Fuel Assembly.

the neutronic cross coupling in distributing among all modules a reactivity insertion in one module may be sufficient to confine the meltdown and fuel compaction to the fuel assembly which has lost flow. In addition, application of the controlled expansion concept (CEX) may provide sufficient negative feedback to override the reactivity worth of the collapsing of a single fuel assembly.

The simultaneous occurrence of an accident involving pipe rupture, the loss of primary pump power, or control-rod expulsion without scram when the reactor is already on the shortest controlled period approaching 100% power would probably lead to core meltdown. In the analyses performed for these three accidents, the hot-channel coolant outlet temperature was approaching the boiling point, and the expected overshoot in power may be sufficient to give boiling and voiding beginning at the outlet of the core. Once the center fuel assemblies are voiding, fuel meltdown will follow in these fuel assemblies. In the modular core the control-rod expulsion accident may not insert sufficient reactivity in the module losing the control rod to lead to core meltdown because of the neutronic cross-coupling effects. The isolated core-pipe rupture and loss-of-primary-pump-power accidents represent the modular core behavior as all modules would be affected simultaneous in the same manner.

A spent fuel assembly dropped onto the floor of the hot cell during the refueling transfer process would meltdown from decay heat if cooling is not available. Fission products and radioactive material would be released to the hot cell atmosphere. The release of a large explosive force as in the core-meltdown accident would not occur, as the amount of material in a single fuel assembly is less than a critical mass.

If during refueling a spent assembly becomes jammed in the refueling machine with failure of all cooling systems, the decay heat will be sufficient to meltdown the fuel assembly. Again, fission products and radioactive material would be released to the refueling hot cell. A large explosive force would not follow the meltdown of the fuel assembly.

VIII. Conclusions

From the results of these preliminary accident analyses, it would appear that a fast breeder reactor designed with mixed uranium-plutonium carbide fuel in a multiple modular geometric array possesses inherently safe operating characteristics. Several key accidents were studied, all of which indicated inherent stability of the reactor core. In the course of this study, several added safety features were identified which could be readily designed into the overall reactor system, thereby providing further engineered safeguards for this fast breeder reactor. Admittedly, not all conceivable accidents were studied in this investigation, but a careful selection was made for study, on the basis of past experience, of incidents most likely to result in the worst accidents.

The high neutron-leakage characteristic of a modular core serves to provide a zero or slightly negative temperature coefficient while maintaining a good total breeding ratio by use of the interspersed blanket material between the modules. The large negative Doppler coefficient was a predominant accident-limiting mechanism in most of the accidents studied.

The accuracy with which the sodium, Doppler, and other temperature-dependent power coefficients are presently known leaves much to be desired, and, similarly, the adequacy with which the digital computer/analog codes truly portray the actual mechanisms is also open to some question; however, the studies conducted would appear to represent a reasonable accurate "status-of-art" analysis.

It would appear from the results of these analyses that the particular core concept chosen may be overly conservative. Future cores could be considered having less strongly pronounced safety advantages, liberalized in terms of higher specific powers and internal breeding ratios and lower neutron leakage, inventory requirements, doubling times, and overall fuel-cycle costs. In anticipation of improved designs of this sort, a more precise knowledge of the power coefficients and transient nature of fast breeder cores must be developed. In addition to a more precise knowledge of the Doppler and coolant coefficients, it is necessary to achieve a better understanding of the thermo- and fluid dynamics as limited by sodium behavior during a prompt power burst.

APPENDIX A

Mechanical Design of Reactor

The reactor consists of seven hexagonal modules; each contains 37 fuel-assembly positions, one of which is occupied by a safety rod. Surrounding the

fuel assemblies are 54 blanket assembly positions, of which six are occupied by control rods. Each module is surrounded by 36 reflector assemblies, each of which is shared by an adjacent module except on the outer

periphery of the reactor. A one-quarter core layout is shown in Fig. 1.

The reactor is contained within a double-walled reactor vessel with coolant nozzles located above the top of the fuel assemblies. The core support structure is attached to a core barrel suspended from the upper part of the inner reactor vessel. The reactor vessel is a composite plug comprised of blast-absorbing material on the bottom and radiation-absorbing material above.

Control-rod drive mechanisms are located on top of the plug. Access to the reactor for refueling or maintenance is obtained by uncoupling the control-rod-drive lines, unlatching the plug from the vessel, hoisting the plug by means of a traveling hoist, and rolling it to one side.

The fuel assemblies are hexagonal, containing 127 rods. The rods are attached to an end fitting at the lower end. The end fitting in turn is attached to the can. The rods are laterally restrained by spring grids spaced uniformly up the can. The cans are terminated at the upper end by a lift fitting, and at the lower end by a mechanical latch similar to that described in Ref. 1. Bumper pads are provided at two levels on the exterior of the cans to take up clearance between cans.

The radial blanket assemblies are externally identical with fuel-assembly cans with latches of smaller diameter to prevent insertion of fuel-assembly cans into blanket positions.

The reflector-assembly cans are dimensionally similar to fuel-assembly cans. The reflector cans contain

short lengths of graphite logs and do not depend upon the graphite for support. They are helium back filled and welded shut. The reflector cans have end fittings which socket into the core support structure at the bottom and into the reflector support grid at the top. The reflector support grid positions the reflector around each module and limits the lateral movement of fuel and blanket cans. It does not interfere with refueling.

The fuel rods are stacks of stabilized (U,Pu)C pellets contained in stainless steel clad. The stabilization provides compatibility with the clad by chemically tying up the offending free element. The axial blankets are integral with the fuel rods. The fuel rods are sodium bonded and vented to the coolant.

The control-rod assembly consists of a hexagonal array of tubes occupying the center section of the modified fuel assembly. The absorber, B_4C powder, bearing tubes are segmented, with a horizontal grid at about one-foot intervals, making a unit assembly of the tube array. The top grid from which all rods hang is provided with a vertical connecting member to the control-rod drive coupling. The radial spacer grids permit differential linear expansion of the individual rods to eliminate any bowing which might otherwise develop due to flux gradients around the control rod perimeter. Strips attached at the spacer grids provide running surfaces against the control rod guide tube.

The numerical data for the mechanical design are tabulated in Table 1.

APPENDIX B

Plant Systems Description

Primary System

The primary coolant system consists of three identical loops to transfer heat from the reactor core to the intermediate sodium system. The general arrangement of a loop is presented in Figures 2 and 13. Each loop consists of a circulating pump, the single intermediate heat exchanger, connecting double-walled piping, and instrumentation for operational control of the system. The pump suction is connected to the intermediate-heat-exchanger shell, whereas pump discharge is connected to the top inlet of the reactor vessel. The top discharge from the reactor vessel is connected to the intermediate heat exchanger.

All system equipment that contains sodium is heat-traced. All primary-system equipment is located inside the primary reactor containment. The environment around all primary-system equipment will be nitrogen or oxygen-depleted air to prevent sodium

oxidation and radioactivity dispersal in the event of a leak.

Reactor Vessel

The reactor vessel (see Fig. 14) is of double-walled construction, consisting of a primary and a secondary vessel, and is fabricated entirely from Type 304 stainless steel. The primary vessel provides containment for the reactor core and serves as the support member for the core and core barrel. The upper end of the primary vessel is welded to the vessel support ring. The secondary vessel provides sodium containment in the unlikely event of a failure of the primary vessel. Three inches of stainless steel reflective insulation covers the outside of the secondary vessel.

The radial gap between the primary and secondary vessels is selected so that if drainage of sodium from the primary to the secondary vessel occurs, the core will remain submerged in sodium. During power operation this gap is filled with stagnant nitrogen. At

FIG. 13. One of Three Identical Loops.

shutdown, the sodium within the vessel is heated by circulating hot nitrogen between the two vessels.

Shield Plug

The shield plug has three major functions:

1. provides a full-diameter opening at the top of the vessel capable of containing the full design pressure;
2. provides shielding to attenuate neutron flux and gamma radiation;
3. provides support and alignment for control-rod drive mechanisms.

The total thickness of the plug shield is 5 ft. The plug shield consists of the following materials listed from top to bottom: 4 in. of carbon steel; 12 in. of stacked 3 w/o boronated graphite block; a layer of carbon steel shielding (not a structural or pressure-containing component); a type 304 stainless steel structural member; 12 in. of boronated graphite block; a type 304 stainless steel structural and pressure-containing member; and 24 in. of blast-absorbing crushable material. The total minimum thickness of the four steel plates is 11½ in.

The two lower stainless steel plates, above the crushable material, provide the structural support for the plug assembly and the design pressure loading. The space between these two plates contains ribs or similar stiffening members to enhance the load-carrying capability of the assembly.

Reflective stainless steel insulation is attached to the bottom of the plug assembly as required to reduce the rate of heat transfer through the plug and

to promote satisfactory temperature distributions throughout the upper region of the vessel and support structure.

Nitrogen is provided for plug cooling in order to maintain the upper plug surface and seal at temperatures below 130°F.

Closure of Shield Plug

The vessel closure is sealed and secured in place during normal operating conditions (at a pressure of 15 psig) by a low-melting-point metal alloy. Cerrobend, a lead, cadmium, tin and bismuth alloy, is typical of several possible seal materials. It has a density of 0.339 lb/in.³ and a melting point of 158°F. A shear strength of around 3500 psi makes it adequate for closure holddown at many times the normal operating pressure of 15 psig.

A rotating shear block closure provides a backup holddown and is capable of securing the plug at pressures of at least 150 psig. This mechanical closure is simple in design and will have the capability of being operated remotely by pneumatic cylinders mounted to the refueling-cell floor. Additional beams across the plug hold the plug in place in the event of a large release of explosive energy. Human access to the closure mechanism during plug removal is contemplated only under unusual conditions.

Axial Thermal Shield and Meltdown Pan

An axial thermal shield and meltdown pan assembly is supported at the bottom of the vessel below the core support structure. It consists of a 304 stain-

FIG 14 Reactor Vessel

less steel dished head with a conical piece welded to it. One-inch-diameter by 6-in.-length, 1.0 w/o natural boron-type 304 stainless steel rods are welded to the assembly.

Control-rod Drive System

The control-rod drives, of a rack-and-pinion type, are located on the top of the upper plug and are completely enclosed by the gas-tight shell. Power, control, and gas-supply lines form helical coils which are suspended from the ceiling of the refueling cell. This arrangement makes it possible to remove the top plug from the reactor and hoist it up and move the plug to its storage position without disconnecting any cables or gas-supply lines.

A positive pressure of inert gas is maintained in the containment shell around the drives to prevent contamination of the mechanisms by the cell atmosphere. Bellow seals similar to those used at Fermi and EBR-II isolate the control-rod drive components from the reactor cover gas. An inert gas pressure higher than the reactor cover gas pressure is maintained in the mechanism housing as further insurance against contamination.

The rack, which is attached to the control rod, is driven up and down by a pinion. As the rack moves up a spring is compressed. The motor torque is transmitted through a gear-reduction unit and an electromagnet clutch to the pinion. Provision is made in the drive train for continuous indication of control-rod position.

A scram is initiated by cutting the power to the electromagnetic clutch, which allows the rack and control rod to fall freely. The spring in the rack housing provides an initial force to accelerate the control rod more rapidly at the beginning of a scram. A spring- or pneumatically-operated dash pot within the rack housing decelerates the control-rod train at the end of its fall.

The lower end of the rack assembly is attached to the control rod with a latch.

Pumps

The three variable-speed pumps for circulating primary-system sodium are double-suction, vertical centrifugal units of the free surface type, consisting of:

1. a pump drive motor mounted on a shield deck;
2. a pump volute located in the primary-sodium-system piping below the shield deck;
3. a pump impeller located in the volute and connected to the motor by a shaft;

4. a vertical column between the shield deck and the volute to contain the shaft.

The vertical column contains a sodium pool, a chamber for argon cover gas, gas-shaft seal, and other necessary pump components.

All parts of the pumps in contact with sodium are stainless steel except the bearings and other special parts. Each pump is equipped with a smaller auxiliary motor connected to a separate power supply to insure adequate minimum flow of sodium in the event of loss of power.

Intermediate Heat Exchangers

The intermediate (sodium to sodium) heat exchangers are of the vertical shell-and-tube type with the primary sodium on the shell side, entering and leaving through two diametrically opposed horizontal nozzles. Intermediate sodium flows through the tubes. The shell side contains a free sodium surface covered by argon gas to prevent oxidation and to reduce tube-sheet thermal stresses. The argon space contains a sodium-overflow line to accommodate volume surges.

The thermal center of the heat exchanger is located above the center of the core to provide natural circulation of the coolant after a complete loss-of-power accident. The tube sheets and tube bundle may be removed from the shell for inspection or maintenance.

Valves

No loop-isolation or pump check valves are provided. Loop-isolation valves are omitted as power operation is not contemplated if one pump becomes inoperative. In this event, the plant would be shut down for the necessary repairs. By eliminating shut-off valves, system complexity is reduced, and plant economics are improved.

Piping

The primary piping is fabricated of ASTM A-358 Type 304 stainless steel. A wall thickness of $\frac{3}{8}$ in. was selected for structural stability even though the piping code requires less than $\frac{1}{4}$ in.

All primary piping is enclosed by secondary containment piping to limit the loss of coolant in the event of a leak. Without a secondary shell surrounding the primary piping, a leak could drain the pumps and other sections of the primary system, preventing circulation of sodium to remove decay heat. The secondary-containment material in the reactor plenum is stainless steel and the rest is $1\frac{1}{4}$ Cr, $\frac{1}{2}$ Mo. Leak detectors are located at the low points of the containment.

Insulation and Heat Tracing

All equipment that contains sodium is heat-traced to prevent sodium solidification. Selection of an ultimate heating system will be based on detailed engineering and economic analysis. The sodium piping and equipment are covered with insulation to reduce heat losses.

Temperatures

Temperature detectors in the hot and cold legs of each loop provide signals for control of the primary sodium system during startup, shutdown, and normal operation. The temperature signals are used by the reactor control and protection system for control of system temperature and are recorded in the main control room.

Sodium Level

The reactor vessel is equipped with a sodium-level detector. An alarm indicating low or high sodium level is sent to the reactor control and protection system to scram the reactor. Sodium level is recorded, and alarm indicators are located in the main control room.

Flow of Primary Sodium

Flow rates are measured in each cold leg of the primary sodium loop. A loop-flow signal in any loop actuates an alarm in the main control room. The low-flow alarm is sent to the reactor control and protection system, which initiates a low-flow scram if flow falls below a predetermined value.

Cell Structures

The primary-system hot cell is a gas-tight structure enclosed by 6-ft-thick concrete shield walls lined with stainless steel. The floor and ceiling are also lined with stainless steel. Periscopes and lead glass windows are provided in the walls and ceiling for viewing critical operations. Provisions are made to heat the cell walls during refueling operations to prevent condensation of sodium vapor.

A shield door closes off one end of the cell, providing a maintenance area for the refueling machine. With the door closed, air may be admitted to the maintenance area. Air leakage to the rest of the cell is prevented by introducing inert gas between two sets of door seals at a pressure slightly higher than that in either cell area. Normally, this maintenance area will be used only when the reactor spent-fuel storage-pool plugs and the reactor top plug are in place in order to protect the sodium from possible air leakage. Maintenance inside the hot cell will be performed by

personnel wearing breathing apparatus and protective clothing. The cell will not be supplied with air except for major maintenance when the sodium in the reactor vessel and storage pool must be covered or removed.

Reactor Plug Crane

The reactor plug crane is a remotely controlled bridge crane designed to lift the top plug from the reactor, move it aside for refueling, and replace it after refueling. Four separate hoists are provided to lift the plug, and a bridge drive is provided to move the crane along the rails.

The control-rod drives are lifted integrally with the plug after remote disconnection of the drive shafts. Electric cables connected to the drives have enough slack to allow the plug to be lifted and moved without disconnecting the cables.

The crane hoists are permanently connected to the plug top. During reactor operation, the crane is parked directly over the reactor. The lifting cables are rigged through sheaves over to the hoist mechanisms located on the end of the bridge in the equipment tunnel. The four separate hoists are driven by variable-speed DC motors, with speed-adjustment circuits in the control system that allow the motors to be driven individually or balanced to function together. Each hoist drum has two separate drive units connected to the drum by electric clutches. Power is carried to the hoist and bridge drive through a feed rail in the ventilation corridor. Circuits are provided for each motor so that a complete dual-drive system is available for the hoists, bridge drive, and clutches.

When replacing the plug, the reactor plug crane is positioned over the reactor, by driving against fixed stops. Level-indicating instruments are located directly on the plug top, and readings can be checked through a wall periscope. Corrections to level the plug are made by driving the hoists individually.

Fuel Handling

The reactor is refueled by a remotely controlled refueling machine operating in the shielded, inert-atmosphere cell. The refueling machine accomplishes all the fuel-handling operations, from the introduction of new fuel assemblies into the cell to the final loading of the spent fuel into the transfer cask. In addition to handling fuel, the refueling machine also removes and replaces reflector elements and control rods. All core components except control rods have lifting fixtures identical with those for the fuel.

The refueling machine is a remotely controlled

TABLE B-1. DESIGN PARAMETERS OF REFUELING MACHINE

Minimum Lift Capacity	1000 lb
Nitrogen Cooling Flow	3000 lb/hr at 200°F
Decay Heat of Hottest Fuel Assembly	54 kW
Maximum Speed of Bridge Travel	20 ft/min
Maximum Speed of Trolley Travel	5 ft/min
Maximum Speed of Vertical Mast	20 ft/min
Positioning Accuracy, Linear	$\pm 1/32$ in. (mast extended)

rectilinear crane that carries a vertical telescoping mast. Fuel assemblies and control assemblies are transported with this machine between the reactor, storage pool, and entry and discharge ports. Inert-gas cooling with backup is provided for spent fuel elements during transfer.

The bridge and trolley are driven by variable-speed drive units powered and controlled from supply lines and control leads hung in festoons along the cell wall. The mast of the machine is made in telescoping sections with provision to prevent rotation. A gripper for engaging and lifting core components is attached to the bottom of the mast. In addition to the lifting function, the mast must be capable of exerting a downward force of at least 200 lb in order to unlatch the fuel assembly from the support structure. The gripper mechanism is actuated with a pneumatic (nitrogen) cylinder located well above the sodium level. The gripper is designed so that it remains closed (latched) unless the pneumatic system is pressurized; even then it is not possible to open it if a weight exceeding 200 lb is hanging from it.

The method of powering all refueling machine motions is through use of pneumatic (nitrogen) piston-type motors and pneumatic cylinders. All control systems and valves are located outside the cell. Reliability, simplicity, and environment were the major factors in selecting this type of power unit. Electric motors would require complex cooling systems to operate in the cell atmosphere.

Cables or auxiliary power drives are provided to move the refueling machine and unload the fuel into the storage pool in the event of a failure of the normal motive equipment. Spent core assemblies are cooled during transfer by flow of nitrogen gas through the mast assembly supplied from a hose trailed behind the machine.

Two cooling systems are provided with separate gas supplies. Automatic controls switch from one system to the other in the event of a system failure. The forced-cooling system must operate continuously dur-

ing a transfer of a spent core assembly. The design provides a method of checking the cooling system immediately after a fuel element is withdrawn from the sodium. If cooling gas is lost during a transfer, a fuel element could reach the boiling point of sodium in 5 min.

The refueling machine is positioned at the desired location by aligning position markers attached to the machine with markers on the crane and trolley rails by means of optical equipment. An alternative system would use an electronic positioning system. Proper location is determined with an electrical position repeat-back system on both bridge and trolley which indicates the position of the vertical mast in relation to a fixed grid pattern. The electronic position-indication system would be backed up with the visual system described. The design parameters for the refueling machine are presented in Table B-1.

Storage of Spent Fuel

Spent fuel assemblies and control rods are stored in a sodium-filled tank located below the cell floor. Ports extend through a top cover plate and shield down into a tank in which sodium coolant is circulated. This provides storage space for more than a full core load of fuel and blanket assemblies. Because sodium is used as the coolant, the storage pit can also be utilized for storing partially spent fuel during maintenance operations. The heat-rejection system and heat capacity of the spent-fuel-storage pit are also used to remove decay heat from the reactor.

The shield plate and the height of sodium above the fuel provides sufficient gamma shielding in case the hot cell must be entered for maintenance. The storage-pool sodium is isolated from the cell atmosphere by seal plugs placed by the manipulator or refueling machine into the top guide sleeves of each storage position. An inert cover-gas blanket is maintained under the top plate at a pressure slightly below that in the refueling cell. This helps prevent contamination of the cell by fission gases released by the stored fuel. Decay heat is removed by circulating the sodium through an intermediate sodium-NaK heat exchange to maintain a temperature of 300°F.

Cover-gas System

The cover-gas systems serve the general function of providing a protective inert atmosphere for the sodium coolant. In addition, the cover gas is used as a collection mechanism for fission product gases (released from the vented fuel), for pressure control, and for continuous purging of the control-rod drive mechanisms.

Two argon cover-gas systems are used to carry out these functions. One of these is the cover-gas-supply system and the other is the cover-gas-purification system.

The supply system maintains an inert gas blanket in the reactor and in all piping, vessels, or equipment where a free surface of sodium exists. The system is designed to maintain the constant gas pressures required in the reactor, primary system, and the primary drain tank. Argon is also utilized as the displacement gas during draining, filling, and transferring sodium.

The purification system serves to remove gaseous fission products (released from vented fuel) from the argon cover gas from primary-system components. The gaseous fission products released to the cover gas are assumed to be xenon and krypton isotopes, as existing data indicate that the halogen fission gases released from the fuel react chemically with the coolant. The system is designed to handle the full

flow of the reactor and the fuel-storage cover-gas volumes (where the fission gases are expected to concentrate) and also bleed flow from other primary-system component cover-gas volumes where fission gases that become entrained in the sodium coolant may be released.

Nitrogen System

The purpose of the nitrogen system is to maintain an inert atmosphere in the hot cell, primary pump and pipe cells, heat exchanger cell, and maintenance cell to minimize the possibility of a fire with radioactive sodium. The system is also used to blanket areas where an inert atmosphere is desirable, such as the sodium melt stations and cyclone separators connected to the rupture discs from the steam generators. It also backs up compressed-air systems for pneumatic tools and instruments. In addition to its use for an inert atmosphere, the nitrogen is also used for cooling purposes.

APPENDIX C

TABLE C-1. LIST OF ACCIDENTS OCCURRING TO REACTOR

Accidents	Feasibility	Preliminary Engineering Safeguards
<i>A. Fuel-handling Mishaps</i>		
1. Improper removal of shield plug	Credible	Mechanisms and instrumentation insuring all control rods are disengaged and fully inserted into core
2. Loss of power to fuel-handling machine	Credible	Have backup power; make provisions for manual operation
3. Failure of coolant system of fuel-handling machine	Credible	Have temperature sensors to detect condition; auxiliary cooling system
4. Jamming of spent fuel assembly partially removed from core	Credible	Raise sodium level or use auxiliary inert gas cooling to prevent melting of rods and release of fission products
5. Structural failure of fuel-handling machine causing release of subassembly:	Credible	Keep core subcritical by an amount greater than the maximum worth of the fuel assembly during refueling; limit worth of assembly
a. Drop into core	Credible	Inert gas cooling
b. Dropped at some point before final storage	Credible	
6. Loss of administrative control (not following procedures):		
a. Coupled with mechanical failure (e.g., several control rods out of core and central assembly dropped in while refueling)	Credible	System of checks and balances in any given operation to limit results after mechanical failure; make control rods, fuel assemblies, etc., with relatively low worths
b. Independent of mechanical failure:		
(1) Improper enrichment of fuel assembly	Credible	Production quality control
(2) Interchange of core and blanket assemblies	Credible	Assemblies have different mechanical characteristics or different size of inlet nozzles fitting core support plate

TABLE C-I.—Continued.

Accidents	Feasibility	Preliminary Engineering Safeguards
<i>B. Startup Accidents</i>		
1. Loss of coolant while starting up	Unlikely	Design provides natural convection to cool core; scram reactor
2. Introduction of slug of cold sodium	Credible	Design for low sodium worth; design with large inlet plenum; loop-temperature measurements; scram reactor
3. Introduction of moderator to core	Unlikely	Do not have any hydrogenous material in the vicinity of the primary system
4. Bowing and resulting changes of equivalent core radius	Credible	Elements mechanically designed to force bowing to be a negative addition of reactivity
5. Loss of administrative control	Credible	Interlock built into control system to prevent any disregard of startup procedures; fully automatic initiation of scram.
<i>C. Full-power Mishaps</i>		
1. Failure of power		
a. Main power	Unlikely	Have flywheels on pumps; emergency power; scram reactor. Each pump supplied by a separate power supply
b. Main power and emergency power	Unlikely	Battery power; scram reactor
c. Main power, emergency power, and battery power	Incredible	Scram reactor
d. Main, emergency, and battery power—control rods fail to scram	Incredible	Would lead to core meltdown
2. Failure of pumps		
a. Loss of primary flow	Unlikely	Loss-of-flow scram; auxiliary pump
b. Loss of secondary flow	Credible	Loss-of-flow scram; auxiliary pump
3. Loss of coolant		
a. Gross failure of vessel	Unlikely	Double containment prevents uncovering of core; emergency sodium-injection system
b. Pipe break in primary system	Unlikely	Auxiliary makeup-system leakage from secondary to primary; double-containment piping; leak detectors
c. Leaks in intermediate heat exchanger	Credible	Primary-system high-coolant-level scram
d. Leaks in sodium-steam generator	Credible	Have steam generator behind retaining wall so Na-H ₂ O reaction does not damage primary system; scram reactor
e. Local coolant blockage due to oxide buildup, mechanical blockage of flow passages, gross swelling of fuel elements, etc.	Credible	ΔP detectors to scram core; have cleanup systems to remove oxide; have screens in inlet pipes to stop debris from any failed components
f. Pipe break in secondary system	Unlikely	Leak detectors scram reactor
g. Loss of coolant through boiling after large power excursion	Credible	Overpower scram of fast-acting control rods

TABLE C-I.—Continued.

Accidents	Feasibility	Preliminary Engineering Safeguards
<i>D. Unplanned Changes in Core Makeup</i>		
1. Control-rod expulsion	Unlikely	Limit worths of rods; have standby safety rods
2. Fuel slumping within clad	Credible	Would only occur with large-scale fuel melting; use carbide rather than oxide fuel to operate far from fuel melting point.
3. Control rod rails to scram	Credible	Control-rod mechanisms independent of each other; have enough extra control-rod worth that one rod could fail to scram with no consequences
4. Changing control-rod position coupled with loss of administrative control	Credible	Limit worth and rate of travel of rod; checks and balances to minimize neglect of operating procedures
<i>E. Sodium Spills and Fires</i>		
1. In and around reactor vessel	Unlikely	Eliminate hydrogenous material; have emergency inert gas system for smothering purposes
2. Elsewhere	Credible	Keep potential hazards (steam generators) in separate buildings or behind retaining walls; design containment to withstand theoretical maximum temperature and pressures generated in sodium fire in order to insure core safety

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Safety Characteristics of Large Gas-cooled Fast Power Reactors*

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1. INTRODUCTION

Gas cooling for fast reactors was first studied seriously by General Atomic some four years ago. Initially, the main emphasis was on broadly based surveys aimed at assessing the general attractiveness of the idea, as judged not only by likely capital and fuel costs, but also by the operational, maintenance, and safety characteristics indicated.

This work involved investigation of a wide range of applications within the general field of economic production of electric power. Thus, the objective was more than just the design of a reactor to suit a set of arbitrary requirements, but was extended to include finding the particular applications most suited to the special characteristics that gas cooling provided.

By way of example, it was found that the system looked particularly favorable for very large mixed Pu-U²³⁸ oxide- or carbide-fueled reactors of relatively low enrichment, and therefore having a high internal conversion ratio.

As a culmination of this phase of the work, an approximately 1000-MWt reference design was formulated as a basis for subsequent study including, in particular, much more specific investigation of safety aspects, which is the main concern of the present conference.

It is of particular interest to note that in the course of the studies of gas-cooled fast reactors so far completed, questions related to safety have, somewhat surprisingly, turned out to be primarily responsible for many of the attractive features of the system.

Insofar as early neglect of gases as contenders for fast reactor cooling was, as much as anything, due to worries about the safety of relying on a high-pressure coolant with a negligible capability of natural-convection cooling, this would indeed seem to represent an astonishing reorientation of thought.

It is therefore desirable at the outset to say some-

thing about how these second thoughts on gas cooling could arise.

Fundamentally, the source of the major favorable claims made for the use of a gas as coolant for fast reactors stems from its very small interaction with the nuclear performance. Although this interaction with a liquid metal coolant like sodium is not at first glance a large one, consequences arise from it that can profoundly affect not only safety matters but, because of design compromises that this aspect entails, can also greatly modify the overall performance obtainable. This magnification of the possible consequences of small effects is characteristic of the whole nuclear business and arises from the fact that the satisfactory operation of any reactor vitally depends on the influence of second-order effects.

In the case of fast reactors which have to rely on differential thermal expansion and Doppler coefficients for their stability, and which characteristically have little heat capacity in their very high-power density cores, this leads to great importance being attached to even small reactivity changes that might arise, i.e., from local or complete expulsion of coolant.

At this time, one can make a fundamental point for gas cooling, namely, holes in a gas, hence, local voids at the core center (which are generally the worst worry), are impossible. As will be shown later, even the total loss of coolant (with He or CO₂) leads to less than a dollar's increase in reactivity. To achieve a similar situation with a liquid coolant requires that the influence of the coolant on core neutron leakage terms be high enough to offset sufficiently the opposed effects on spectral-shift terms, or at least that a sufficiently high Doppler effects be retained to override the net deficiency. The trouble with putting things right this way is that in large systems performance can be seriously compromised and, furthermore, new safety difficulties can be created.

Any deliberate increase in core leakage drops the available internal conversion ratio, which in turn forces either employment of a high reactivity reserve for burnup, or much shortened refueling inter-

* Work performed under Contract AT(04-3)-167, Project Agreement 23, between U. S. Atomic Energy Commission and General Atomic.

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vals. The former is fundamentally less safe (and wasteful of neutrons), and the latter is operationally undesirable and can, in fact, lead to a material increase in chances of involuntary down-time extension.

If deliberate spectrum softening must be resorted to in order to blanket the troubles with enough Doppler coefficient, then sacrifices of overall conversion ratio are involved which can be quite severe to a plutonium system.

That these factors are very real and important can perhaps be best illustrated by pointing out that overall conversion ratios as low as 1.2, refueling requirements several times a year, and burnup-reactivity reserves of several dollars have been mentioned in recent published studies of large sodium systems. These figures may be compared with the combination of 1.5 overall conversion ratio, with no need for partial refueling at all for less than a dollar's reactivity swing that is contemplated for the equivalent gas-cooled systems. This low invested excess reactivity constitutes, in fact, a most important fundamental safety advantage which is reflected in the calculated consequences of a number of accident situations.

Although it is not suggested that all sodium systems will always have to live with the severity of the compromises implied by the figures just mentioned, they do illustrate the degree to which safety ground rules can influence the overall performance of fast reactors, and that this influence is far stronger in the case of liquid metal coolants than it is with gases such as CO_2 or He.

Before leaving the subject of coolant-nuclear interaction, a quite fundamental, but little appreciated, point in favor of gas coolants may be given, which turns out to be very important in the proper assessment of the significance of void coefficients. It is that whereas a liquid can be expelled from a reactor core very rapidly (by local boiling or gas entrainment), gases take much more time, for the entire primary circuit volume is involved in the associated time constants, and not just that of the relatively insignificant core contents. As a result, substantial coolant loss from the core region within the time normally required to detect this loss and to complete scram operations is impossible, barring a complete containment explosion. Thus, the reactivity worth of gas coolants is of much less significance than is the case with liquids, in addition to being of characteristically much lower magnitude.

In discussing only the nuclear consequences of coolant loss, we have thus far successfully avoided mentioning the main worry about gas cooling, which is concerned with the effect of a substantial loss in its heat transfer capabilities by a loss of pressure.

This is a point at which progressing development of gas-cooled thermal reactors has greatly modified earlier opinion by offering new techniques for reliable pressure containment, specifically, the notion of total containment of the primary circuit by a single, pre-stressed-concrete vessel. This completely changes ideas on containment integrity, fundamentally on account of the extreme degree of redundancy of the vessel structure, which limits local fault propagation, and because the strength members are outside the radiation- and temperature-influenced field.

It is indeed characteristic of this type of vessel to fail by local liner cracking, thus allowing a modest initial leak, followed by a measure of self-healing, to the extent that the loss of pressure leaves surplus force in the tensioning cables to close up the cracks in the system. Fracture of a main external cooling duct is, of course, eliminated as a case to be handled.

This type of containment has therefore been accepted as essential to the conception of the gas-cooled fast-reactor system, and the loss-of-coolant analysis with which we will deal more specifically later, has been directed toward an inquiry as to what rate of pressure loss is tolerable, without core damage, rather than toward looking at what postulated calamities might befall the integrity of the primary loop.

One further important general principle relating to the safety philosophy of the particular reactor system discussed here needs mention before passing to the more specific questions, and that concerns the means for dealing with steam or water flooding of the core, for fracture of a boiler tube is clearly a situation to be reckoned with.

Basically, the method adopted to ensure a continuous decrease in reactivity on water-steam introduction is to incorporate suitable resonance absorbers permanently in the fuel. Thanks to the normally hard spectrum, these have very little effect on normal operation, but progressively shut down the reactor as the spectrum is softened by water introduction. Having thus catered to water flooding, we gain the by no means insignificant side benefit of being able to flood the core deliberately to permit visible salvage operations, and to take care of long-term shutdown heat using only natural convection. The ability, in fact, to enlist the aid of regular faucet water when in trouble or when facing ultimate catastrophes must surely rank as a very reassuring feature.

Having thus briefly outlined some of the main safety aspects of the design philosophy of the system, we will now pass to the results of more specific analysis.

To clarify this, we will briefly describe the princi-

TABLE 1. DETAILED SPECIFICATIONS FOR REFERENCE DESIGN
(Gadolinium resonance absorber)

<i>Prescribed Operating Conditions</i>		Coolant Void Fraction (in Boxes)	0.435
Coolant	Helium	Active Core Length	116.5 cm
Ambient Pressure	1000 lb/in. ² abs	Fission-product Void Length	95.25 cm
Entry Temperature	300°C	Axial Blanket Length (Each End)	46 cm
Outlet Temperature	600°C	Total Fuel-element Length	266.5 cm (8.73 ft)
Maximum Surface Temperature	660°C (800°C with hot-spot allowance)	Effective Core Diameter	233 cm
Pumping Power	2.5% of core heat output	Fraction of Total Can Roughened	0.27
Internal Conversion Ratio	1.04		
Radial Max/Mean Flux	1.20	<i>Performance</i>	
Axial "Reflector Saving" Length	20 cm	Rating	0.62 MWt/kg fissile
Fission-Gas Max Pressure	1000 lb/in. ²	Total Power	1093 MWt (7½% in blankets)
<i>Prescribed Mechanical Features</i>		Power Density	218.6 kWt/liter (core volume)
Core Volume	5000 liters	Pressure Drop	32.6 lb/in. ²
Core L/D Ratio	0.50	Exit Velocity Head	4.6 lb/in. ²
Number of Rods per Box	100	Maximum Linear Rating	18.9 kW/ft
Axial Blanket Length	46 cm (each)	Overall Conversion Ratio	1.50
Can Thickness (Adequate for 1000-lb/in. ² Pressure Differential)	3% of diameter	Net Electric Power	420 MW
Can Material	Hastelloy	<i>Nuclear Properties</i>	
Fuel	Mixed U + Pu oxides (90% density)	Doppler Coefficient	$T(\partial k/\partial T) = -0.006418$
	$\left\{ \begin{array}{ll} \text{U}^{235} & 3 \\ \text{U}^{238} & 816 \\ \text{Pu}^{239} & 78 \\ \text{Pu}^{240} & 13 \\ \text{Pu}^{241} & 8 \\ \text{Pu}^{242} & 1 \\ \text{Gd (natural)} & 35 \end{array} \right\} 100$	Coolant Coefficient	$\partial k/\partial T = 1.8 \times 10^{-6} \text{ } ^\circ\text{K} \text{ (at } 700^\circ\text{K)}$
Atom Ratio		Clad Temp Coefficient	$\partial k/\partial T = -2.3 \times 10^{-6} \text{ } ^\circ\text{K}$
		Delayed-neutron Fraction	0.0044
		Neutron Lifetime	$4.37 \times 10^{-7} \text{ sec}$
Hole Diam/Fuel Diam	0.20	Core Expansion Coefficient from Axial Expansion	$v(\partial k/\partial v) = -0.14$ ($v \equiv$ core volume)
Box Wall Thickness	1/8 in.	Core Expansion Coefficient from Radial Expansion	$v(\partial k/\partial v) = 0.24$ ($v \equiv$ core volume)
Box Spacing	1/32 in.	<i>Thermal Properties Used</i>	
Type of Roughening	Such as to double heat transfer for a threefold increase in friction; $K_H = 0.0053$	Helium Velocity	375 ft/sec (avg)
Number of Control Elements	29	Helium Specific Heat	1.2425 Btu/lb°F
<i>Derived Structural Dimensions and Compositions</i>		Heat Transfer Coefficient (Rough)	1880 Btu/hr-ft ² /°F (avg)
Fissile Loading	1770 kg	Heat Transfer Coefficient (Smooth)	404 T_{film} Btu/hr-ft ² /°F (avg)
Fuel-element OD (Active and Blanket Sections)	1.114 cm	Fuel Specific Heat	$(3.3 \times 10^{-6} T_R + 0.07) \text{ Btu/lb}^\circ\text{F}$ with T_R in °R
Fuel-element Pitch (Square, in Boxes)	1.313 cm	Fuel Thermal Conductivity	0.03 W/cm-°C
Box Internal Dimensions	13.13 cm on side	Fuel Linear Expansion Coefficient	$10.5 \times 10^{-6} \text{ (cm/cm-}^\circ\text{C)}$
Number of Rods per Box	100	Clad Specific Heat	$(6.173 \times 10^{-5} T_R + 0.0358) \text{ Btu/lb-}^\circ\text{F}$
Number of Boxes Containing Fuel Rods	213	Clad Thermal Conductivity	$(0.0048 T_R + 3.432) \text{ Btu/hr-ft}^2/\text{ft}^\circ\text{F}$
Total Fuel Rods	21,300	Clad Linear Expansion Coefficient	$1.67 \times 10^{-5} \text{ (cm/cm-}^\circ\text{C)}$
Fuel Volume Fraction (Oxide Only)	0.415		

pal mechanical features of a reference design considered typical of the system, and to which the subsequent analysis specifically refers.

2. REFERENCE DESIGN FEATURES

Details of the reference design which formed the basis for the present work have been given in detail.⁽¹⁾ We will only mention enough of the leading features of this system to clarify what follows, particular emphasis being on items that concern safety. The leading dimensions and performance of the reference design are summarized in Table 1. A diagrammatic representation of the core disposition is shown in Fig. 1.

As shown in Fig. 2, the entire reactor primary system—comprising a 5000-liter core with a surrounding blanket, circulators, steam generators, and associated main coolant ducting, as well as the control drive mechanisms—is housed in a simple, cylindrical, prestressed-concrete vessel. The vessel is illustrated as being housed in a concrete-lined shaft, the top face being at ground level, but this is not regarded in any way as a general necessity.

Coolant flow is downward through a 3½-ft-deep top grid plate (which is the only core constraint) to the core region, and then upward via multiple ducts, and through an internal concrete shield to the steam-generation region located above. Apart from keeping the control mechanism and core-structure location planes close together and uniformly well cooled, this arrangement has the safety feature that in the event of meltdown, fuel would be expelled by both coolant flow and gravity in the direction of the hottest end of the system. Thus, not only would the melt not have to travel so far, but the possibility of subsequent freezing within the core is reduced greatly (a major source of concern in analysis of such accidents). The purpose of the internal concrete shield is to allow access, under shutdown or low-power conditions, to the steam-generator tube banks and control drive mechanisms, and to reduce the activation of these components during high-power operation. Bottom loading is employed, primarily to avoid having to pass fuel elements through the top grid plate, which would have introduced considerable waste space between fuel

FIG. 1. Core Disposition.

FIG. 2. Engineering Drawing of Primary System.

boxes, and which early analysis had shown to be particularly detrimental to performance. A removable internal fuel-transfer machine is employed; the only feature of this, however, that directly concerns the present safety aspects is that it allows dry reloading at atmospheric pressure, by incorporating a means for continuous cooling of the extracted fuel elements, derived from a tapping from the main circulator outlet. Although quite small local pumping power is

involved (some $\frac{1}{3}$ hp), without this, fuel limiting temperatures would be met after only about 3 min after removal from the core environment, even four days after shutdown.

Although dry reloading is thus catered for, performing the operation under water, after deliberate core flooding for the purpose, is being seriously considered as an alternative having much attraction. Notably there is completely eliminated the need for

primary-circuit forced-cooling circulation during the operation, and generally there is provided a reassuring protection during the time of open access to the reactor vessel.

The steam generators comprise eight independent units, each of which incorporates once-through evaporator economizer, superheater, and re-superheater sections.

The re-superheater is used in this instance because an external steam-to-steam heat exchanger is proposed for reheat. This arrangement considerably reduces the bulk of the steam generator to be housed within the pressure vessel, as it avoids having to accommodate low-pressure steam there. In the present application, this reduction is useful because the core power density is so high in fast reactors that the vessel size is almost entirely determined by the steam-generator bulk. Further advantages of this arrangement are that it keeps all steam pressures above that of the coolant and reduces necessary penetration sizes; both are factors of concern in limiting the maximum credible rate of coolant loss.

A further important safety consideration is the provision of a large free space below the core, into which, in the event of meltdown, the fuel could pass without the impediment of a lower grid plate. A conical splitter at the bottom of this space is incorporated to avoid reforming a critical assembly.

The circulators are driven by single-stage steam turbines, each passing the full steam flow from a pair of boiler units. This arrangement has the merit of readily permitting design for 50% overspeed continuous operation under depressurized conditions.

This design concept was evolved for General Atomic's large HTGR's, where prototype development has been initiated on a machine that has much in common with the machine needed for the present concept. The circulators are located in the top of the main pressure vessel and are under coolant pressure internally and externally, their casings thus being relieved of sustained high pressures. Access is obtained by removal of a steel pressure dome and a concrete shield plug beneath, allowing complete removal of the circulator units.

The pressure vessel is of the prestressed-concrete type and is cylindrical in shape with flat ends. The tension system proposed consists of a simple arrangement of separate axial and circumferential cables, capable of independent adjustment and continuous strain measurement.

The pressure vessel incorporates an inner steel lining faced with heat insulation, and immediately adjacent built-in water cooling coils.

2.1. Fuel Elements

The reference design is based on simple round-rod elements, incorporating core and axial blanket zones, together with a voided extension to limit fission-gas-pressure buildup.

This approach is particularly well adapted to use with high-pressure coolants, since the fuel rods can be designed to limit fission-gas pressure (even with total release) to less than that of the coolant. Under these circumstances, the gas containment is obtained at no penalty in can thickness, since the latter must be sufficient to deal with the possibility of pressure equalization resulting from small in-leaks of coolant, followed by depressurization.

The fuel pellets will be operated up to a maximum burnup of 100,000 MWd/tonne. Taking a fission yield of stable rare gases of 0.2 atom per atom fissioned, and assuming all the gas to be released from the fuel to a space at the top of the fuel rod provided for its storage, the volume of reservoir required for 1000-lb/in.² pressure at a storage temperature of 300°C (core inlet temperature) is approximately one-half the fuel volume for an axial max/mean power distribution of 1.3.

2.1.1. Fuel-rod Design

The fuel rod for the reference design consists of a Hastelloy-X can, ~0.44-in. OD and 12.5 mils thick, sized to accommodate 46 in. of core and 18 in. of blanket above and below the core. Continuous with this tubing is an extension, 37.5 in. long, which is used as a fission-product storage reservoir. This extension is swaged down to about 80% of the normal diameter to reduce pressure drop. The core and axial blanket pellets are 0.50 in. long and smaller than the internal diameter of the can by the amount necessary for ease of assembly; this amount depends on the tolerances in can diameter. The pellets have a small central hole, the prime purpose of which is to facilitate the free escape of fission gas to the end void space in the event of a local melt, thus avoiding development of excessive local pressures. The fission-product storage reservoir is at the top (gas entry) end and is fitted with an end cap which attaches to an upper support in the fuel-element box. The lower end is capped, but not confined in the axial position. Approximately one-half the lower outlet of the active rod length carries surface roughening of the same type as used in the British AGR reactor.

2.2. Fuel Boxes and Assembly

The concept is based on bundles of fuel rods which can be separately orificed for coolant flow and which

are contained in a box which acts as a support and flow barrier. Square-section fuel boxes are employed, the flat walls of which could have to withstand a pressure differential of the order of 30 lb/in.², the coolant pressure drop. A typical fuel-element assembly is shown in Fig. 3. The dimensional requirements dictated by nuclear, thermal, and hydrodynamic design are shown in Fig. 4.

To avoid the large pressure differential, the box interspaces are open at both ends, so that pressures are approximately equal inside and outside the fuel boxes along their whole length. This, of course, allows some coolant bypass, but the amount in our case is little over 1%, owing to the close box spacing ($\frac{1}{32}$ -in. gaps) allowed by the bottom-loaded core design.

The fuel rods are attached to an internal support grid at the top of the fuel element. This support is in turn welded to the box structure inside the reduced box section. The top cap on the fuel rods is slotted to fit the support grid. This attachment is the only means of confining the rods in the longitudinal direction. The rest of the element is permitted to expand longitudinally under an increase in temperature and to slide within the spacing grids. This arrangement allows complete ejection of a partly melted fuel bundle under the combined influence of gravity and coolant pressure drop.

The fuel-element boxes are held in the grid plate by a nut which engages a threaded shaft attached to the top fitting of the box. The conical surface at the bottom of the nut and the beveled lower face of the grid plate provide, in effect, two cones located some 42 in. apart, which lock the fuel boxes in position and locate them with respect to the grid plate. The box retention nuts are operable from the top face of the internal shield plug by means of extension wrenches built into the latter.

2.3. Control Rods and Drives

Control is maintained by boron (in carbide form) in round steel cans operated in guide tubes located in specially adapted fuel boxes.

Drive is by direct rod-to-mechanisms located in the upper shielded compartment of the pressure containment.

These mechanisms employ a synchronous motor-operated ball screw for normal operation, with direct linear pneumatic actuation for scram, with hydraulic snubbing.

Followers are not used, since the downward extension of the rod is limited to the confines of the core by closed ends in the guide tubes, which retain the detached rods.

FIG. 4. Fuel-box Proportions.

3. ANALYSIS OF SELECTED ACCIDENTS

3.1. Consequences of Coolant Loss

Perhaps the most immediately obvious safety problem associated with use of a high-pressure gas as a reactor coolant is the possible effects of its inadvertent loss.

We are not here concerned with the credibility of the leak rates indicated, but are only interested in this context in what the core could stand without damage.

In the present concept, the situation is eased by the following design features:

1. integral prestressed-concrete containment, eliminating external ducts or component casing failures as a source of rapid leak;
2. use of a core designed to allow water flooding to deal with long-term shutdown heat removal by natural convection;
3. use of multiple circulators (driven independently of external power supply) capable of 50% overspeed at low pressure;
4. availability of boiler sections as heat sinks already incorporated within the pressure vessel.

The problem to be dealt with quantitatively here is the estimation of the core-temperature history consequent of arbitrary coolant pressure leak rates, including modification of the results by application of different corrective measures—in particular, allowing

circulator overspeed and the cutting out of feed heating after an appropriate interval.

3.1.1. Pressure Decay

Assuming isothermal initial reservoir conditions and neglecting viscous effects, it can be shown that the pressure in a container having a fixed-area leak decays exponentially to a point where the pressure ratio across the leak drops to a critical value (2.05 for helium). During this period, container pressure is given by

$$P = P_0 e^{-(t/t_c)},$$

where P_0 is the initial absolute pressure, t the time, and t_c a time constant, related (in the assumed ideal conditions) to the leak area A and the initial container volume V by

$$t_c = \frac{V}{A} \left[\frac{M}{RT\gamma} \left(\frac{y+1}{2} \right)^{(y+1)/(\gamma-1)} \right]^{1/2}.$$

In the latter equation M is the coolant molecular weight, R the gas constant, T the absolute initial temperature, and γ the ratio of specific heats. For helium,

$$t_c = 0.302 \left(\frac{V}{A\sqrt{T}} \right) \text{sec}, \quad (1)$$

where V is in litres, A is in cm^2 , and T is in $^\circ\text{K}$.

As a point of interest, the time constant for CO_2 would be ~ 3.5 times larger, allowing even more time for corrective action after the same accident. After the critical pressure ratio across the leak is reached (which takes $3\frac{1}{2}$ time constants), the relationships are much more complicated, but fortunately the flow equations can be integrated for the particular value of γ for helium, yielding the relation

$$\left(\frac{t'}{t_c} \right) = \frac{5\sqrt{3}}{16} \left[\frac{10}{3\sqrt{3}} - (p_R^{2/5} - 1)^{1/2} (p_R^{2/5} + 2) \right],$$

where p_R is the ratio of container pressure to atmospheric pressure at time interval t' , after the moment of reaching the critical pressure; t_c is the same time constant as before.

Putting $p_R = 1.0$ in the above equation, we find the rather surprising result that the time needed to equalize pressure is finite and equal to $(25/24)$ time constants after the critical point is reached, or approximately $4\frac{1}{2}$ time constants after the initial leak starts.

Plotting the above functions has shown that, in practice, continuing the initial, simple, exponential

pressure decay right down to atmospheric pressure, and thereafter keeping it constant, is a good enough approximation for analysis of the effect of coolant loss. This approximation was, in fact, used in computing what follows.

To preserve generality (and because total containment volume is not yet a settled quantity), it is preferable to work in terms of arbitrary time constants rather than in equivalent leak areas, but the relation between the two can be obtained in any particular case from Eq. (1). The containment shown in the illustrations of the reference design has an internal volume of some 30,000 ft^3 , which would give a time constant of about 12 sec for an effective 1- ft^2 hole at the reactor intake. This gives some idea of the quantities involved.

It is emphasized that the hole area referred to here is an ideal one, approximated only by a carefully designed nozzle. In practice, the same area, spread out in the form of long cracks, for instance, would have many times less flow rate owing to the various effects that have been ignored. Even a single unobstructed near-circular orifice would only permit about half this flow rate because of flow-distribution irregularity.

In calculating core temperatures during and subsequent to coolant leakage, further assumptions are necessary concerning the heat rejection (via the boiler) during the period. The assumption made is that reactor inlet temperature remains constant for any one case. Owing to the close approach temperature that an economic steam system would require, this assumption is considered justified. The effect of feed-heating removal is taken to be an immediate reduction of reactor gas inlet temperature by approximately 350°F from its normal value.

3.1.2. Power after Shutdown

The mathematical model used for afterheat generation following reactor shutdown is

$$P = At^{-B},$$

where P is the percent of initial operating power, t is time in seconds, and the constants A and B change over different time intervals in accordance with the following:

Time Interval (sec)	A	B
1.0 to 10	12.05	0.0639
10 to 150	15.31	0.1807
150 to 4×10^6	26.02	0.2834
4×10^6 to 2×10^8	53.18	0.335

3.1.3. Scram Time Delay

From the leak rates considered, the behavior of the system is relatively insensitive to time delay before the reactor is shut down. The time delay used was 0.5 sec, which is pessimistic considering the magnitude of the initiating phenomena.

3.1.4. Normal Corrective Measures

The steam-driven turbine circulators are capable of 50% overspeed under reduced pressure conditions and represent a readily available means for improving coolant flow. A conservative 0.5-min time delay for this action is used in the analysis. Another normally available means of improving the cooling is the reduction of core inlet temperature, which may be accomplished by bypassing the feedwater heaters in the steam plant. For the purpose of this study, it is assumed that the inlet temperature can be reduced by $\sim 350^{\circ}\text{F}$ in the 0.5-min time delay.

3.1.5. Heat Transfer Analysis

The heat transfer under transient conditions was calculated by means of the RAT code, which permits the subdivision of the fuel element under consideration into many pieces, both radially and axially, and follows the changes in temperature in all pieces during the course of the transient. The code can be used for a variable heat transfer condition along the fuel element, and the rough and smooth sections of the reactor element were properly considered.

3.1.6. Results of Analysis

The most important effect of a loss-of-coolant accident is the rise in cladding temperature, since the cladding must be kept appreciably below the melting point to prevent a failure. The reference cladding material is Hastelloy-X, which has a melting point of 2350°F . Figure 5 shows a history of the maximum cladding temperature with a pressure decay constant of 0.5 min for the case of (1) no corrective action, (2) a reduction of inlet temperature by $\sim 350^{\circ}\text{F}$, (3) an overspeed of 50%, and (4) both (2) and (3). The full corrective action of (4) limits the temperature to a maximum of 1976°F , which is just about equal to the acceptable limit. It should be noted that the low thermal capacity of the fuel element yields an immediate and rapid drop in the cladding temperature after the reactor is scrammed. It is interesting to note here that the core heat capacity is, in fact, so low that the stored heat in the fuel really constitutes no problem—it is all dumped to coolant long before pressure could fall far and while there is still a good heat transfer. It is the nuclear afterheat which constitutes the essential difficulty, but the effects of this are sufficiently slow in building up as to allow a quite reasonable delay in effecting preventive measures.

This situation can be reckoned as one useful by-product of having, for other reasons, to employ so large a pressure vessel volume in relation to that of the core alone.

FIG. 5. Maximum Cladding Temperature Histories for Various Combinations of Corrective Actions on Inlet Temperature and Circulator Speed.

FIG. 6. Maximum Fuel and Maximum Cladding Temperatures as a Function of Time.

Figures 6 through 9 show the temperatures at other points in the fuel element for case (4). Figure 10 shows that a leak of twice this rate would not lead to a melting of the cladding. If the only corrective action taken is the overspeed of the circulators, the maximum cladding temperature is as shown in Fig. 11 for various leak rates. This figure shows that without this corrective measure, the same temperatures would be reached for a leak rate of 25% of the magnitude considered in the detailed study.

Thus, it would appear that with scram initiated within one-half second of the start of trouble, it is possible to deal with a leak that would halve the pressure in as short a time as about 20 sec with the normal cooling arrangements, and without can failure.

FIG. 7. Radial Temperature Profiles at Point of Maximum Cladding Temperature for Various Times after Beginning of Accident.

FIG. 8. Maximum Cladding Temperature Axial Profiles at Various Times after Beginning of Accident.

This requires the remedial measures of cutting off the feed-water heating within a half minute, and an overspeed allowance on the blowers. The power demand of the latter is quite low. The ambient pressure would be nearly atmospheric by the time the speed increase is required.

3.2. Reactivity Changes Caused by Introduction of Water into the Core

Several possible accident or operational situations require analysis of the reactivity change consequent to the introduction of water or steam into the coolant channels of a GCFR: first, the rupture of a steam

generator tube; second, the availability and physical properties of water make it a logical choice for emergency coolant should the mass flow rate of helium be severely reduced; and third, the performance of refueling operations with a completely flooded core is being seriously considered, either as an emergency substitute for dry refueling or as a standard operational procedure.

In connection with these measures, the reactivity change due to water entry has been studied for all

possible concentrations of water in the coolant channels and these results are summarized in Fig. 12. The top curve is for GCFR fuel with no additional parasitic absorber and shows the important characteristics that small amounts of water give negative reactivity insertions. However, the reactivity becomes positive after 10% flooding and increases to 11% in reactivity at the fully flooded condition (for the case of unpoisoned fuel, unpoisoned water). In all calculations, it was assumed that the flooding was with cold water and that all control rods were out.

In reality, a boiler-tube rupture would introduce steam rather than cold water into the core. In Fig. 12, this accident would correspond to a very small percentage of the flooded condition. For example, an upper boundary on the steam density is 2.25 lb/ft³, corresponding to the saturation point for 1000 psi. This maximum steam density corresponds to a 3.6% flooded condition in Fig. 12, well within the negative-reactivity area.

Therefore, in the event of ruptured steam-generator tubes, the reactor would inherently shut down for any quantity of steam entry and without the incorporation of additional absorbent material in the core.

However, situations of emergency core cooling with water or underwater refueling require either the introduction of additional poison or an ensured scram of a large number of control rods (critical volumes are small with 50% water and 10% enriched fuel). As shown in Fig. 12, if approximately 3×10^{20} atoms of gadolinium are incorporated per cubic centimeter of core volume, the multiplication factor then decreases for any degree of water content. This concentration corresponds to approximately one atom of gadolinium for every three fissile atoms.

FIG. 9. Maximum Fuel Temperature Axial Profiles at Various Times after Beginning of Accident.

FIG. 10. Maximum Cladding Temperature Histories for Various Pressure-decay Constants.

FIG. 11. Maximum Cladding Temperature Histories for Various Pressure decay Constants.

FIG. 12. Effective Multiplication as a Function of Percent of Helium Space Flooded.

We are considering incorporating this quantity of gadolinium, in oxide form, in the fuel itself (which was the assumption for the calculations of Fig. 12), since it appears that the loss of conversion ratio is small (approximately 0.05), and the measure enables the use of unpoisoned water for flooding. The alternative possibility of incorporating poisons in the water is also being considered, either as a substitute for or as a supplement to fuel poisoning (which would decrease or eliminate any loss in conversion ratio at operating condition).

In summary, this analysis was of a generally conservative nature, in that no scram was assumed, all poison was included in the fuel material, relatively low (13%) concentrations of Pu^{240} were assumed for the reference fuel (the poison requirements are sensitive to this value), and a single, albeit effective, element was allowed as poison material. It can be concluded that the reactivity can indeed be caused to decrease for any degree of water flooding in a GCFR with a minimal penalty to the neutron economy at operating conditions.

3.3 Analysis of Control-rod Withdrawal

3.3.1. Analytical Methods Employed

The analysis of various step and ramp transients was carried out with the use of a modified version of the BLOOST code, consisting of a group of kinetics subroutines which solve the lumped-parameter kinetics equations coupled with a two-dimensional heat-transfer calculation for a fuel element and associated coolant channel. Six groups of delayed neutrons are included. The heat-transfer routines provide the local and average values of cladding, fuel, and coolant temperatures which are used to compute feedback to the kinetics subroutines. The reactivity contribution due to local fuel temperature change may be weighted pointwise by an arbitrary weighting function so as to give the properly averaged Doppler reactivity. In the cases considered in this report, the product of the flux and adjoint flux was used as the weighting function.

A detailed temperature map of the element provided at each time step makes possible observation of hot spots, local melting, etc. Provision is made for

arbitrary reactivity insertion and for scrams triggered by preset power levels after a specified delay time.

3.3.2. Rod Withdrawal from Hot Operating Conditions

3.3.2.1. Description of the Accident. The reactor is considered to be at hot operating conditions when the maximum fuel temperature in the hottest rod is approximately 2200°C and maximum clad temperature in the hottest rod is approximately 680°C . One dollar of excess reactivity is available and is held down by three control rods, with each rod holding down approximately $0.00148k$ (less than one dollar of reactivity is required for burnup). It is presumed that one rod is withdrawn at the rate of 10 in./min. The scram is assumed to occur at 120% of full power.

3.3.2.2. Results of the Accident. Figures 13 and 14 show the changes of temperature and reactivity as a function of time during the course of the accident. The reactivity insertion is almost linear. As may be observed from Fig. 13, the feedback reactivity from Doppler and core-expansion effects follows the accident reactivity closely, and the two effects are in a quasi-equilibrium state after about 10 sec.

In Fig. 14, the temperature is plotted as a function of time. There is a temperature rise at the maximum temperature point in the hottest element of 320°C . The peak temperature of 2520°C is below

the melting point of the oxide fuel. The average fuel temperature in the hottest element also shows a small rise in temperature, in this case only 160°C . The cladding temperature increases to a maximum of 760°C on the inner surface of the can, which is well within the temperature limitation of Hastelloy-X cladding material.

It should be noted that the time needed to reach 120% of full power, at which point the scram is initiated, is almost 65 sec, with the maximum period being 130 sec. It is unlikely that an excursion of this nature would be permitted to proceed this far under normal operating conditions.

In summary, control-rod withdrawal from the hot operating condition causes no damage, even if the accident is allowed to proceed until power has reached 120% of the normal value. The resulting temperature rises are more sensitive to the fractional overpower setting for the scram signal than to the rate of rod withdrawal, owing to the relatively long periods involved which allow the temperature to come into equilibrium with the power level.

3.3.3. Rod Withdrawal from Cold Critical Condition

3.3.3.1. Description of the Accident. In this case, the reactor was assumed to be operating at 1 kWt and at a uniform temperature of 300°C . Excess re-

FIG. 13. Feedback and Net Reactivity as a Function of Time Up to the Beginning of Scram for Rod Withdrawal from Hot Operating Conditions.

FIG. 14. Maximum Fuel Temperature, Average Fuel Temperature, and Maximum Cladding Temperature as a Function of Time for Rod Withdrawal from Hot Operating Conditions.

FIG. 15. Accident, Feedback, and Net Reactivity as a Function of Time for Cold Startup Accident.

activity in the core included one dollar's burnup reactivity plus approximately two dollar's burnup reactivity in the cold to hot swing. This reactivity was being held down by 11 control rods, each inserted to the same position as were the 3 control rods in the rod-withdrawal accident. To initiate the accident, the extreme assumption is made that *all* 11 rods are simultaneously withdrawn at a rate of 10 in./min. Various scram possibilities are considered.

3.3.3.2. Results of the Study. Figures 15 through 17 describe the course of the accident. In Figure 15, the reactivity due to rod withdrawal as well as the feedback and net reactivities are shown as a function of time. As may be observed, there is no effective temperature feedback until after about 21 sec have elapsed. At this point, the rods have inserted almost one dollar and the feedback reactivity increases rapidly, then leveling off to become parallel to the accident curve. The net reactivity consequently decreases as the feedback increases and levels off to a constant value of approximately 0.001. This is con-

siderably higher than that of Fig. 13, since the insertion rate assumed for the cold startup case is eleven times the rate for the single rod withdrawal accident.

The power profile is shown in Fig. 16. The initial power overshoot is small and is soon compensated for by the temperature feedbacks. The power then increases at a rate of approximately 15 MWt/sec.

Figure 17 shows the peak temperatures in the fuel and cladding as functions of time in this accident. It is not until about 21 sec have elapsed that any change is noted. After this time, the temperatures increase rapidly. The peak temperatures achieved in the course of this accident depend very strongly on the time at which scram is initiated. If the scram is tripped during the period passing through a preset value as low as even one second, or by 50% power, the accident will be terminated prior to any sensible temperature change in the core.

A scram at 70% power would limit the fuel temperature to 1260°C and the cladding temperature to 500°C. Even if scram occurred only after 120% of

FIG. 16. Reactor Power as a Function of Time for Cold Startup Accident.

FIG. 17. Maximum Fuel and Cladding Temperature as a Function of Time for Cold Startup Accident.

full power, temperatures would remain below fuel melting points (approximately the same temperatures as those of Fig. 14).

In summary, the cold-startup accident considered here does not appear to involve consequences as severe as those yielded by rod withdrawal from hot operating conditions. The two accidents ultimately become similar and are not damaging to the core or any of its members.

3.4. Fuel-assembly Insertion at Cold Critical

The worth of the central fuel assembly has been determined to be approximately 0.4% δk when added to an otherwise fully loaded core. Assuming that the fuel-loading machine is restricted to an insertion rate of 10 in./min, then the reactivity insertion rate due to the insertion of even the central fuel element is less than that considered in the cold-startup accident. Fuel and clad temperatures for this case are clearly less than those obtained in the previous analysis of the cold-startup accident.

4. STABILITY ANALYSIS

An important part of the safety considerations for the GCFR has been the study of the inherent stability of the system as a function of such important parameters as power level and temperature-feedback

mechanisms. By inherent stability we mean stability without external control. By stability we mean, loosely, that the reactor is insensitive to small perturbations in its power level, temperatures, or the physical constants of the system. Thus after any perturbation it will remain close to or return to an equilibrium state.

4.1. Description of the Calculation Model

The usual space-independent reactor kinetics equations in the form

$$\frac{dn}{dt} = \frac{\rho(n, T_{j,k,t}) - \beta}{\Lambda} n(t) + \sum_{i=1}^6 \lambda_i C_i(t); \quad (1)$$

$$\frac{dC_i}{dt} = \frac{\beta_i}{\Lambda} n(t) - \lambda_i C_i(t) \quad (i = 1, \dots, 6) \quad (2)$$

are assumed to be sufficient.

An average fuel element and its cladding material are considered to be divided into a number of axial and radial regions. The average temperatures of these regions are assumed to be given approximately by the relationship

$$\begin{aligned} C_{j,k} \frac{dT_{j,k}}{dt} = & f_{j,k} n(t) + h_{j,k-1} [T_{j,k-1}(t) - T_{j,k}(t)] \\ & - h_{j,k} [T_{j,k}(t) - T_{j,k+1}(t)] \\ & + g_{j-1,k} [T_{j-1,k}(t) - T_{j,k}(t)] \\ & - g_{j,k} [T_{j,k}(t) - T_{j,k+1}(t)]. \end{aligned} \quad (3)$$

The outer radial regions correspond to the coolant channel, in which the temperature is assumed to be given by

$$C_{j,n} \left[\frac{dT_{j,n}}{dt} + \frac{v}{2l} (T_{j+1}(t) - T_{j-1}(t)) \right] = h_{j,n-1} [T_{j,n-1}(t) - T_{j,n}(t)]. \quad (4)$$

Appropriate boundary conditions are derived from specification of inlet coolant temperature and zero temperature gradient at the fuel centerline. Equations (1) through (4) are then rewritten in the conventional manner to give the state variables in terms of perturbations from the hot-critical equilibrium point, and the $\rho(t)n(t)$ term in Eq. (1) is linearized.

The reactivity feedback was assumed to be local from fuel, cladding, and coolant temperatures as given by

$$\rho(t) = \sum_k \sum_j W_{j,k} \delta T_{j,k}(t), \quad (5)$$

where the term $W_{j,k}$ includes the reactivity coefficient and the appropriate spatial weighting function.

The resulting set of differential equations are then of the form

$$\frac{d\bar{x}}{dt} = A\bar{x}, \quad (6)$$

where A is a matrix with constant coefficients and $\bar{x}$ is the vector

$$\bar{x} = \{\delta n(t), \delta C_1(t), \dots, \delta C_6(t), \delta T_{1,1}(t), \dots, \delta T_{m,n}(t)\}.$$

A number of different and more or less restrictive stability criteria could be cited as background for this study. Typical of these is the following criterion from Bellman:⁽²⁾ "If all solutions of Eq. (6) approach zero as $t \rightarrow \infty$, the same holds for solutions of

$$\frac{d\bar{x}}{dt} = [A + B(t)]\bar{x}, \quad (7)$$

provided that

$$\|B(t)\| \leq c_1, \quad \text{for } t \geq t_0,$$

where c_1 is a constant that depends on A ." That is, the system is asymptotically stable against the perturbation $B(t)$. For other types of perturbations other criteria may of course be preferable.

A computer routine called GASA was written to evaluate the roots of the matrix A of (6), check for the sign of the real parts, and hence determine the system stability. In addition, the routine evaluates the transfer function relating any of the variables to

an external reactivity perturbation or any other state variable.

For this study the GCFR fuel element was divided into one to five axial regions and three radial regions (fuel, cladding, and coolant). Axial heat transfer was neglected, since the fuel is in pelletized form.

Three reactivity temperature mechanisms were included in this model: the Doppler effect in the fuel, the effect due to axial expansion in the cladding, and the effect of changing density with temperature in the coolant. These temperature coefficients were considered to have the major effect on the dynamic behavior of the system.

The Doppler reactivity effect is inversely proportional to the absolute temperature and thus is nonlinear. However, for small perturbations and considering the other approximations of the model, a linear variation of reactivity feedback with temperature due to the fuel was assumed to be adequate. A flux-adjoint flux weighting of the Doppler feedback contribution from each axial zone was employed.

The important reactivity effect in the cladding was assumed to be axial expansion, which lowers the macroscopic cross sections in the region of expansion by decreasing the density. This core expansion is assumed to take place instantaneously with temperature changes in the cladding and is assumed to be independent of fuel temperature. A uniform weighting of the cladding temperature in each axial region was used to determine the reactivity contribution of that region. The main reactivity feedback mechanism in the coolant is a combination of leakage and moderation changes due to changes in density. It was assumed that the time constant for core bowing effects was long relative to coolant density effects and therefore the negative bowing feedback term was neglected. The coolant axial weighting function was calculated from the GAZE criticality code for hot operating conditions.

4.2. Results of GCFR Stability Analysis

The stability regions for the reference design GCFR were mapped as a function of the temperature-feedback coefficients by means of the GASA code. Results were obtained for values of the Doppler coefficient ranging from 10^{-8} to $10^{-5}/^\circ\text{C}$, for values of the coolant coefficient from $+10^{-7}/^\circ\text{C}$ to $+10^{-4}/^\circ\text{C}$, and for three values of the cladding coefficient, namely, 0, $-4.2 \times 10^{-7}/^\circ\text{C}$, and $-1.0 \times 10^{-6}/^\circ\text{C}$. These values more than adequately cover the range of variation in the coefficients possible for this design.

The stability regions were calculated for assumptions of one, three, and five axial zones. It was found that three are sufficient. The resulting stability map is shown in Fig. 18 for the three-zone case. The region

FIG. 18. Stability Map for GCFR.

of stability lies to the right of the curves, and it can be seen that the operating point is well within the stable region.

Since the roots of the matrix *A* are evaluated numerically to determine the stability of the system, the value of the parameters at which the real part of the largest root changes signs is not determined exactly, but only within certain limits. These limits are determined by the number of points at which the roots are evaluated and the accuracy with which the roots can be evaluated by the numerical procedure. The uncertainty associated with the curves given in Fig. 18 is about 8% of the value of the fuel and coolant temperature coefficients at each point.

The stability boundary curves for $\alpha_c = 0.0$, -4.2×10^{-7} , and -1.0×10^{-6} ($^{\circ}\text{C}$) come very close together for values of the Doppler and coolant coefficients greater than 8×10^{-6} ($^{\circ}\text{C}$) $^{-1}$. This indicates that the cladding coefficient has little effect on the size of the stability region when these coefficients are large.

4.3. Conclusions

The results of this stability study indicate that the GCFR system is stable for the design operating parameters. Moreover, the results show that the coolant coefficient would have to be made more positive by at least an order of magnitude and the Doppler coefficient reduced by an order of magnitude for the system to become unstable.

5. MAXIMUM ENERGY RELEASE STUDIES

Certain characteristics of fast reactors, such as the sensitivity of reactivity to reductions in the dimensions of the core, have focused attention on the possibility of severe accidents which would lead to an appreciable amount of energy being released with termination after vaporization of all or part of the fuel in the core. All fast reactors which have been built in the United States, or under consideration, have been subjected to such accident analysis, and a survey of the work in this area is given in Ref. (3). Analysis of severe accidents in the Fermi Fast Breeder

Reactor and the EBR-II were made for highly enriched metal-alloy fuel which gives a relatively hard spectrum and almost no Doppler coefficient. The SEFOR analysis relates to uranium oxide fuel in a relatively low-power reactor. All previous studies have been made for reactors with sodium coolant.

The intent of this work is to make use of the present information and calculational methods available to examine the case for the GCFR, with the purpose of understanding the differences in going to large cores cooled with gas, producing relatively high powers, and with strong Doppler coefficients.

5.1. Causes of Severe Accidents

The sudden release of damaging quantities of energy requires the insertion of somewhat more than a dollar in reactivity in time periods of the order of a hundredth of a second or less. In past reactor safeguards studies, such accidents have been postulated on the hypothetical events of core collapse, fuel melting followed by core compaction, and in the case of SEFOR, a lengthy chain of events ending with the rapid insertion of reactivity with an experimental transient device. The ensuing power surges in these cases were sufficient to cause pressure buildup, which then expanded the core and ended the excursions.

Before attempting any meaningful analysis, it is therefore necessary to examine any possible circumstances which might cause a prompt excursion in GCFR and estimate the rate of reactivity insertion at prompt criticality involved in this initiating accident.

5.1.1. Possible Reactivity Accidents Other Than Core Rearrangement

In the present design, the operating core has less than one dollar ($0.0044 \text{ } \delta k/k$) of excess reactivity for burnup. All the control rods are operated from the top of the core, thus eliminating a fallout. The coolant flow is downward, eliminating possible expulsion from the pressure drop. The worth of each rod is also less than one dollar. The central fuel assembly (containing 100 pins) is worth only one dollar and, moreover, cannot fall into the core since the fuel is fed from the bottom.

The friction forces due to the gas flow would cause some deflection of the grid plate. It has been calculated that this would increase the core volume by 0.04% and, therefore, would be worth only $8 \times 10^{-5} \text{ } \delta k/k$, or two cents in reactivity.

Complete loss of helium coolant is calculated to increase reactivity by thirty cents. This amount of reactivity is insufficient to cause a severe accident;

moreover, it is impossible suddenly to void the GCFR core due to the relatively large surrounding gas volume, the prestressed-concrete containment, and the limitations on the cross-sectional area of individual penetrations to the vessel. Indeed, these characteristics of gas coolant appear to offer intrinsic and significant safety improvements over liquid metals for large systems.

5.1.2. Core Collapse

Since the core is suspended from the grid plate, a major dislocation of the plate, possibly due to an earthquake or outside explosion, might cause the core to fall toward the bottom of the refueling space. Were the entire core to fall on a flat surface, it would then be compacted. Calculations indicated that large rates of reactivity insertion, of the order of \$100/sec, would be attained from such an accident.

Even though this accident is highly improbable, it is believed that special preventative measures should be taken. Therefore, we have incorporated into the GCFR design a splitting cone in the space below the core.

The central void caused by the splitting cone is sufficient to cause a net negative reactivity insertion and, therefore, to eliminate this accident possibility.

5.1.3. Core Meltdown

In previous analyses of metal-fueled reactors, it has been hypothesized that an accident situation of this type might result in central melting of fuel and subsequent refreezing in the lower core regions, followed by a gravity drop of upper fuel from which large reactivity insertions can be obtained. In the case of GCFR, the safety situation would appear to be considerably improved.

The use of prestressed-concrete containment, redundancy of circulators directly driven by steam with the possibility of 50% overspeed, as well as providing for emergency water flooding have previously been discussed for prevention of loss-of-coolant or loss-of-flow accidents. Nevertheless, should all these measures fail and the central fuel melt, the reactivity can be expected to decrease for the GCFR. The reason is as follows.

Contrary to the metal-fueled case, it could be expected that cladding adjacent to molten UO_2 fuel would promptly melt, thus leaving a very weak support structure for the fuel pins. Since there is no lower support plate in the GCFR fuel assemblies, the bottom half of the fuel elements would fall out of the core and shut down the accident situation. It is important that the initial downward motion of the

melted fuel is in the direction of decreasing reactivity worth (since the melt would occur at or below the center line).

In summary, at this stage of our investigations, we have not isolated any accidental situation which would lead to the order of reactivity insertions of \$100/sec in the current GCFR design. The study has, however, led to the specification of a splitting cone below the core to eliminate the possibility of sudden compaction of a critical mass of falling fuel.

Despite the lack of a justifying accident situation, we have proceeded to analyze the energy release which would result from arbitrarily large reactivity insertion rates.

5.2. Methods of Analysis of a Severe Accident

The various analytic methods developed to study this type of accident are offsprings of the original analysis of Bethe and Tait.⁽⁴⁾ A comprehensive survey of these methods was made by Nicholson⁽⁵⁾ and by W. J. McCarthy and D. Okrent.⁽⁶⁾ The main disadvantage of the analytic solution is that it does not allow for the Doppler correction, which can be very significant in large cores.^(5, 7) A second drawback of the analytic solution is that during a severe accident, only the central portion of the core behaves as a homogeneous mixture. The outer part of the core does not initially experience any pressure surge and should be considered as heterogeneous.

FIG. 19. Power as a Function of Time for Different Reactivity Ramps, Assuming No Disassembly.

FIG. 20. Average Internal Energy of Fuel as a Function of Time, Assuming No Disassembly.

FIG. 21. Internal Energy in Different Fuel Regions as a Function of Time.

FIG. 22. Prompt Reactivity as a Function of Time.

FIG. 23. Equivalent Step Reactivity as a Function of Ramp Insertion Rate.

In addition, we had available two machine-calculational tools, the AX-IT code⁽⁶⁾ and the BLOOST code.⁽⁸⁾ AX-IT was modified to include a Doppler contribution to reactivity. Although it could be used either for ramp insertions of reactivity (simulated by imposing inward velocities on the various spherical layers), it was found to be more accurate and to use less computer time to specify step insertions of reactivity. The step insertion, which was equivalent to the desired ramp rate, was obtained from the BLOOST code. BLOOST allows consideration of delayed neutrons, axial and radial temperature distributions in the fuel element, and spatial weighting of the Doppler reactivity contribution. AX-IT considers only prompt neutrons and spherical geometry, but per-

forms hydrodynamic calculations in addition to neutronic calculations. Pressure changes are calculated from the equation of state, energy input, and material density; acceleration of spherical shells is calculated from the pressure gradients, and reactivity from one-group transport theory.

5.2.1. Results for Ramp Insertions, Assuming No Disassembly

Figure 19 shows the power as a function of time for the reference design GCFR, as calculated from BLOOST, for ramp reactivity insertions of \$10/sec, \$50/sec, and \$100/sec. The oscillatory behavior is due to the Doppler feedback (the cladding reactivity coefficient was included but was negligible due to the

FIG. 24. Relative Energy Yield for Hottest Fuel as a Function of Step Reactivity Insertion.

prompt and, therefore, adiabatic nature of the energy release). Peaks of later power pulses can be higher or lower than the initial peaks, depending on the balance between the $1/T$ dependence of the Doppler coefficient, the axial weighting of the coefficient, and the ramp-insertion rate. The oscillations are damped by delayed-neutron effects.

The results are only physically meaningful up to the energy threshold at which significant pressures are obtained, since the outward velocity would then rapidly shut down the reaction.

Figure 20 shows the average internal energy of the fuel as a function of time for the three cases considered, and Fig. 21 shows the distribution of fuel at different internal energy levels for the \$100/sec and \$50/sec cases. It was estimated from the equation-of-state data for $\text{UO}_2\text{-PuO}_2$ that the threshold energy for pressure generation, Q^* , is approximately 300 cal/g.⁽⁹⁾

FIG. 25. Total Energy above Threshold Value as a Function of Step Reactivity Insertion.

It is seen that this level would be reached on the first pulse of a \$100/sec accident, and the second pulse for a \$50/sec ramp. As will be shown later from the AX-IT calculations, the energy release would be terminated in less than a millisecond after reaching the threshold point.

It can be seen from Fig. 21 that a relatively small fraction of the fuel will be vaporized and that this fraction will, in general, decrease as the maximum-to-average power ratio increases. This indicates that the energy release in accidents of this type is less than proportional to reactor power, since the maximum-to-average power ratio can be expected to increase for larger cores.

Figure 22 shows the prompt reactivity as a function of time for the different cases. Results from these cases are further plotted in Fig. 23 and can be used to correlate the AX-IT energy releases with the results for proper insertion rate. The prompt reac-

FIG. 26. Maximum Pressure as Function of Step Reactivity Insertion.

FIG. 27. Kinetic Energy as a Function of Step Reactivity Insertion.

Fig. 28. Local Pressures and Velocities as Functions of Time for \$600/Sec Ramp ($\delta k_p = 4.4 \times 10^{-3}$).

tivities involved are sufficiently close to zero so that delayed neutrons should not be neglected.

5.2.2. Correlation of Ramp with Equivalent Step Insertion of Reactivity

In Fig. 23 is presented the relationship between ramp and step reactivity insertions which were used for the AX-IT code. The equivalent step reactivity corresponds to the ramp insertion (from BLOOST code) at the peak power of the first pulse. Since one dollar is $0.0044 \delta k$ for GCFR, Fig. 22 shows that a \$50/sec insertion rate is equivalent to a step insertion of 10^{-3} in reactivity and that the step insertion is roughly proportional to the square root of the ramp-insertion rate.

5.2.3. Results of Calculations for Energy Release

Results from the AX-IT code are plotted in Figs. 24 through 30. The information includes results for

FIG. 29. Energy Yields as a Function of Time for Step Insertion of 4.4×10^{-3} in Reactivity.

energy release, maximum pressures, and kinetic energies for different step insertions of reactivity.

The work available from such an accident is bounded by the internal energy of the fuel above the threshold value, $Q - Q^*$. This function is plotted in Fig. 25. It is seen that a \$50/sec insertion rate would result in a $(Q - Q^*)$ of approximately 700 MW-sec. This function $(Q - Q^*)$ is roughly proportional to the square of the step reactivity insertion, or roughly proportional to the ramp insertion rate.

Approximate curves for the maximum pressure attained in the accident are given in Fig. 26. A value of the order of 1000 atm is observed for a \$50/sec insertion rate; the maximum local pressure is also approximately proportional to the ramp insertion rate.

The pressures shown in Fig. 26 are local values, and the shock wave should be reduced considerably by the surrounding massive, and relatively porous, unmelted fuel and blanket material. Furthermore, the very large space available between the core and the reactor vessel (the latter sized by the boiler dimensions) allows room for the insertion of large quantities of porous material to extend this effect and further protect the vessel from impulsive pressure leaks. This effect was, however, not taken into account for this study.

The kinetic energy of the core and blanket materials was obtained from the calculation for the treatment of the outer regions as homogeneous mixtures. These values are shown in Fig. 27 and are another measure of the energy available for damage. For a \$50/sec ramp, the kinetic energy is only 10 MW-sec, since this function drops off sharply as reactivity insertion is reduced. It is roughly proportional to the square of the ramp insertion rate.

In Fig. 28 are plotted the pressures and velocities of the core center and core edge for very severe accident conditions, corresponding to a reactivity ramp of \$600/sec.

In Figs. 29 and 30 the total energy pulse Q , the upper bound on available work, $Q - Q^*$, and the kinetic energy are plotted as functions of time for step insertions of 4.4×10^{-3} and 4.4×10^{-4} in reactivity, corresponding to ramp rates of \$600/sec and \$6/sec. It is seen that the energy pulse is terminated in the order of milliseconds or less in all cases.

5.3. Conclusions

The calculations of energy release presented here are of a survey nature intended to outline important design points and to lend understanding to the functional dependence of the variables, rather than to arrive at definite design values.

FIG. 30. Energy Yields as a Function of Time for Step Insertion of 4.4×10^{-4} in Reactivity.

Several important characteristics have been discovered.

1. The specific mechanical design configuration of a reactor is of great importance in eliminating potential causes of severe accidents, e.g., top support of fuel, withdrawal of control rods upward against the coolant flow direction, provision of splitting cones below the reactor core, and a stiff grid plate.

2. The minimal reactivity worth of gas as compared with liquid metal is a distinct advantage for the GCFR.

3. The Doppler coefficient in large ceramic reactors is of considerable importance, in that many reactivity accidents can be terminated by this mechanism alone, without disassembly or undue core melting.

4. The quantized nature of the energy pulses allows time for disassembly between pulses. The energy release is proportional to the fraction of fuel which is

vaporized during a single pulse, so that the maximum-to-average specific power for the fuel is an important design variable. On the other hand, it would not be desirable that the power be overly shaped so as to peak near the core boundaries, since an initial accident could then lead to core compaction, rather than disassembly.

5. The unmolten, compressible material surrounding the vaporized region should prove of great effectiveness as a blast shield.

6. The substitution of gas for liquid-metal coolant should also be beneficial in reducing the effective speed of sound in the core structure, i.e., the pressure wave should be much less without tamping by liquid metal.

7. The concrete pressure vessel should prove an effective blast shield due to its large wall inertia.

8. The low fuel enrichment that goes with design

for high internal conversion ratio is valuable from the safety point of view, in increasing the minimum critical value, i.e., reducing the inherent number of critical measures in the system.

APPENDIX A

Reactivity Coefficients

A.1. Doppler Coefficient

For this study, the Doppler coefficient due to the broadening of U^{238} resonances was evaluated by the Nordheim integral method for well-spaced resonances. The effect of some of the approximations in this method were checked with the GAROL code, which includes the effects of resonance overlap and down-scatter from all nuclides. Although the more accurate calculation showed that there was considerable variation in the collision density as a function of energy, the overall Doppler coefficient changed by only approximately 5%. This effect, which tends to increase the magnitude of the Doppler coefficient, is somewhat balanced by the opposite effect of Pu^{239} . The coefficient as evaluated shows a $1/T$ dependence and is given by the equation

$$\frac{\delta k}{\delta T} = \frac{-0.006418}{T}.$$

The hot-to-cold swing from $293^\circ K$ to $1341^\circ K$ is 0.009755, or \$2.27.

A.2. Helium Density Coefficient

The helium density coefficient was evaluated from one-dimensional diffusion-theory codes and from modified zero-dimensional burnup studies. The results

FIG. 31. Criticality Factor as a Function of Change in Core Volume.

9. No accident situations have yet been discovered for the GCFR which would be of the severity required to result in such energy releases of the orders considered in this section of the study.

showed that complete loss of helium at beginning of life would lead to an increase in reactivity of some 30 cents. The burnup studies showed that this amount would be reduced somewhat during the lifetime of the core.

The helium temperature coefficient is evaluated by assuming that the reactivity change is essentially linear with the coolant density. If the perfect-gas laws are utilized, the coefficient can be evaluated as a function of temperature, giving

$$\frac{\partial k}{\partial T} = \alpha \frac{T_0}{T^2},$$

where α is 30 cents or 0.00129 in multiplication.

A.3. Core Expansion

The change in reactivity as a function of change in core volume is shown in Fig. 31. The evaluation is a result of one-dimensional diffusion-theory calculations. From the slope of the curve, it may be determined that the reactivity changes by 0.0014 δk per percent change in volume in the axial direction and by 0.0024 δk per percent change in volume in the radial direction.

To determine the temperature coefficient of reactivity due to expansion, it remains only to determine the expansion coefficients in the radial and axial directions. In the axial direction, the assumption was made that elongation of the core due to temperature effects was due to the axial expansion of the cladding alone. This leads to the expression

$$\frac{\delta k}{\delta T_{\text{clad}}} = -2.3 \times 10^{-6} \delta k/^{\circ}C$$

and yields a cold-to-hot swing of -\$0.31.

Evaluation of the temperature coefficient of expansion in the radial direction is somewhat more involved. The assumption is made that the bowing effect is a function only of the difference between the temperature of the inner box wall and that of the outer wall. The temperature gradient across the fuel pins is neglected. The reason for this assumption is as follows. Although the box structure is quite rigid, the pins are long, thin rods which are quite flexible in comparison, and the stress that would be required to keep the pins straight is small compared with the stress required to bend the box.

It is further assumed that the box walls, with their large area exposed to coolant flow, are directly re-

FIG. 32. Reactivity Feedback due to Coolant Density Change and Bowing as a Function of Coolant Temperature.

sponsive to the coolant temperature. The temperature difference between the inner and outer box walls is derived from the assumptions that the coolant does not mix as it travels downward through the boxes and that the radial peaking of the power profile results in a similar peaking in the temperature rise of the coolant as it passes through the box. These lead to the expression

$$\delta k / \delta T = -1.6 \times 10^{-6} \delta k / ^\circ\text{C},$$

where δT refers to the change in the average coolant temperature.

In Fig. 32 are plotted the reactivity due to bowing-induced radial expansion and that due to helium density, both as a function of average coolant temperature. Their sum is also plotted. It is of interest to note that the sum indicates that the reactivity contribution from the sum of these two effects is always negative or zero.

It is recognized that in a fast transient situation

the negative effect of box bowing would be somewhat delayed compared with the coolant temperature rise, which in turn is delayed relative to the fuel temperature rise. In the analyses of this study, fuel assembly bowing was neglected in Section 4 for stability analysis and Section 5 for fast transients, and was assumed to be in equilibrium with the coolant temperature for the slow transients of Section 3.

A.4 Grid-plate Bowing

Grid-plate bowing has been calculated by determining the amount of spreading of the core that would result from the coolant drag forces pulling on the grid plate and deflecting it in a concave manner. This effect has been shown to be worth about 2 cents in negative reactivity at full flow and has been neglected in all calculations.

A.5. Reactor Properties

The neutron lifetime of 4.37×10^{-7} sec was evaluated by averaging $1/v$ over the reactor spectrum and then using the relationship

$$l = \left\langle \frac{1}{v} \right\rangle \frac{1}{\langle \nu \Sigma_f \rangle}.$$

The fraction of neutrons in each delayed group and the decay constant for each of these groups are given in Table 2, where contributions from fissions in all nuclides are included. The average delayed-neutron fraction gives a value of 0.0044 for the dollar.

TABLE 2. DELAYED-NEUTRON GROUPS

Group No.	Fractional β	λ (sec ⁻¹)
1	0.0001	1.29×10^{-2}
2	0.000867	3.11×10^{-2}
3	0.000808	1.34×10^{-1}
4	0.001612	3.31×10^{-1}
5	0.000761	1.26
6	0.000121	3.21

FIG. 33. Reactivity Inserted per Control Rod as a Function of Time after Scram and Position.

A.6. Control Rods

The 29 control rods are required to hold down approximately 9% in reactivity. This results in an average control-rod worth of approximately 0.3%, or 70 cents per rod. The 9% is made up of approximately 1% in cold-to-hot swing and about 1 dollar in excess reactivity for burnup. The remainder is the shutdown margin. The withdrawal rate is set at 10 in./min. The scram rate is set by assuming an initial acceleration of 5 g and a symmetrical velocity-position diagram. Figure 33 also shows the scram reactivity insertion per rod as a function of time.

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Session IV
October 12, 1965

Discussion of Papers Among Panel

Chairman: D. OKRENT
Argonne National Laboratory

Secretaries: D. Meneghetti
D. A. Meneley
Argonne National Laboratory

Panel Members:

M. Benedict, Massachusetts Institute of Technology
K. P. Cohen, General Electric
G. P. Denielou, CEA
P. Fortescue, General Atomic
W. Häfele, Karlsruhe
D. Okrent, Argonne
L. Silverman, Harvard University
T. J. Thompson, Massachusetts Institute of Technology
J. H. Wright, Westinghouse
W. H. Zinn, Combustion Engineering

Mr. Okrent: Good afternoon gentlemen. I should like to introduce the independent members of the Panel. On my immediate right we have Dr. Leslie Silverman of Harvard University, and a former chairman of the Advisory Committee on Reactor Safeguards. Next to him we have Dr. Manson Benedict of Massachusetts Institute of Technology, a former member of the Advisory Committee on Reactor Safeguards and a member of the General Advisory Committee. Next is Dr. W. H. Zinn, Vice-President of Combustion Engineering and the first Director of Argonne National Laboratory, and finally we have Dr. T. J. Thompson of MIT and a former chairman of the Advisory Committee on Reactor Safeguards. At the other end of the table we have the five reactor designers. Since this panel is in itself an experiment, we will attempt a trial procedure which we may modify as we go along. We will start by asking the independent members to develop questions of interest. We will expect all the various reactor designers to chime in as they think it appropriate, and in particular, to answer questions that may be put to them. Dr. Silverman is nearest to me. Would you care to start?

Mr. Silverman: I might say I don't qualify as a fast reactor man. I am quite interested to know something about the relative costs of these units in terms of containment. We are always harassed by people who have regarded the Reactor Safeguards Committee as the Reactor Prevention Committee. Added safeguards must be placed upon the reactor to make it qualify in terms of the prevention of any undue risk to the health and safety of the public. The cost of containment and the cost of engineered safeguards fit this category. In addition to that, we are also concerned with the effectiveness of these devices and their reliability. So I think a good general question to all the speakers would be "Can they develop anything along this line on the cost of these 'paper reactors' at the present time?" And, can they give us some feeling for the accident problem as they see it? One of the healthy things I heard this morning was the consideration of something that was probably long overdue, and that is to call the accident we worry about the design basis accident instead of the maximum credible. Everybody has trouble with the word "credible;" and the next thing they are concerned about is what's "maximum credible." Other applicants come in and talk about the hypothetical accident or, in some cases, a maximum credible operating accident. So I think terminology here is another problem, and I would like to encourage the use of design basis acci-

dent as being more realistic, because we hope that none of these accidents ever take place. I would also like to hear the panel members develop some information on backup shutdown devices, and would like to see some more information presented on the large sodium fire, which may go along with the accidents that seem to be considered in the design. That is all that I had at the moment, Mr. Chairman.

Mr. Okrent: I wonder if any of the reactor designers would care to comment on costs of containment and engineered safeguards relative to overall plant costs.

Mr. Häfele: I should say that our cost evaluation of the Sodium-I design indicates that the overall cost of the plant is \$116,000,000, equal to 462,000,000 Deutsche Marks (DM). Of these 462,000,000 DM, 200,000,000 are assumed to be the cost for the reactor parts. That is 42% of the total plant. Of the 200,000,000 DM for the reactor parts, however, 83,000,000 DM were considered to be sort of a contingency. That is more than 40% of the reactor parts, because you really are not sure how expensive the large sodium components will finally be. Therefore the basis for calculation in the reactor part is 116,000,000 DM, or 32% of the overall sum. Of this, 24,000,000 DM goes to the reactor building and construction, including the container. That is a 6 to 7% item, if you take the whole overall price of 460,000,000 DM.

Mr. Okrent: As I understand it then, the containment part is a 6 or 7% item.

Mr. Häfele: Yes. In our evaluation it comes out to be 6% of the whole cost. In another containment philosophy we have pursued, it would increase to perhaps 8%. Our design is already close to a two-shell containment, having, so to say, the inner compartment a steel and concrete structure. We believe that an increase from 6 to 8% would be sufficient to cover the case where one insists on having the double-containment facility. Our conclusion is that this is not too heavy competition between safety and economy. The burden might much more be on the side of operation and maintenance by having this tightness in the inner part of the reactor building.

Mr. Okrent: Dr. Cohen, did you have a comment?

Mr. Cohen: Yes. I wanted to say that it is really very difficult to isolate the containment cost from the other costs associated with safety, and even to separate the costs associated with safety from the costs associated with just radiation. The entire construction of a nuclear plant has unusual requirements compared to let's say building a ship, as regards tightness, weld inspection, etc. We have many times tried to evalu-

ate the containment costs. Depending upon the types of assumptions made, one gets that anywhere that 2 to 20% of the whole plant cost could be attributed to containment.

Mr. Fortescue: Speaking on containment, the gas-cooled system has special problems of its own, which differentiate it considerably from the sodium system. Of course, the essential difference is the proposal to use prestressed concrete for the primary containment. In this respect, it is different even from the steam-cooled systems, which are not readily adaptable to that type of containment, primarily on account of problems with thermal insulation. However, having adopted the idea of using prestressed concrete for primary containment, a different kind of philosophy is used in determining the second containment. In normal practice, if you can use such a word in this business, prestressed concrete vessels are used without secondary containment. They are so used in Europe with natural uranium and thermal systems. It seems to me the essential point is this. If one accepts the characteristic of the prestressed-concrete containment, that by virtue of its redundancy and the fact that the tensile members are outside the temperature and irradiation field, and so forth, catastrophic failure is to be ruled out. If you accept this philosophy, the source of concern is a modest rate of leak. What dictates the need for a secondary containment is whether or not, and to what extent, you assume a number of failed fuel elements to occur at the same time as this leak, because the only reason for wanting to contain the reactor is to prevent egress of the fission products. The philosophy which we have adopted is that we would provide, not a hermetically sealed secondary containment, i.e., one capable of a positive pressure, but a form of containment which is not pressurized and which is normally vented through filters. In effect, this involves the philosophy that you are accepting a degree of leak which is considered maximum credible to the system of gas, and furthermore assuming rather arbitrarily that, say, 1 or 2% of the fuel elements are, in fact, discharging their fission products into the confinement. It is not at all difficult to deal with that. It would be very difficult to deal with a leak plus all of the fission products.

Mr. Okrent: Are you not indeed in trouble if you have only a confinement system and a catastrophic rupture of your pressure vessel results? You then have nothing to catch the noble gases.

Mr. Fortescue: That is right. Indeed that remark applies to any use of primary containment with no secondary containment, so I don't think we would go that far. What we say is, we would have the delay

and filtering system. To get some idea of the order of magnitude of attenuation which you can get with a practical filter, we believe you could dump up to, say, 10-20 megacuries of activity into such a containment and keep within site boundary restrictions, meaning, in particular, 25-R whole-body dose at site boundary.

Mr. Okrent: Would this be with a tight building or a vented building?

Mr. Fortescue: Vented building with trapping as far as you can go. Of course, the noble gases go right through, in fact, most of this trouble is the noble gases. But the cost of the nonsealed containment would be very much less than that of total (pressurized) containment.

Mr. Denielou: I would first concur with the opinion of Karl Cohen. I think it is so very difficult to distinguish between what is due to safety considerations and what is due only to radiation. Then, I would make a distinction between tightness and withstanding pressure. In our experience, if you design a building for withstanding only snow, winds, and so forth, it doesn't cost very much to have it tight. You can rely on very simple devices, such as siphon type of things to have the wires out, and so on. The tightness, in my opinion, is very cheap, but pressure costs a lot.

Mr. Wright: You know, it is easy to look at something that costs \$120,000,000 and say well, so what if there is another \$6 or \$8,000,000 associated with containment. But, for the companies represented in this room that have been fighting this reactor battle on the commercial front at least, that is a huge amount of money. Our first competition has been and will remain the coal industry, after which internal competition in the nuclear industry is the problem. There have been far more coal power stations built in the last year than nuclear power plants. And I think that this will continue for some time; therefore, I do not view lightly an extra \$6 or \$8,000,000. I would concur that the cost of a fully pressurized containment system with shielding, around such a reactor as we are talking about, would probably not exceed \$8,000,000. I might even guess it as \$5 to \$8,000,000. Concerning the sodium fire, the philosophy which we employ, and which I guess is the same as the Germans use, is that of a primary containment system which might be likened unto a prestressed concrete vessel, except that its normal operation is at zero pressure. This would seem to provide a lot of safety margin. But at the same time, I would have difficulty at this moment to buy a prestressed concrete vessel for any reactor, without extra containment. And certainly the case of radioactive sodium fires requires considerable attention with regard to containment. I'm afraid I would be inclined

to feel that you have to consider this \$5 to \$8,000,000 for a while yet.

Mr. Silverman: Part of the cost of containment, as I see it, is inherent in the design. You want to keep inert gas in the primary containment because you have sodium in your system. It is pretty hard to operate without inert gas under those circumstances. I brought the sodium fire into the picture because this is considered one of the "after" problems; as somebody stated, if one could do away with containment, then the sodium fire would be perhaps the controlling accident.

Mr. Fortescue: It does seem to me that the question which very strongly influences the cost of secondary containment is the matter of how seriously one takes this maximum impossible accident, i.e., the Bethe-Tait explosive type of reaction. We all do our calculations in figures in terms of tons of TNT yield and then, it seems to me, we all stop at that point. Do we or don't we have to make a secondary containment that really does contain that? In other words, if you have got to take the pressure which results from the dumping of that energy into heat energy, and also the burning of any sodium, which presumably goes along with it, with the assumption that all of the fission products not just a few of them, are in there, which seems to go along with the big bang, it seems to me you have a mighty expensive containment. So I am wondering how seriously this has been reflected in the cost of their containment? Or is it that the calculations stop with the big bang?

Mr. Okrent: I believe the idea of the two-containment system is that even in the presence of such an explosive event, there is no fire, because it is confined within an inert atmosphere.

Mr. Fortescue: But not all systems envisage necessarily an inert atmosphere in the secondary containment.

Mr. Okrent: No, this is within the primary containment, where the sodium is contained.

Mr. Fortescue: I am not thinking of the primary containment rupturing in a big way. I am thinking of a subsequent gentle escape of high pressure, which must be considered to be in the primary containment, as a result of the heating of the gas that is there. Eventually it sort of fizzles out into the secondary containment and then you are stuck with the problem. Are you going to deal with that pressure or not?

Mr. Okrent: I suppose the answer depends on what one assumes as the leak rate. If you rupture your primary containment, then I think you do have many things leaking into whatever you have outside. If it is truly a primary containment, you don't; it is a minor

leakage. Could I ask Dr. Silverman if he wants to pursue the question of engineered safeguards at this time?

Mr. Silverman: I think probably I have taken up enough time, I will let somebody else take over.

Mr. Okrent: Dr. Benedict would you care to raise a point?

Mr. Benedict: Well, there are two somewhat related points that I would be glad to have comments on from the reactor designers. One has to do with the penalty which is paid or the compromises which are made when an attempt is made to design a sodium-cooled fast reactor such that the increase in reactivity on loss of sodium doesn't lead to any serious consequences. I think the steps which Dr. Cohen described, which have led to his company's design of a reactor in which the increase in reactivity on loss of sodium is down to a tolerable level, certainly are very valuable. They give us valuable insight into what can be done to make this reactivity increase acceptable, and also what penalty we have to pay in order to make it as low as it has been made. As I see it, the penalties are first that the breeding gain is much lower than would be possible if we did not soften the spectrum and increase the leakage; second, the internal conversion ratio is substantially lower. I think, Dr. Cohen, you are down somewhere around 0.7, or thereabout.

Mr. Cohen: That is right.

Mr. Benedict: Which in turn gives two other disadvantages to the reactor, of which one is the rather high swing in reactivity between fresh fuel and fuel when it is time to replace the fuel. I think you had something like \$7 worth of reactivity change, did you not?

Mr. Cohen: No, I think it is about \$10.

Mr. Benedict: \$10, and the need for rather frequent refueling. I note that if the internal conversion ratio can be kept close to unity, or as was the case with the gas-cooled reactor, is above unity, there is practically no reactivity swing during fuel lifetime, and the intervals between refuelings can be much higher. I wonder, as a matter of judgment, whether the panel members believe that we have gained more than we have lost in introducing this substantial reactivity change during burnup in place of a higher reactivity change in case sodium voiding occurs. The other point that I noted is that there is a very substantial difference in the amount of reactivity increase which takes place on loss of coolant in the several designs that were discussed today; I am sure that the audience recognizes that there is both a difference in numbers here and a difference in design philosophy. If I have three of the numbers correct, in case of the Karlsruhe design presently the maximum increase in reactivity which can

result in the most unfavorable loss of sodium coolant was something like \$7. In the case of the General Electric design, because of steps which have been taken to soften the spectrum and otherwise make this effect smaller, the number is down to about \$1.60. Finally, in reading the General Atomic paper, I note that the increase in reactivity resulting from a complete loss of helium coolant from the 1000-lb/in.² pressure down to atmospheric was some \$0.30. These of course represent very significant differences, provided one has to be concerned about a loss of a substantial fraction of the coolant in each of these cases. It becomes very important to consider how likely it is, particularly for the design which would have an increase in reactivity of \$7 on a most unfavorable loss of coolant, whether this much coolant, or any appreciable fraction of this much coolant, could be lost, and how quickly this loss could take place.

Now, as was emphasized earlier in the Conference, the sodium coolant has the very undesirable characteristic of being prone to very substantial superheat. One of Dr. Rosenow's students at MIT has found that unless rather special precautions are taken to prevent superheating, superheat to the extent of 50°F can easily be obtained before any boiling occurs at all. A simple estimate indicates that if we had sodium initially at, say, 1500°F or at its boiling point at atmospheric pressure, and 50°F superheat were to take place followed by flashing, some 50% of the volume initially occupied by the liquid sodium would now be occupied by sodium vapor. This could mean that if there were a substantial fraction of the reactor in which the sodium was superheated to the extent of 50°F, say by blockage of one or more fuel assemblies, almost instantaneously 50% of this volume would become sodium void. Thus it becomes very important in a sodium-cooled reactor which has as large a sodium void coefficient as \$7, for instance, to do everything possible to prevent sodium superheat. Reliance cannot be put on acoustic or other means to determine when superheating is likely to take place, because this is almost an instantaneous event. One has to be assured in the first place that the coolant flow is maintained and that superheating is not occurring. Thus unless sodium superheat can be prevented, measures to keep the reactivity increase to a tolerable level in case the sodium flashes are essential. Otherwise some other coolant, such as steam or gas which does not have this problem associated with it, becomes a safer cooling medium. I would like to have the panel develop these two points further. First, perhaps we could ask Dr. Cohen his view as to the relative balance between the gains and losses which the GE design has in going to a low internal conversion ratio and a

substantial reactivity swing during burnup, in order to keep the reactivity increase on sodium voiding down to a low level.

Mr. Cohen: I believe you have in part answered this yourself in your second question. We have, in fact, taken quite seriously the possibility of sodium flashing, and this is the reason we have striven so hard to reduce the reactivity effect. You quite correctly pointed out that we didn't get this free, even in the matter of safety from other types of accidents. And this is one of the reasons why we have another special criterion to deal with: the burnup reactivity swing, or at least to try to limit it. We are not very happy about the compromise that taking the void to be credible has forced us to make. We have, however, believed that we ought at least to look the devil in the eye and see how bad it gets, so we have some idea of the limit of the price that we may have to pay. I thoroughly subscribe to the point of view of Dr. Häfele, which is that we must study this problem of superheat and understand it. I have some feeling that the kind of egg-crate structures which are characteristic of the core are going to be fairly resistant to even a considerable shock wave. This is just the kind of structure to build to absorb shocks. Unless the shock from superheating is much severer than we think. By this structure we hope to localize the shock.

Mr. Häfele: If I may, I would also like to reply to your two points. First, I had the impression that you implied that the total breeding would change in the case of a very pancaked core, i.e., a high-leakage core. If one makes a comparison by the same assumptions, usually the total breeding ratio is somewhat increased, provided the blanket is thick enough, because the spectrum is somewhat hardened. Only the partition between external breeding and internal breeding is heavily changed. Indeed, since the internal breeding ratio goes down considerably, we at Karlsruhe believe that a point not yet brought to the full attention of the design people is the question of availability and maintenance cycle. We tried to evaluate this quantitatively (by Rheinisch-Westfälische Elektrizitätsgesellschaft) and they said that it will be of very great importance to have a yearly maintenance cycle, but they couldn't evaluate it in the form of figures. Therefore it is difficult, or up to now impossible, to get the numbers, but we do believe that a yearly maintenance cycle, and therefore a fuel cycle of one year, is of great importance when one can quantitatively evaluate the necessity of having such a high availability. One would be also in a position to evaluate the penalty of not having it, or to weigh it against the safety difficulties.

My second comment concerns the figure of \$7 for

the sodium void effect. First, the design basis for this Sodium-1 reactor was a sodium void coefficient of \$3. We calculated the \$7 figure only recently, after having introduced our own 26-group set. Here I should stress the point that even if we assume the same microscopic input and only use different weighting spectra, the value varies widely; the value of \$7 is the maximum value calculated. Really we do not know what the figure is. This uncertainty is underlined if you calculate the sodium void coefficient first with two-dimensional methods, and then with one-dimensional methods. In this case, the overall effect comes out with a different sign. All this reflects a situation in which one has to say we don't know what the effect really is. If the value were really \$7, then probably it would be necessary to change the core design as I mentioned in my oral presentation. I agree with you that the necessity of changing the core design is indeed emphasized by the superheat aspects. We have tried to stress this point and we are working vigorously on understanding this phenomenon. On the other hand, I do come back to the point that it is of great impact and importance only if you accept the fact that the safety system can fail.

Mr. Benedict: I am afraid I don't exactly understand that point of view. Suppose a substantial volume of superheated sodium can be built up in the core through some undetected reduction in flow, and this sodium then suddenly flashes. This can occur much too rapidly for the safety system to compensate it, and one could even have a prompt reactivity excursion.

Mr. Häfele: Yes, assuming that it is not possible to detect the plugging by instrumentation.

Mr. Okrent: Could I ask whether you both mean the same thing though? I believe Dr. Benedict had in mind more than one subassembly, in order to get a significant amount of reactivity added.

Mr. Benedict: One has to have a sufficient volume to give more than \$1 worth, or else the control system can presumably compensate for it.

Mr. Okrent: I don't believe that a single subassembly is likely to contribute a dollar, even if it is completely voided.

Mr. Häfele: I implied that already propagation has taken place. Avoiding propagation would be a measure against it, but it would be a heavy penalty for breeding, because the boxes of the subassemblies must be strong in that case. I agree with Dr. Okrent that plugging of a central subassembly gives you only 7 to 10 cents, an amount automatically controlled by the control system.

Mr. Denielou: In reply to Dr. Benedict's question, I think the comparison between the safety advantages

of putting in beryllium and the economic advantages of using it is difficult to make. But the disadvantages are more certain than the advantages. When I look at the sodium voidage figure, I have, with or without beryllium, \$1.6 or \$1.8 for a sodium voidage beginning in the upper axial blanket, and maybe \$2 or \$3 if it begins in the central planes. But because of the uncertainties already mentioned, within \$1.6, \$2, and \$3, it doesn't seem to matter so much. On the contrary, the disadvantages to the doubling time from softening the spectrum seem very clear.

Mr. Cohen: May I respond to that? I would like first to suggest that when one compares beryllium in and beryllium out, one should probably make sure that one is doing the same calculation in the same way. It is very difficult to compare calculations done at two different sites, as we saw earlier in this meeting.

I do believe there is a considerable difference between, say, \$3 and \$1.60. If I have \$3, I don't have to get the very very worst pattern to be super prompt critical. Consequently, the probability of getting something bad is larger. If I have just barely enough maximum sodium void reactivity to get me into trouble, I have to have a perfectly planned mishap in order to get into trouble. So I think there is considerable difference between smaller and larger amounts of the void reactivity.

Mr. Benedict: There is one thought that occurred to me upon hearing Dr. Cohen's paper this morning. I was wondering whether in view of the very substantial reduction in breeding gain which the introduction of beryllia into the plutonium-fueled reactor evidently brings about, whether your system might not have a higher breeding gain if it were using the thorium-U²³³ cycle rather than the plutonium-uranium cycle. Have you given any consideration to that?

Mr. Cohen: We have given some consideration to it, but our point of view is that we ought to exhaust the possibilities of the U-Pu system, including the sodium, before we go to another.

Mr. Zinn: I make this comment that we are making progress of sorts. At the conference two years ago, we had the sodium void problem before us in full measure, for the first time, and it gave everyone a great deal of concern. Between then and now, rather ingeniously, people have invented a way of getting the whole core voided by getting simple blockage of one fuel channel. So now I have two questions that are both directed towards what I would consider to be engineered safeguards. What is being thought of by the designers as measures to inhibit the consequences of flow blockage in one channel, and inhibit the propagation of this effect through the core? For instance, has consideration been given to stronger and differ-

ently shaped cans? I notice they are all hexagonal. Has one thought of ways of getting the coolant to the fuel pins through more than one opening or orifice? I suspect that we may wish to use some engineered safeguards to mitigate this effect which was pointed up rather dramatically this morning.

The second question involves the observation that the boundary line between the hypothetical maximum accident and the credible one seems to be the place where the control rods function, and I said control rods deliberately, because I believe all the designs terminate an unpleasant event by the movement of control rods, which I assume is always a movement of fuel or absorbers in the core. This means that the mechanical arrangements for making this movement are concentrated in one area, always on top of the reactor, a restricted area, and there is no other backup. There isn't redundancy for this control-rod system. My second question is: has there been thought of a redundant system or a backup system.

Mr. Denielou: To the first question, I think that the current design will be able to avoid the propagation of the melting in a single fuel subassembly. The only point is we are not able to demonstrate it to learned people. So before trying to avoid it, we prefer studying it; it costs money. Concerning the second point, we do not anticipate any other backup system. There is a feeling of defiance against all these fuseable systems, and so on, in France, more or less. We prefer having one single reliable system to a multiplicity of systems.

Mr. Fortescue: I think there is a measure of redundancy built into control rods, insofar as you don't need all of them to go in to shut the reactor down in any particular case. For instance, in the design I was giving this morning, only eleven out of the 27 control rods would be sufficient to shut the reactor down to room-temperature conditions. So there is a measure of redundancy in all control rods insofar that you don't need them all to go in. Beyond that, the only other measure that has occurred to us is to make quite sure that the design of the control rod is such that there is a limited rate of withdrawal. This would mean non-reversible mechanisms. It would seem to be sort of a primary goal in any control drive system used for fast reactors, meaning a screw or some such device which cannot possibly exceed a certain speed, and driven by synchronized motors or something like that.

Mr. Häfele: With respect to that question of a redundant safety system, I would like to say the following: As long as a logical pattern on which the safety system is designed is defined, then the safety system is always redundant. The problem is whether that logical definition covers all possible events and cir-

cumstances. Therefore the question goes back to the very first question, to define the scheme of a failure. Because it might easily be that one common reason for failure for all rods has been overlooked and then despite your logical layout of the safety system, no redundancy exists in that system any more.

Mr. Cohen: I would like to say that these are very good points that Dr. Zinn has brought up. On redundant systems, I think that we haven't done enough on them. There is the redundancy, of course, in the fact that all rods do not have to work. The point that all of these rods are localized in one place is, of course, a matter for concern. One should do more thinking.

Mr. Zinn: Might I interject a remark? What brought it to my mind was the statement by Dr. Benedict about getting a rapid pressure pulse in the reactor due to vaporization of the superheated sodium. And I am reminded of the fact that in the SL-1 accident, every single control rod drive was seized in its channel by a water hammer which acted on all of them in the same way, making all of them inoperative. The pressure pulse actually made all the rods imbed firmly in the reactor structure. They couldn't have moved. Therefore, I even question whether identical design of control rods should be used—whether one shouldn't put some variation into the design so that the same simple accident couldn't catch all of them.

Mr. Thompson: Just by chance, I had a question in the same general area. It seemed to me that one of the problems is the reliability that one can place on any one of the systems. I happen to have a very good mechanical engineer that works with me. Every time I ask him to make something that is really fail-safe, he tells me to do it electronically. I also have a very able electronics expert that works with me, and he says that if you want to do it really reliably, do it with some sort of a mechanical interlinkage. The problem really is that experience has not to date shown us that we can indeed design a perfectly reliable control system. I know of a recent instance in which all of the control rods in one particular reactor were seized in such a way that they could not be moved, because there was an attack on the seals that attached the control rods to the rest of the mechanism. It happened that this reactor was shut down at the time, but the same situation occurring in the reactor in operation might prevent totally all control rods from going into the reactor. I know of at least two or three instances in which the scram system for a reactor was either totally or partially disabled while in operation for longer or shorter periods. I have to say in all honesty that a part of our scram system at MIT went out for a fraction of a week; we don't

know how much of a fraction. We check it every Monday before startup, and it went out because a relay shorted from primary to secondary. This is a sealed-type relay. I have talked to many electronics people and none of them have ever seen this happen before.

In one major reactor in the United States, the scram control system was totally inoperative, in that it could not have been scrammed, for a period of time as long as the total month. There was no way that this reactor could have been scrammed during that period. These things do happen. Experience says that they do and therefore all of us must anticipate such failures and believe that they may happen. There are many control authorities including those in Canada and those in a number of laboratories in the United States that have asserted within the last two to three years that it is impossible to build a fail-safe system. If this is indeed true, then one has to think a good deal about putting all your eggs in one basket and assuming that the control system will always work perfectly. It appears likely that an unusually heavy burden is being placed on the operability of a single control system. It seems to me that it is particularly important that there be no common cause which could prevent all the rods from going in. For instance, at least in one case involving one control rod, sodium vapor going through a bellows seal in a fast reactor froze that rod so that it was inoperable. You can imagine that in some reactors this might be particularly bad, say in a reactor where there is only a very small change in reactivity with lifetime, so that the control rods might actually remain almost motionless for long periods of time. If they did, they might indeed freeze in some such manner.

Mr. Denielou: We would move them from time to time.

Mr. Thompson: But then you have a rod-exercise program. In fact, I have yet to see the perfect control-rod design. I would like to distinguish also between two things. One is redundancy, which has been mentioned several times. Redundancy is a repetitive application or use in parallel, so to speak, of the same sort of mechanism or system, for instance, three power-level trip circuits with at least two required to be operative at all times. But there is another principle that is also important. That is diversity. Very often you do not foresee some accident that may occur which will, in fact, immobilize your entire control system or any sets of control systems of identical design. It is, therefore, worthwhile for the reactor designer to attempt the challenge of trying to work diversity into fast reactor control systems. I would like to hear all of the five reactor designers involved here

answer three or four questions. One, do you believe it credible that there could be a major meltdown, and perhaps a reassembly, in the event that a control rod scram failed to work? In other words, what are the consequences of a failure to scram? As a corollary, if the rods fail to work, will boiling then result in a major accident? Two, do you believe that you can design a completely reliable control system? Three, do you have any ideas for a backup system? I think that has been pretty much answered. I think nobody does at the moment. Question number four is brought to mind by something that Denielou said. He said, I believe, it is a little schizophrenic to ask a man to design a system not to boil, and then ask him to make an analysis of what happens when it boils. In a way he is right. But in another way, this is exactly the thing the man should do, because he should really know the consequences of exceeding the boiling point. If anybody should know it, it should be the man who designs the plant, unless it is the man who will eventually operate the plant. If the designer realizes what will happen when the boiling point is exceeded, then he may indeed worry about whether the boiling point will be reached, and he will know what sort of risks he is taking. A reactor designer must not only explore the normal behavior of his reactor, but he must know what happens as he goes groping into the darkness of the regions beyond normal operation. He must know whether he is going to reach a cliff and fall off and be killed, or he must know whether it is a nice gentle slope that will gradually lead him down onto, perhaps, some lower plane. It would appear that one could get into a situation with a large reactor in which there was nothing one could do except to sit there and watch it happen. If this is indeed true, when such reactors are really designed, then I think the situation is indeed precarious. Therefore the fourth question I would like to ask the panelists is: How close do you really feel you can come to the boiling point of sodium in your reactor in a safe way?

Mr. Okrent: The first question is what happens to a reactor in an accident if indeed there is no scram. Mr. Wright?

Mr. Wright: I think I answered the question in the presentation, namely, that if we get flow blockage of one channel, a mechanical scram is absolutely required, or we progress through reactivity insertion to a single assembly meltdown which in turn gives a power-level rise to boiling, which in turn gives total core meltdown and criticality. I think the answer is that we envisage no way other than engineered safeguards to overcome that problem.

Mr. Häfele: I am not satisfied with the answer, because an engineering safeguard is just a prolongation

of the safety system. Therefore, I think it is not really the answer to what happens if the safety system fails.

Mr. Wright: I think you presumed that the safety system would fail, didn't you, Mr. Thompson?

Mr. Thompson: Yes, if you say it wouldn't scram, either because of electronic reasons or because the rods were all frozen in place, or something like that.

Mr. Häfele: And your answer is engineered safeguards?

Mr. Wright: No, if the use of control rods is thrown out, then I see no way of stopping it.

Mr. Okrent: Is this true of all the sodium-cooled designs? Dr. Cohen, could your design take blockage and voiding in single subassembly and ride it out, as it were?

Mr. Cohen: A single subassembly, certainly. I think the basic question which Dr. Thompson has asked is: can your reactor get into trouble in an emergency situation if you fail to scram? Say, for example, that power is lost to the pumps; the pumping drops and scram does not occur, the reactor keeping at power. Although some reliance may be placed in the structural coefficient, and so forth, the answer is basically yes, we are going to get into trouble.

Mr. Fortescue: Although most of the reasons so far proposed for things going wrong seem to be connected with sodium, obviously, we still have the same problem with the gas-cooled system, namely, that one must suppose it is a possibility that you cannot scram any of the rods; but to imagine that would require a common cause. I think it is beyond credibility that coincidence or separate kinds of things wrong with the different ones should occur at the same time. Thus it is likely that a common cause causes the jamming of the rods. Further, one has to look to see whether this common cause comes at the particularly unfortunate moment when you need to scram. If it is a question of a common cause of jamming and you have time to make matters right, in other words, you are allowed to run the reactor for a while while you think, there are various means possible to shut down the gas-cooled system other than the control rods. One, of course, common to any reactor, would be a rather extreme one, namely, to unload the fuel elements, which could be done provided you didn't care what happens to one if you drop it. I mean it would be difficult to arrange a tidy removal, while the reactor is running (MUCH LAUGHTER). In general, we certainly endorse the philosophy that diversity in the types of drive should be introduced. Although it is easy to introduce diversity into the drive mechanism, I think that is not really the trouble. What must be done is to introduce diversity into what is inside the core, a diversity which reacts in a different way to a com-

mon cause. That is the essential thing. All one can do is to acknowledge that it is a good thing in principle. I must remark that the kind of common cause capable and so violent as to jam simultaneously all the elements would presumably be one which justified pretty drastic remedial measures, even though these damage the core.

Mr. Thompson: I want to point out again that you don't have to jam the rods. I repeat again one major reactor worked for a large fraction of a month with its entire scram system electronically inoperative. Regardless of the signals given, no scram would occur. Therefore, mechanical ability was insufficient in this case. This is the sort of thing that can lie in wait for the unwary. It is not necessary that this sort of failure happen at the instant another causal failure or perturbation of the system occurs. The inoperative system need only wait for something else to happen, something that required scram.

Mr. Fortescue: I think there are some sort of ameliorating things one can do in the design, for instance, adoption of a solid drive. One would not use a cable drive for a fast reactor. A solid connection between the two with a substantial machine capable of overcoming minor friction could be worked into the design to mitigate this.

Mr. Okrent: I believe in the case mentioned that would not have helped. I wonder if we can go on to the last of Dr. Thompson's questions concerning how close one can come to boiling. Is there a big margin or a small margin to boiling in one's design?

Mr. Denielou: I would prefer answering the first question, if I may. If the control system fails to operate, we can imagine incidents which will destroy the reactor. But I think it is almost the same for any type of reactor.

Mr. Cohen: There were some other questions which I don't think were answered. One was: can you design a perfect safety system? I think that question answers itself. Then there was the statement that nobody has any ideas for backup. I deny that. For example, we have ideas for backup which would function despite binding. There could exist a thimble in the reactor and material or poison falls in the thimble. Of course, there is a need for a great many better ideas, because obviously this one has got some practical difficulties.

Mr. Wright: With regard to the same type of a thing, certainly there are other backup systems that are wilder and less conventional, but I would like to mention two points here. One is that the whole future concept of this reactor may take on a different form with time. Instead of building a nice little plant that we can justify putting next door to some other nice plant built at Ravenswood, maybe this is the time to

address ourselves to problems of truly highly reliable equipment, which is placed underground at some considerable distance. Maybe 15 or 20 years from now, reactor systems particularly of this type, might be located 100 ft below grade, hopefully with components which worked for an extended period of time without maintenance. Then maybe what we would be risking is the reactor system, as opposed to any particular safety problems. This approach might make feasible the going to a less safe reactor than any of those you have heard of today. Back to Dr. Benedict's question, in which he spoke of the economic incentives for moving away from a safe reactor. I think there are tremendous economic incentives. For example, with a simple equidimensional right circular cylinder for the core, there is a very high internal breeding ratio, above 1. The total inventory requirements to run the reactor are dropped by as much as a third. The reactivity requirements for lifetime are zero. At the same time it doesn't have the kind of sodium void characteristics that we would like, but I am not sure that this is really an ultimate constraint. A gain can be made on the fuel cycle that is of the order of four-tenths of a mil or more. In addition to that, doubling time is dropped down to 6 or 7 years. Whether or not for this performance we simply have to have a whole new concept of reactors and how to deal with their safety, I don't know.

Concerning a backup scheme, sodium cores have enough pressure drop that they would be lifted except for the holddown devices. One scheme that can be thought of is a system that has no bottom core-support plate. It has basically a system of channels at the top into which the fuel assemblies are drawn from the bottom. They are pulled up into this position. They are held there by some kind of fusible wire. Having enough weight attached to the bottom, you could either pull it in two or pull the whole assembly out if it overheated. Such backups are certainly possible, if we look at the space programs and other far-out things. I think we can reach quite far in terms of having additional safety systems. I bet we could get a dozen from the audience right now.

Mr. Denielou: That is a French patent.

Mr. Wright: Oh—I am sorry. (LAUGHTER)

Mr. Okrent: One thing that has been said at times in the past is that the nuclear accident that occurs from the worst sodium expulsion is no worse, in general, than the accident calculated if the core melts. Since the core melts when the sodium leaves, it hardly matters whether the coefficient was positive or negative. I would like to ask the sodium reactor designers whether they think this way or not.

Mr. Denielou: Yes.

Mr. Cohen: I would say no for a short answer. We don't know very much about what the meltdown accident is going to be, and though one can indeed make calculations about it, one would be naive to really believe them. The accidents due to voiding are one thing, and the accident from melting another. I don't take the point of view that necessarily one is worse than the other; it could well be either way.

Mr. Okrent: I guess to use a phrase that was suggested before, if you took a design basis meltdown accident, and a design basis sodium voiding accident, would you have any different requirements for your primary containment system?

Mr. Cohen: That just depends on the void coefficients that you assume in your void accident, and on the maximum to average power ratio, and consequently the rate of assembly, in your meltdown accident. There isn't any one answer.

Mr. Häfele: Tentatively I would say there is a difference, because if sodium starts boiling first and fuel evaporates thereafter, both the ramp rates of the fuel compaction and of sodium boiling add. That means you add the ramp rates, and you can easily get 200 or 300, even more, dollars per second, which has a strong impact on the energy release in that excursion. If only fuel vaporization occurred, then a completely different ramp rate may be meaningful in leading to an energy release. I would even stress the point a little bit further. One has to recognize the fact that perhaps even no nuclear excursion occurs at all, except the one which vaporizes the fuel, which has to be a really strong excursion. That means no larger excursion would take place, just a plain sodium vapor explosion takes place. These are two completely different mechanisms, and therefore we do envisage a situation in which the energy release is remarkably different. We have a feeling at Karlsruhe that one should repeat a BORAX-type experiment of the old days. It was very illuminating and many results were concluded from it.

Mr. Silverman: Mr. Wright raised a point which I think is very important, namely, that of locating a reactor underground. We already know what we can do underground with proper attention to geology, faults, and things of this type to avoid venting. Optimum economic design could provide the incentive aspects to go underground and point out the benefits to be gained. I am not saying you should design the maximum unsafe reactor. What I am pointing out is that if you design a reactor for the best technology and economic yields, then the thing to do is to look at it in a location that is underground and see whether or not you have a better safety position for such a reactor as an incentive.

Mr. Zinn: When you say underground, how far underground? Does this mean below a layer of rock which is impervious, or does this mean just out of sight?

Mr. Silverman: Based on what Edward Teller says you can calculate what the energy yield is of the given explosion and decide on the depth. I don't remember how many kilotons for how many feet, but he has a formula that fits nicely.

Mr. Zinn: But if we believe what we saw this morning that everyone comes up with 1000 kg or less TNT, then we really don't have an explosion-containment problem; it is a gas-containment problem. Is that any better underground than above, where you can perhaps find leaks that exist?

Mr. Silverman: It is better underground in my opinion, because we are already doing it underground. We have underground gas-storage chambers all over the country now.

Mr. Zinn: A fairly leaky business, isn't it?

Mr. Silverman: No, it isn't too bad. I can assure you that the economics of it are such that the leaks are negligible and that the actual amounts that are leaking so far can be considered acceptable by any standards we have for health and safety.

Mr. Zinn: I know something about this. When they were trying to locate the first such large underground storage in this area, we provided them with a radioactive tracer so that they could go around and find all the wells that had been abandoned which were leaking. This had to be done over a period of a year and a half. There were a lot of these wells to find and plug up. I don't believe they found them all to this day, in terms of the kind of leaks we are considering here. I wonder if it is really so that these things are tight in terms of the quantities of radioactivity that we are dealing with in these accident conditions.

Mr. Silverman: A number of tests of this type are being carried out with radioactivity to investigate the possibility for underground storage of rad-wastes from normal processing. Of course, there are different geological formations everywhere. If you get underneath the water table, there is no question but that you have a capping phenomena that seals it off. I am not able to pick out specific areas, but where studies have been made and the wells have been drilled, good locations have been obtained. You don't have to just put it down without any kind of a ceiling. You can certainly provide the kind of sealant they are providing for liquefied natural gas and things of that kind. They actually freeze a layer of mud around the location they are dealing with.

Mr. Häfele: I am very astonished to hear that you want to bring reactors underground. I read in all

articles, journals, and technical journals and newspapers that the USAEC wants to bring reactors downtown into the big cities. And I think that is the right path, and undoubtedly it will happen with some reactors. If fast reactors cannot compete with that trend, they won't succeed at all. In Karlsruhe we won't follow that trend.

Mr. Silverman: A point that might be raised here is that such reactors may be suitable downtown in big cities if they are 500 to 1000 ft below the city. We are unable to say now that these are so safe you can do it on the basis of the information available.

Mr. Fortescue: I agree with Häfele and am against putting reactors underground. The essential point is the desire to contain fission products. I don't see the leak-tight standards associated with containment that are remotely connected with shallow underground stuff. It is quite a different thing from going down in a salt mine. This we are not prepared to do, I hope.

Mr. Denielou: May I strongly concur. We are not that desperate.

Mr. Wright: I rather think the point was lost somewhere down the line. The point was that it might be possible to design a less-safe reactor having far better economics. On that basis, the overall economics might be superior by running a less safe reactor below grade. Now, certainly, I suspect that we will be in the battle trying to locate one in the middle of cities, just like everyone else. But the reactor that we try to put there will be very profoundly safe. I am just saying that there might be a trade-off in these things. We are not saying that all reactors go underground.

Mr. Cohen: Might I make a facetious remark, namely, that as soon as you put the reactor underground, you are going to put a hatchway for people to walk down. And, when you go to review the safety of this reactor, since these are engineered safeguards, the hatches will be assumed to be left open.

Mr. Okrent: I believe Dr. Silverman had a more detailed, technical question concerning backup engineered safeguards. I wonder if we could look at what the gas-cooled reactor has to offer in the way of emergency heat removal in the event primary systems just don't work. What kind of emergency heat removal is there in the event that the core melts down to the bottom of the vessel, and so forth?

Mr. Fortescue: We do propose to have emergency cooling other than by the steam-turbine circulation system. This is primarily apart from safety. From the pure measure of convenience in commissioning the plant, it would be necessary to do this. Certainly one has to take into account the possibility of melt-

down of a single fuel element. This could arise from failure of a single pin which was self-propagating within the fuel cell. Because of that, we certainly would have to have the other backup safety, namely, the containment around the vessel, not a pressurized containment, but a leaking one. It is not essential with a prestressed concrete vessel, but it is a backup because you have to reckon on some radioactivity in the primary circuit. We have redundancy in the four circulators, each operated by a separate boiler. Still, there is one possible common cause for them all stopping at once, either (a) total absence of boiler feedwater, or (b) a main turbine casing going, which would preclude the passage of steam. Because of that, we want an emergency-shutdown blower, which, fortunately, doesn't require a great deal of power. There are also minor backups to keep the lining of the pressure vessel cooled to avoid damaging the concrete. This is done by a redundant system having two or more independent water circuits. This is not a matter of vital consequence for immediate safety, because the damage done to concrete is not of a catastrophic nature. It just might put the reactor vessel out of commission. I think the system doesn't differ from that for sodium here. I don't see any great difference in the fact that this happens to be gas-cooled rather than sodium-cooled.

Mr. Okrent: Is there any problem in the unlikely event of the core melting down onto your separator—a problem of the decay heat supplying more energy than you find convenient to take away? What I have in mind is that Dr. Cohen mentioned that he felt that the large pool of sodium represents a big advantage in keeping things cool after the design-basis accident. At the moment I don't see any reason why gas-cooled reactors are less likely to require a design-basis accident. Therefore, I am asking: what do you do after this design accident?

Mr. Fortescue: I should have mentioned that the splitting cone contains poison material as well to accentuate the effect of the splitting. But I think one assumes that when it is down there, it is presumably partly melted and certainly things would get pretty hot down there. But I don't really see that this endangers the pressure vessel. In the long term you flood the reactor with water eventually.

Mr. Häfele: Could I ask Dr. Fortescue what is the design-basis accident in your design?

Mr. Fortescue: There are a number of safety-oriented things which have caused headaches. If you are asking which one caused the worst headache, it is the loss of coolant. The loss-of-coolant accident caused us to reject everything but a prestressed concrete vessel, which is a limitation on design. The worst acci-

dent that we envisage is the meltdown of the core which, of course, means that you have got fission products there as well. We add to this a maximum credible leak from the vessel, which we hope is small enough to be dealt with by the filter system.

Mr. Okrent: Are there comments on the question of the credibility of failure of the vessel? It seems to me this is a key part of the armor and everything seems to be hinged on this. I think it might be worth exploring this point for a minute.

Mr. Zinn: I have a question that has always concerned me. Are there any openings that have to be sealed with gaskets, steel covers, and so on in the concrete vessel? How are they given a similar integrity to the concrete and its stressing cable?

Mr. Fortescue: In general, the penetration usually consists of a fairly thick-walled tube, a metal tube, which passes through the concrete wall and is welded to the inner skin forming part of it. In general, that tube has in itself on its outer surface rings which interlock with the concrete, so that the main thrust on any end cap is taken and transmitted to the concrete and not presented to the welded joint between the relatively thin surfaces of the inner skin. In general, the biggest main openings that we envisage are maybe 5 ft in diameter. They are to get access to the blowers and so forth. Now a plug there would consist in fact of a steel disc, if you like, of sufficiently thick strength to withstand the pressure. That steel disc is held into the steel tube by split rings around the outside to take the thrust. You back that up with seal welding to take the small leaks. That is roughly the way things are sealed up.

Mr. Zinn: In view of that type of sealing, is the concrete vessel really that much different from what would be a well-designed steel containment vessel?

Mr. Fortescue: Oh, I think it is. The essential point about the concrete vessel is its extreme redundancy. The concrete vessel as a whole is an extremely redundant structure. In other words, its strength comes from a multiplicity of separate cables and no means of propagation of failure from one cable to the next. I don't quite see how one can postulate that the individual openings offer a safety problem comparable to the possibility of, say, fracturing a duct. Nobody supposes a steel vessel would blow up—and I am not talking about fast reactors here, I am talking about steel vessels, in general. But one does have to suppose that a main duct fractures, and this is indeed a possibility because there are thermal stresses of considerable magnitude in ducts. The fracture of a main duct is the thing to avoid. This is avoided with a prestressed concrete vessel, and I don't see that the fracture of a large thick disc is the thing one need contemplate.

Mr. Zinn: I think one must distinguish between two cases. For a sodium fast reactor, the containment building has to contain a pressure which presumably can be calculated in advance and designed for, and have a leakage rate which is specified and small enough, which is frequently determined by the inventory of fission products in the whole core. In the gas-cooled reactor exactly the same situation exists. It seems to me that in both cases it is the leakage from the containment that determines the difficulty or the expense of building it, not so much the ability to contain the pressure.

Mr. Fortescue: If you are talking about the rate of leakage from the primary system, one of the points where the totally enclosed system scores is that there are indeed a very much less number of penetrations. They may be big but the total number of penetrations is very small, because all of the equipment is internal. It is true you have got to take steam pipes out, but they are of much smaller diameter. One of the points you have to ensure is that there can't be a leak if you break a steam pipe at such a point that a hole is left in the vessel where the steam pipe was. This has to be gotten around by design, and a redundancy is built in around such penetrations, a redundancy which does involve bellows as one possibility.

Mr. Silverman: Can your steam boiler break or blow up inside that containment?

Mr. Fortescue: (LAUGHTER) It is assumed, in the calculations of the strength of the vessel, that the steam boiler blows up. It is assumed tube fractures result. Insofar as one has no pressure shell around the boiler, there is another area in which one has gained.

Mr. Okrent: I recall a talk at a recent special ANS meeting on concrete vessels in which a German expert, in response to a question I raised, said that a catastrophic failure of a prestressed concrete pressure vessel could occur in two ways. One would be a shear mode of failure, the other around enclosures. If that was the feeling of one expert, I wonder how one can design on the basis of incredibility of a catastrophic failure.

Mr. Fortescue: This depends a lot on the design of the individual vessel. I think the first French prestressed concrete vessel had a dish end. The end was not prestressed—it was dished concrete. It was conceivable in such a design that you could blow the whole end out. But if you are prestressing the ends, the sides, and everything else, you took away the concrete; it would look just like a bird cage. It is just a complete tangle of wires crisscrossing in every direction, and it is the very epitome of redundancy. Incidentally, some people are used to seeing what happens to concrete in patios, and so forth, and they may get the impression

that it is an unpredictable material. As a matter of fact, it is surprisingly not so. At GA they have recently completed a destructive test on a concrete model; that darn thing failed within one percent of the pressure at which they said it would. Now that is nothing but a coincidence, but it is amazing—LAUGHTER—how close you can get with a bit of luck.

Mr. Benedict: I would like to return to Dr. Häfele's comment that in the case of sodium-cooled fast reactors it would be highly desirable to have some type of excursion test which led ultimately to one of such severity that the reactor was destroyed, like the BORAX experiments. It seems to me that in dealing with some of the questions that arise in overheating of sodium in reactors, notably the unpredictability of the degree of superheating and the unpredictability of the character of the two-phase flow that would result upon boiling, no amount of careful pre-experiment analysis can hope to anticipate exactly what will happen in the reactor. A great deal of very fine engineering and nuclear analysis has been made for the expected performance of these fast reactors, and I am inclined to attach rather more confidence in the results. I think that we know some of the nuclear coefficients better than we think we do. But I am very skeptical about our ability to predict the detailed character of the combined sodium boiling in core and subsequent fuel meltdown when we have to attempt to predict how two or three different phases of materials will interact in a few milliseconds or a few tenths of a second. I don't think we can do that, and I think that we do have to have tests to destruction of a typical, or more than one typical, sodium-cooled fast reactor before we can really be on confident ground.

Mr. Häfele: When I used the term BORAX I was not eager to destroy a reactor. Rather, I wished to make the point that in particular the mechanism of explosion of sodium vapor should be investigated.

Mr. Benedict: I didn't mean to imply that only destructive experiments should be made. I believe that the philosophy which guided the first BORAX experiment, which was to carry out transient experiments of progressively increasing severity, understanding as much as we could from each succeeding experiment, and perhaps ultimately leading to a destructive test, was what was needed in the case of sodium-cooled reactors also. Don't you think so, Dr. Zinn?

Mr. Zinn: Yes, although I recognize the problem is pretty difficult. The sort of experiment you have to do with a sodium-cooled reactor is much more costly than the experiments that have been done with water-cooled reactors.

Mr. Häfele: If I recall properly, by the end of the fifties there was a series of papers investigating the

mechanism of steam explosion in case of water. A parallel action should be necessary to some extent at least in case of sodium vapor.

Mr. Cohen: Let me say that we are *not* going to do this experiment in SEFOR.

Mr. Zinn: I want to lower the intellectual level of the discussion and ask the speakers of this morning what they think of the problem of persuading the utility companies to accept these reactors from the point of view of maintenance, and maintenance in connection with much more minor accidents. I might mention some of them. We would start out with the melting of a single fuel channel, which might be possible in all designs. (It might be possible even during the time of fuel transfer, but even simpler things might be matters of concern.) What if someone drops some objects into a sodium-cooled reactor. How difficult is it to satisfy oneself that this won't be the cause of the melting of a fuel channel? With what we heard this morning, perhaps this is a more serious problem in connection with propagation. How about problems of jammed loading mechanisms and problems of jammed control rods, even just from the point of view of lost operating time and the difficulty of maintenance? Is this likely to be a handicap to the fast reactors? That is really the question.

Mr. Häfele: I think it is an important point, and I tried to bring out this morning during my report, that we did pay a great deal of attention to that point. The loading and unloading schemes were designed in such a way that regular loading would be done by power steering, not by a blind mechanism, but by a feeling mechanism under sodium. The Sodium-I design is such that if something goes wrong the sodium level can be lowered. Optical access is then available and therefore repair or adjustment work would not be too difficult. Further, we tripled the number of loading and unloading mechanisms, all to decrease the probability of failure and to increase the question of availability.

Mr. Fortescue: The one thing that comes up more and more from utilities is the question "can the fast reactor ever be a kilowatt factory in a commercial proposition"? We find much more expression of concern from the utilities as to whether it can be operated by other than by a flock of geniuses. Is it likely to be a system in which some slight fault causes shutdown for a couple of weeks? Things like that worry them far more than the explosions we have been talking about, and I think somewhat justifiably because this is real and the explosions are hypothetical. Indeed, these are the very motives which make us look at gas cooling in the first place. We didn't look at gas cooling to try and make it inherently safer, or to make it a better job at all.

Fundamentally, we looked at it first of all to see whether this approach was one which went in the direction of operational ease of maintenance. For instance, the sort of thing which is extremely attractive to an operator is the thought that he can do repairs to the reactor visually, rather than groping underneath an invisible blanket by remote control. The prospect of being able to flood the reactor with water deliberately is regarded as an extremely attractive thing from the point of view of putting things right. The absence of refueling at frequent intervals was received with major enthusiasm, perhaps beyond what it deserves. The accent on keeping high availability is very great. One of the things that does worry the utilities is the thought that they would forever be down for a couple of days for something that wouldn't hurt fossil fuel at all. Perhaps a similar accident could be put right in ten minutes in a fossil-fueled plant.

Mr. Okrent: I have a feeling looking at your all-enclosed design that there might be trouble with maintenance of your systems, the control-rod drives and the like. Should you get a reasonable number of fuel-element failures, as anyone may expect, what happens to the general activity for maintenance within the pressure vessel? Do you indeed have a simple system to maintain?

Mr. Fortescue: Maintenance of what you might call routine machinery, like blowers and circulators, seems to worry utilities a lot less than maintenance of the black magic part inside the reactor. The specific question is: how do you deal with the maintenance of totally enclosed system? The answer is that the operation is always done when you are depressurized, and furthermore when you have atmospheric air present instead of helium. Maintenance is not supposed to require remote control at all, because the measures taken to allow this are (a) that the entire compartment is fully shielded to the extent that you can work in there, and (b) there is an overpressure put into that area, so there is a deliberate leak of clean helium out of the section into the rest of the reactor, which is passed through a filtered system. This provides a big time factor to shut things down if activity is detected early. But there are measures taken to make sure there is no plate-out on the outside of the reactor. Now there well may be some plate-out on the inside of things, if the reason why you opened up is a melted fuel element or something. The general position is that when it comes to activity on the inside of the boiler tube or something like that, you have to face up to decontamination. But it is a very much different thing than contaminating the outside of something. In general, the acceptance of

the difficulty of getting at an enclosed system generally seems to be that it outweighs the disadvantage. In particular, the ability to see what you are doing in the core region is what is considered so attractive.

Mr. Zinn: I would like to ask the people who have vented fuel whether this gives any problems for maintenance? Do they expect any problems?

Mr. Cohen: There will be a paper on this tomorrow. The answer is that this was one of the first things looked at. Based on experimental results, it does not seem at this time to be a major concern. Concerning the utilities, I think it is fair to say that they are concerned about sodium-cooled reactors, as I imagine they would be concerned about any other reactor type which has had the somewhat checkered history of our sodium reactors. They are, however, people who are very much interested in the basic promise of the system, and I think that they are prepared to consider it honestly. Let me remark that I think that we are now a very highly technological society, even more so than we were, say, 15 years ago, and continually improving. I feel that the methods of reliability analysis, the improved instrumentation, and other electronic techniques which are coming along, including the feedback mechanisms which Dr. Häfele described, are a very good answer to some of the pitfalls that some of our complicated mechanisms have run into. I believe that handling sodium is a problem that a sophisticated society will be able to solve.

Mr. Wright: I agree that there is a lot of very deep concern in the country today. I think it is going to take a lot of demonstration and a lot of getting a hold of this thing called reliability to overcome it.

Mr. Silverman: I would like to ask if it is just the reactor type, or the fact that some utilities have tried mercury boilers and combined this with a heat exchange mechanism. Their experience has been somewhat dubious, so that they are not apt to jump into another sodium system that has more complications.

Mr. Wright: I think that is a negligible part of the problem, if a part.

Mr. Silverman: Well, you may or may not recall that two or three utilities had mercury boilers. The experiences they had with the mercury boilers was such that—they couldn't get rid of them fast enough, as far as experience goes. So liquid metal systems have a bad reputation in the utilities.

Mr. Cohen: I would remark that the people who stayed along with the mercury boilers recently came in for windfall profits when they sold the mercury. (LAUGHTER.)

Mr. Thompson: I would like to change the subject a bit. It seems to me that the design of a fast reactor with a 1000-MW(e) capacity may not, today, be quite realistic. We listened to Dr. Okrent's figures and I agree with Professor Benedict that these are too divergent; if those of you who have made such calculations get together and talk a little, and compare your results with appropriate definitive experiments, this will be clarified. But experience with water reactors have shown that things have gone a little more slowly, that unforeseen problems and challenges have arisen at each step of the growth. These challenges include components problems which you haven't even mentioned here at all. Materials problems have been discussed considerably, as well as reactivity problems, control problems, and so on. The development of the water reactor was aided enormously by the Naval program and even to some extent by the production reactor program in this country. I think there is no question but that water reactors were accelerated a good ten years by the fact that the Navy wanted these very badly. The fast power reactor program in this country has been very nicely and firmly launched right here at Argonne. One of the finest efforts was directed by Dr. Zinn. The Enrico Fermi Reactor has helped immensely. But this program has not had the benefit of the very large expenditure of funds available as had the Naval program. In fact, last night we heard that the total investment in the fast reactor program in this country to date is about comparable to the cost of a single very high-energy accelerator. If I were to guess, I would venture that in the long run the funds put into the fast reactors will bear more fruit in terms of benefit to mankind than they will for the accelerators, even though I still believe that there should be continuing efforts in both directions.

I think it is perhaps unfortunate that people started out to aim at the goal of a 1000-MW(e) plant immediately. Such a plant is as large as the biggest of the proposed water reactors. The reactors that are really on the final design drawing boards now are for about half that size. I would also caution that there aren't a huge number of utilities that could usefully use a 1000-MW(e) plant, at least right now. The fraction of the local load that should be borne by one plant is open to question. It seems to me that one must crawl before he can walk, and walk before he can run. Hence, it seems to me that too strong a selling pitch at this time for a 1000-MW plant may, in fact, be a bad thing for the fast reactor industry. The Congress or perhaps even to some extent the AEC will believe that these very large plants can

be built tomorrow. If the fast reactor design groups of the world do that and then do not build them, the public and the groups with funds may become discouraged and say "it is just another boondoggle." They will start to withdraw funds prematurely. I think this might be even worse than slow and steady progress. It seems to me then that the thing one should do in the next round is to try for a prototype which will work, which will be safe, and which pays less attention to economics.

We have talked very little about the engineering problems. I cannot help but remember that in 1955 everybody then was talking about "prosaic" boiling and pressurized water reactors. But it seems to me that the water reactor technologists have been very busy for ten years trying to get some of these prosaic reactors to run correctly and properly with minimum down time. I therefore say that although I believe in a strong fast reactor program, there are going to be similar discouraging periods for fast reactors. I think we ought to prepare funding groups like Congress and others for the fact that there will be periods of darkness before the dawn.

The 1000-MW studies should be used to delineate quite carefully what the problems are or may be. Much of this is being done. Yet there is always a temptation in paper studies to minimize the problems and come out optimistically, especially under economic competition from other types of reactors and from other bidders for development contracts. The responsibility of building a real reactor will force one to face reality very quickly. With these thoughts in mind I would like to ask each of the participants here, if they were to try to design a reactor of this type tomorrow, what are the four or five principal problems? What are the experiments, the things you would like to have answered, that you feel are essential in order to make a plant of the type you propose work, and work safely?

Mr. Häfele: I think that this comment must be answered from the side of the designers. We gave a great deal of thought as to whether the target of 1000 MW (e) would be correct or not, and I think it is correct for two reasons. First of all, it is that large because we have to identify the area where the problems are, and they are the bigger the larger the reactor is. There is competition between economy, safety, and breeding, which is a triangle, and the triangle is the larger, the bigger the power output is. Therefore, the problems become the clearer the bigger the reactor is. In order to do that and to follow that, it is reasonable to investigate 1000-MW(e) reactors. That is one point.

The second point is that all of us have to remind

ourselves that we are shooting for a flying target. At the earliest the reactor will be ready by 1980. At that time, even in Germany, plans will be for the 1000- or 1200-MW(e) size. Arriving at a reactor which is, by nature, limited say to 600 MW or so, wouldn't make sense. We don't say that we will build a 1000-MW(e) reactor tomorrow, but we do say that after we understand its problems, we will be ready to commit a prototype of 300 MW(e) tomorrow. There is a great big difference, and I think the procedure is logical and reasonable. If you ask what is necessary, what do you want to have, in order to commit a reactor, then I think we also can give at least the main answers. First of all, I would say one should see the big sodium components in operation, safely and cheaply. Secondly, one should have a fuel element able to give 100,000 MW days/ton, but not only with one pin rather than with a high number of pins irradiated in fast flux. Thirdly, one should have had a plutonium critical to resolve these final questions of sodium void. These are the three main areas to our judgment. This is somewhat less difficult for the commitment of a prototype of an intermediate size, but the problems to be attacked should be typical for the larger size.

Mr. Denielou: May I give the answer to the same subject? I think I would place, first, the fuel element having 100,000 MW days/ton; secondly to be sure that there is no propagation mechanism for the melting of the core from one point; thirdly, to have the most reliable steam generators.

Mr. Wright: Although safety and fuel are important and exciting present problems, there are ways around them. We feel that the obtaining of reliable components at anything like a reasonable cost is the very major problem which stands in the way for a long period of time. The system must work as it is designed to work. I don't believe that we are very close to that in the sodium system. I am sure that each of us here should give every attention we possibly can, including a prayer at night occasionally, that the SEFOR thing runs, because that thing has got to run. If that thing doesn't run well, we are all going to be in lots of trouble I believe. In terms of what has to be done, I think carbide fuel is an eventual fuel, but to start out with I would certainly choose an oxide core. I suspect that it would be loaded down with beryllium—beryllia or graphite, so that it could be made safe. I believe that it can be made safe. The potential may not be particularly striking, but that is another economic problem. I would suspect that 50,000 MW days/ton is enough to carry the thing along to where it would be very in-

teresting and very attractive. It is not at all mandatory until you get into, perhaps, the third core to go on to higher burnups. The basic problem is the old unglamorous problem of hardware, and how to glue it together so that it really works.

Mr. Cohen: I agree with the last three speakers. There is no question but that we need a great deal of work and information in lots of areas: physics, fuel, safety, sodium technology, sodium components, major equipment, and instruments. Major components are a major headache. And, of course, SEFOR has to work. I think it is certainly a good idea to take a look at where you are going, and to look at what a 1000-MW plant looks like to be sure you are directing your development program in the proper way. I don't think there is any question about anybody pretending to commit himself to a 1000-MW plant tomorrow morning. I also agree that a smaller plant presents a considerably easier problem.

Mr. Fortescue: I think without a doubt that the development of the fuel is the essential part of the problem. The essential difference between this and the thermal reactors, to my mind, is the economic need for very high burnups in fast reactors, which poses problems not even looked at on thermal reactors. If we are going to achieve 10% burnup, this is certainly going to mean some metallurgical development of no mean order, because it is extremely difficult to achieve these high burnups in the face of a phenomenon like embrittlement. If you are going to have to pass the fuel cans through the plastic-flow region many times during the course of the reactor, there is no doubt that quite a considerable development will have to be acquired to work up to anything like this economic 10% burnup. But fortunately, one has some time to do that because the first thing to do is to get the plant right—the 10% burnup can follow, I think. Of course, in the gas-cooled reactor field we have our other special problems, namely, the prestressed concrete vessel, because if we are going to tie one to the other like that, right there you have got a development problem. Worse than that in a way, you have got an acceptance problem as well. It is difficult to see how this can be other than time-consuming. On the question of size, our impression from talking to the utilities is that 500 MW and upward are just what they want; in fact, it is extremely difficult to sell anything less. I would almost go so far as to say they are more interested in the ability to make it big than the ability to make it economical at the moment. I think possibly one factor in this is that for a utility to accept a reactor at all means sort of practically reorganizing themselves. An entirely dif-

ferent kind of staff might be required, and so forth. If you are going to accept that perturbation, you might as well justify it by a large-scale perturbation of your profits and revenue.

Mr. Hüfele: May I address a question to the panelists? How large, in your judgment, is the credibility of a major sodium pipe break? Is it incredible or is it credible?

Mr. Silverman: It is probably time to take that break now, but I think it is credible.

Mr. Denielou: May I reply to a question we managed to escape. We were asked at what distance from boiling are we going to operate? Let me give the answer now very briefly. We are limited by the cladding temperature of 650°C at the present time. This sets up a limitation and we have not been thinking of going much further.

Mr. Thompson: I should note that members of any committees are speaking here as individuals and their opinions are their own and do not reflect committee opinions. Just to show that this is indeed true, I would say on my own part that the credibility of a major failure of pipe carrying sodium is in my opinion pretty well down the list. I would say that there are many, many other things that are more likely to happen.

Mr. Okrent: I think I will try to get questions from the floor now.

Mr. Zebroski (GE): I have a combination of a question and a comment on the one really substantial new accident that has developed at this conference, that is, the superheating and sodium voiding accident, and I think Dr. Benedict, as a chemical engineer, might want to invent with me a solution for this kind of problem. I think this solution can go along two lines. You can provide an exceptionally hot surface somewhere within the element which would attain sufficient superheat and boil earlier, or you could provide a venturi, which would provide a localized low-pressure region and a lower saturation temperature, or both. This would prevent a coherent wave front of sudden voiding following on the superheating of the entire channel cross section, as postulated in Dr. Benedict's calculation.

I would then observe that most reactor designers spend a great deal of time trying to "disinvent" these solutions to the problem, because we have hot-spot temperatures of the order of 1275°F corresponding to an exit bulk temperature of 1100°F after considerable efforts to minimize hot spots. For a single pin it is easy to discover and calculate peripheral temperature variations greater than 50°F. So I wonder if when we do these tests in a multi-rod

channel configuration (rather than an idealized, thermally symmetrical, single-channel experiment) if it is really likely that we will have a substantial volume of superheated sodium involved. Dr. Benedict suggested this proviso—whether we would have a substantial volume that would superheat and create a large region of voiding instantaneously.

Mr. Benedict: Well, admittedly there is a wide variation in metal surface temperatures, even in the best designed reactor. But it seems to me that the kind of thing which could lead to a rather rapid and rather extensive superheating of sodium would be flow stoppage, which I think is in a class of a credible maloperation. This could occur in such a way that even though there were local points of higher metal temperatures, a substantial volume of sodium could be heated above the boiling point before the hottest point triggered the phase change that can be beneficial if it occurs with just a limited number of BTUs of superheat stored up, and can be very destructive if a substantial number of BTUs of heat are stored up. In line with your search for an invention which would make this a less unfortunate instance, I haven't any particular substance to suggest, but if we could find something which had a limited solubility in sodium and appeared in the vapor phase so that the sodium fizzed a little bit in flowing through the reactor, I think we might have means for catalyzing the information of the second phase almost at every point in the reactor.

Mr. Voice: Fission gases from vented fuel would do this automatically.

Mr. Schluderberg (B&W): Dr. Benedict, may I direct this question to you? Do you believe that in view of all the questions about sodium and other coolants that have been raised today, a coolant can be picked at this time upon which to base the national program? Isn't it possible that maybe we are rejecting a stone now that may become the cornerstone later?

Mr. Benedict: I was going to ask Dr. Häfele that question. I know that you would be interested in his answer to it too. What is the status of the Karlsruhe investigation of steam cooling for fast reactors? What pressures and temperatures of steam does he think would be most economic and most feasible as a third possibility among these fast reactor coolants?

Mr. Häfele: I think the kind of difficulty Schluderberg describes will continue. It will continue to be difficult to choose between different coolants. Therefore, at Karlsruhe we are more or less decided to go ahead with two different coolants, sodium and steam. We no longer are trying for a decision between these

two coolants—rather, we have made the decision to do both in the prototype stage.

Mr. Denielou: Don't you think that the decision about a choice of coolants is going to be taken by the United Kingdom and Soviet Union?

Mr. Häfele: We make our decision by ourselves.

Mr. Cohen: Maybe I should inject a note of levity into this serious conversation by remarking it is often noticed that when people divorce, they end up marrying the same kind of person as the second wife or second husband. What I'm about to suggest is that perhaps all of these problems about sodium are here because we really have studied a great deal about it and know a great deal about it. Now I myself am very much interested in other coolants, and I expect to be continually looking at other reactors forever. I am never going to give up on them. But I think one should be awfully careful about saying "Okay, we have come to some difficulties here, or maybe even a little thorny patch, and so let's just go over on the other side of the hill where the grass is greener." I am afraid that when you start working around there in the same depth you will find some other and different problems. One thing true is that any program to be carried through needs continuity, it needs money, and it needs a lot of people working on it in a dedicated way. I wonder whether you can do a great many programs having these requirements simultaneously without soon finding you can't do any?

Mr. Fortescue: I agree wholeheartedly with Häfele that we should be looking at two coolants, but the only trouble is that I differ on the two coolants. I think we should be looking at sodium and helium as the coolants. I would genuinely like to hear from the steam people what fundamentally they see in steam.

Mr. Häfele: I think there is a big difference between a gas-cooled reactor and a steam-cooled reactor, because a steam-cooled reactor does have a good thermodynamic heat transfer property and you can operate at 650°C cladding temperature. At these temperatures you do have rather good creep strengths, even with somewhat ordinary metals, say stainless steel 1613 or so, or Inconel, which is available today. You don't have to rely on the principle of a collapsed cladding. Whereas applying CO₂ or helium, you would have to apply the principle of collapsed cladding material. And I don't think that you can rely on that principle for a burnup of 100,000 MWD/ton because of all the swelling of fuel inside. That is one big point.

The second big point is that steam cooling might become a natural evolution from present water technology, in particular boiling water reactor technology.

By the same token, it solves the problem of superheat; therefore, it is very natural to consider steam. The steam pressures also should be dictated by the conditions of today's power stations, which use 2,200 psi. Then you can evolve from all this technology.

The third point is that even now I am not convinced that the capital cost of a gas-cooled design is lower than that of the steam-cooled. I would say on the contrary. I admit that in the case of the gas cooling you do have the advantage of the better internal breeding ratio. But this should be balanced against the question of economy, reliability, and experience in the steam field. In doing this, we do arrive at steam and not at CO₂ or helium.

Mr. Fortescue: On the question of the steam being a better coolant, it is a better coolant than helium if it is operated near saturation at the intake to the reactor, or if you go to supercritical pressures. But the essential point in my mind is this—does this do you any good? The point is that in any steam system one wishes to eliminate the boiler. There is a potential capital gain there. But the moment you do this, you accept a new limitation, namely, you must operate the reactor at the same pressure that you choose for the external plant. Now the external plant wants a very high pressure to keep the capital costs down, so the reactor gets given the high pressure. But then this excellence as a coolant disappears completely, because at such pressures the helium would be more than good enough as the coolant. The point is: it seems to me unnecessary to overdo the goodness of a coolant. At 1,000 psi, helium is, in fact, capable of giving you 38 to 40% overall plant efficiency without exceeding the 650°C can temperature which Häfele mentioned. It seems to me that the real issue is not that at all. It is a question of safety. With the steam system you are admittedly still left with the compromises which the gas was trying to free you from in the sodium system, the proof of which is in the lower conversion ratio that goes with the steam. Not only do you have to accept the lower conversion ratio, but you have to accept 4 or 5 dollars worth of reactivity tied up in the coolant. This seems to be typical. Furthermore, you have not got yourself freedom from a void coefficient, so in fact you are again comprising the core shape to achieve adequate void coefficients. I don't honestly believe that the steam has sufficiently removed itself from the problems of the sodium system to justify a small increase in its heat transfer properties which is useless to you if you don't retain the boiler.

Mr. Häfele: I disagree. In the paper by Dr. Dalle-Donne, to be presented tomorrow, there is a thorough

evaluation of the thermodynamic properties in the primary circuit. On the basis of this paper steam is better even if you increase the pressure of helium. You don't have the freedom to do that because it is rather easy to have blowers at 180 atm in the case of steam, but I don't know of any large-scale helium blowers for that pressure. That is just the reason why we have chosen steam, not to have to jump into a new technology; what you propose is virtually a new technology.

Your other argument was that the safety problem would be more difficult. With steam cooling at 180 atmospheres we will have an insignificant positive, or if necessary, also a negative voiding coefficient, provided that there is some plutonium-240 present. The details of this will be described in the paper by Dr. Ott. Therefore, it is not true that there is a large holdup of positive reactivity in the coolant. On the contrary. By the same token we improve the efficiency. Therefore, we see a number of reasons to choose steam, and I cannot agree with your conclusions.

Mr. Okrent: May I ask one small question? I thought that Dr. Fortescue said something about not exceeding 650°C, but in Table I of his paper, it mentions 800°C with a hot-spot allowance.

Mr. Fortescue: Oh, that is the hot-spot allowance. 650°C is the design maximum. It is just the basis of stating it. That goes along with an 800°C hot spot.

Mr. Häfele: I should repeat my first argument: One cannot work with the principle of a collapsed cladding for a burnup of 100,000 megawatt days per ton.

Mr. Fortescue: I think this answer about having secured an adequate coolant void coefficient with the steam has been done at the expense of conversion ratio. That is my point. I think you can make any reactor okay as long as you compromise in some particular direction. Concerning the other point—are we talking about a system with an isolated primary system, or are we talking about what I would call an open system in which you take the steam down to the turbine?

Mr. Häfele: Open primary system, but with a lot of inventions which will be patented and published thereafter.

Mr. Fortescue: I think that is the whole point. It is a question of contrived safety and fundamental safety which is at issue here. For instance, we believe that total enclosement of the primary circuit is an essential for any pressurized fast reactor. That is accepting a limitation. But not only can you not have the prestressed concrete construction with the steam, but what do you do when you break a main

steam pipe down to the turbines? It isn't just the reactivity increase; you have still got to get the heat out.

Mr. Häfele: I am ready to defend our steam design in three to four months on that basis. Publicly.

Mr. Katman (B&W): We've been quoting a lot of void deficits and density deficits throughout the entire conference. Looking at the intercomparison would lead me to believe that we really don't know what we are talking about when we are quoting all of these void deficits and density deficits. There was a 2 to 3% spread in the numbers calculated by all the groups. When you talk about a 2% positive coefficient or a 1% positive and you can have a 3% or a 2% error, then it really doesn't mean too much; you might be positive or you might be negative. Realistically, do we know what we are talking about? I wonder if it wouldn't be a good thing at this point to say all right, now we've got a lot of paper studies; let's go out and measure a few of these density deficits and see if we have meaningful numbers. Maybe in a year or two we can go at it again. I would like to know some of the plus or minuses on the calculated density deficits and void coefficients that the various panel members expect. They must be of the order of 100% of the number.

Mr. Okrent: I believe estimates have been given for some of the uncertainties. Perhaps one or two of the speakers would care to indicate whether their designs would be vitally changed if there were, say, a 1 or 2% difference, by that I mean if the total amount of reactivity gained by sodium voiding were increased by 1% Δk from what they now calculate.

Mr. Cohen: What we tried to do, of course, was to begin with the fact that we didn't know exactly how to calculate these coefficients, and consequently, to make allowances in the design for this. In our safety analyses, as I pointed out this morning, we divided most of our favorable feedback coefficients by 1.5 in order to take care of this sort of variation. I think we all agree that it would be fine to measure these things; on the other hand, when you do a design you are obliged to believe and act consequentially on your numbers, because this in itself is training for what you're going to do when you finally have good numbers.

Mr. Gast (ANL): I want to belabor this void coefficient a little more, not for itself but to lead into something else. The panel, I think, are all agreed that \$7.00 was too much. One member of the panel thought that \$3.00 was too much, but that \$1.60 was all right. If I can put my own interpretation on what I have heard, I believe that in a particular design,

some limit, say X, is chosen for void effect which is acceptable on the basis that if you do have an accident and the safety system doesn't operate, then the consequences of this accident will exceed some limits of tolerance if the value of the void coefficient is above X. It isn't at all clear what these limits of tolerance are. I want to recall also that all of the speakers mentioned almost in the same breath a thousand pounds of TNT as the consequence of a meltdown accident and seem to imply that all of the designs were quite capable of enduring this kind of thing. I wonder if the panel has any comments or viewpoints on what kind of tolerable limits one could set and then use these to determine in some sort of a logical way what is an acceptable void effect in a particular design.

Mr. Häfele: I think there is a basic difficulty in order to do this. Reactivity worths are one question, but these excursions are ramp rate limited. One has to know the time scale. Mr. Fischer and I evaluated the boundaries for the time scale for a given reactivity burst, and there is an uncertainty of the same order. There is even a third difficulty, namely, the equation of state. You are referring to the fact that everybody is saying one thousand pounds of TNT. In our panel contribution, we said it varies between 50 and 500 kg, and only the upper limit coincides with a thousand pounds of TNT. If a different equation of state is used, for example, that of Ackerman, much smaller values come out. All three uncertainties have to be resolved before one can give an upper limit for the reactivity worth of the sodium void.

Mr. Greebler (GE): There has been much emphasis on the virtues of a high internal breeding ratio, and we certainly agree with the advantages that were cited. It might be worthwhile pointing out that there are some disadvantages also. For example, with our reactor, which has an internal breeding ratio of 0.7 forced on us by the sodium void problem, the fuel which has reached 100,000 MWD/t burnup is loafing along at about 80% of the power density of the clean fuel in the reactor. Since you gentlemen all agree that the ability of the fuel to achieve long lifetime at high specific power is a vital feasibility question, it would seem that this is a potential advantage for the lower internal breeding ratio. On the other hand, the 1.1 internal breeding ratio which eliminates the need for burnup control would impose the peak power on the fuel element which is at the highest burnup. I wonder what comments you might have on that.

Mr. Fortescue: There is one little point. One of the side benefits of a high internal conversion ratio is that you can flatten power better because the power

Mr. Bartels remarked that we will build a prototype and after some time we will build another. My experience with the competitive system in this country has persuaded me that reactors are not going to come one at a time in the next round.

Mr. Wright: We believe that the technology can fall into line so that engineering could start on a prototype job in 1969. This will give us a chance to get some results out of SEFOR and assume there will be sufficient program in certain of the other areas. Let's look at the thing this way. In France they have one designer, one builder, one customer. In the Naval program we had maybe a couple of designers, one customer. In Russia there is one designer and one customer, and the UK follows this line pretty well. The reason that we have good successful operating reactors today is twofold. Certainly the Naval program helped tremendously as regards time scale, but basically there was a profit motive here for private industry to invest a good chunk of their money to go into the thing and take the risks. And there are still risks being taken on the contracts being issued in the last three or four months. They are willing enough to go in and take the risks to make the thing move. Now the real problem is: is the fast breeder development program in this country going to become an academic institution? If so, I assure you, the power will never be competitive out of it unless it is on a 3% public power system. In order for fast breeder power to ever get competitive in the United States, we are going to have to have two or more industrial groups capable of competing on it, and this is the only way we are going to get there.

Mr. Wensch (AEC Washington): I'd like to return to the fast reactor Spert-type facility. Some years ago we proposed such a facility to the fast reactor community and received no endorsement that it was needed. Unfortunately, we already had the money and were ready to go with what I considered a program which had been formulated rather carefully and given the greatest amount of consideration. In view of the remarks made this afternoon, Mr. Chairman, I wonder if we couldn't receive recommendations in more detail from Dr. Benedict, Dr. Zinn, Mr. Thompson, and Dr. Häfele.

Mr. Okrent: Were you asking that they try to define this experiment now before the close of the session?

Mr. Wensch: No, but let's not fool ourselves. Building the facility may not be as difficult as really delineating the program. We went through this some years ago, and if the need is here I think we have lost some years. The preparation of a program is very important.

Mr. Okrent: Perhaps I might offer a comment

first. I think, in reference to the earlier consideration of such an experiment, had it been done at that time it would have completely missed this particular phenomenon, since it had not been identified. At this time, I think it is more likely that one can define experiments that will be pertinent to large sodium reactors than it was then.

Mr. Wright: Which is a reason for the 1000-MW studies perhaps.

Mr. Okrent: And for discussions of this kind. If any of the gentlemen on the panel would care to try to outline this experiment in more detail now, we'd be happy to have them do it. If not, could we leave it at saying that more will be done about this.

Mr. Bump (ANL): I'd like to remind the conference that in the proceedings of the Argonne conference of two years ago, a facility such as we are talking about was described in some detail by APDA. I remember that they quoted 50 million dollars for the facility. I don't know if this is the same one Dr. Wensch is talking about or not, but if you add another 50 million to operating the facility and performing the experiments, you get an idea of what is involved in this. I might also add that we have been thinking of doing at least small experiments in a loop in the FARET which would presumably be useful in this area.

Mr. Okrent: I believe the paper at the 1963 conference dealt with something related to the problem of fuel meltdown, and the thought in that particular paper was that one would like to melt of the order of 10 or 15% of the core in a single experiment without melting the rest. I don't believe that particular program had the problem of propagation of an accident in mind, although conceivably the facility for the job might not be very much different. The cost also might not be much different.

Mr. Stachura (AI): I'd like to bring to the attention of the audience some experiments which I mentioned yesterday, and which I think have a very direct bearing on what we have been talking about. There seems to be a great deal of talk about superheating sodium coolant, about a lack of understanding of meltdown behavior, about fuel compaction, as well as a whole range of meltdown and boiling problems in fast reactors. We have done some experiments in TREAT, and I think there are several qualifications that are worth making here. For one, the heating rates that people calculate for large fast excursions due to reactivity insertions that are at all realistic—control-rod ejection, this type of thing—are not at all so fast that the TREAT power profiles cannot produce the type of thermal behavior of the fuel that these calculations predict. In fact, in one

experiment in UC was heated with about 250 BTU's per cubic inch within one second, which corresponds to an adiabatic temperature rise of about 7000–8000°F. The fuel melted, sagged in the cladding, sat there while the sodium bond appeared to have gotten out of the way. The sample was sitting in stagnant coolant, such as exists in a blocked flow condition. The coolant remained at about 1200°F for about another second, and then began to heat rapidly as the fuel contacted the cladding and improved the heat transfer. Finally, upon failure of the cladding, the fuel was thrown, blown randomly, spread throughout the capsule in a very dispersed way by virtue of the fact that cladding failure resulted in contact of this 4000°F fuel with the cold coolant. It most likely radiated to the coolant and produced large local pressures which then produced a dispersion mechanism. I think this dispersion mechanism ought to be considered as a possible means of obtaining negative reactivity effects when fuel is moved from central portions of the melting reactor into regions where the worth is less. One of the significant things is that it is very difficult, when looking at real reactor accidents, to see where the sodium coolant will obtain the superheat at the kind of heating rates that we see and with the kind of reactor cladding materials that we have. It looks as if tremendous heating rates, that are just not obtained in real accidents, must be necessary. Superheats certainly are measured when somebody does a basic experiment with a thermocouple at the metal surface and looks at the fact that a certain energy is required to create the bubble surface. In some cases, superheats of 30 or 50 degrees may have been obtained. I don't know if anybody has measured the very large superheats that are sometimes quoted. Dick Noyes at Atomics International has done a large number of boiling experiments in which he has not seen amounts of superheat larger than 30 degrees. We may be able to use TREAT to establish the importance of this problem. We may be able also to use other heating techniques before we build a large facility to study a large reactor boiling experiment. I think that there certainly is a need to fill the gap in information about meltdown and about transient boiling. But I think there are a lot of experiments, which have not been funded, that are more in this line and that don't require very large expenditures for large facilities.

Mr. Dalle-Donne (Karlsruhe): I think I can answer some of the questions in regard to sodium superheat. In some experiments at Oak Ridge with potassium superheats of almost 200°F were measured. Later, because theoretical calculations indicated that sodium

should have greater degree of superheat, experiments were done with sodium; these are not yet published, but the sodium superheat was so high that they could not boil sodium. The temperature was so high that they could not follow it. So I think the question of superheat is very debatable. There are very different experimental data—I think because this phenomenon depends on causes which are not completely known.

Mr. Segal (APDA): I think it is worth pointing out again that, whatever the uncertainties, at least two countries do feel themselves ready to commence construction of a large fast reactor, and furthermore in the Soviet case, they don't appear to be worried about containment at all.

Mr. Häfele: As an answer, I would like to refer to a paper of Engelmann and mine at the end of the third Geneva conference, where just that point was outlined and explained.

Mr. Wright: I think those of us who have had an opportunity to meet with the Russians at the Geneva conference and in Russia have found that the problem of containment and of reactor design philosophy, safety wise, is a little bit different there from what it is here.

Mr. Thompson: It may be wise to end on a positive note. We should remember that we are today talking in a restricted community of technical people, all involved in one way or another in fast reactors. We are now getting to the part of the work where we really begin to understand the problems. Contrary to the earlier impression that I may have left, I do feel that the 1000-MW studies reported indeed add quite a bit to our understanding in outlining the problem, and I think they were important. I think we should recognize that the next plants won't be those 1000-MW(e) plants, and we shouldn't mislead the general public to believe they will be. But I think we all believe in fast reactors, that they are absolutely essential, that we are coming to grips at last with the problems, and that we should try as rapidly as possible to obtain answers to those various problems.

Mr. Cohen: I would merely like to remark on the question of the experimental proof of the propagation problem. I would like to say again that I believe we should do experiments, but this doesn't mean that we should immediately do them in a reactor. We ought to begin with out-of-pile loops and see whether we really have a problem of reactor dimensions or not. For what it's worth, my guess is we don't.

Mr. Okrent: Are there any other comments on any subject? If not, thank you gentlemen. The meeting is adjourned.

Session V-A
October 13, 1965

Fuels and Fuel Behavior

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Massachusetts Institute of Technology

Secretary: O. L. Kruger
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Metal Fuels for Fast Reactors

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I. Introduction

The successful attainment of 5.7 a/o burnup at a maximum jacket surface temperature of 620°C and of 8.7 a/o burnup at a maximum jacket temperature of 545°C are discussed in this paper; also discussed are what is being done to further this burnup, to improve its reliability, and to reduce its cost. Uranium-plutonium alloys have properties which make them attractive as fuels for fast power breeder reactors: (a) high fertile-fissile atom density, (b) high thermal conductivity, (c) large reliable thermal expansion, and (d) excellent resistance to sodium corrosion. In addition, uranium-plutonium alloys are: (a) relatively easy to prepare (no synthesis problems are involved), (b) easily controlled in composition and impurity level (no stoichiometry problems are present), (c) amenable to pyrorefining, and (d) readily fabricated and remotely refabricated. Uranium-plutonium alloys have some disadvantages, such as: (a) relatively low melting temperature, (b) incompatibility with some otherwise attractive jacket materials, and (c) low irradiation-swelling temperature.

The advantages of metallic uranium-plutonium fuel are substantial enough to warrant a vigorous exploitation. The weak features of this alloy fuel are open to attack via: (a) alloy development and heat treatment, (b) development of superior jacket materials, and (c) application of mechanical restraint for suppression of swelling.

The alloy fuels of primary interest in this paper are uranium-plutonium-fizzium, uranium-plutonium-titanium, and uranium-plutonium-zirconium. Jacket materials associated with these fuels are V-Ti and other refractory alloys, Type 304 stainless steel, and other high-strength iron-base alloys. The fuel design used exclusively in this work consists of an uranium-plutonium alloy fuel pin, jacketed in a loose fitting tube closed with appropriate end fittings. The annular space between fuel and jacket is sodium filled for heat transfer purposes.

II. Summary

In the area of irradiation performance, burnup levels to 5.7 a/o have been reached in prototype fuel

rods of U-15 w/o Pu-10 w/o Fz alloy jacketed in V-20 w/o Ti alloy tubing. Irradiation was performed at significant temperatures: 620°C maximum at the jacket surface and 720°C maximum at the fuel center. A higher burnup of 8.7 a/o has been achieved in the same fuel alloy jacketed in Nb-1 w/o Zr alloy tubing but at lower temperatures: 545°C maximum at the jacket surface and 625°C maximum at the fuel center.

Newer irradiations are being conducted with more advanced metallic fuels. A fuel rod of U-15 w/o Pu-10 w/o Ti alloy jacketed in V-20 w/o Ti alloy has reached 4.6 a/o burnup at temperatures of 790°C maximum at the fuel center and of 570°C maximum at the jacket surface. Another alloy, U-18.5 w/o Pu-14.1 w/o Zr, also jacketed in V-20 w/o Zr alloy, has achieved 4.2 a/o burnup at even higher temperatures: 820°C maximum at the fuel center and 600°C maximum at the jacket surface. These fuel rods will receive further irradiation.

In the area of alloy development, significant progress has been made in the improvement of melting temperature and in the suppression of phase transformation. Titanium additions of 6.5 w/o to U-15 w/o Pu alloy raise the solidus temperature some 300°C above the 820°C solidus for U-15 w/o Pu-10 w/o Fz alloy. The addition of 10 w/o Zr to U-15 w/o Pu also raises the solidus temperature the same amount. This increase in melting temperature of the fuel can be used directly in increasing power density, improving reliability, or increasing fuel diameter. Raising of the melting temperature may also raise swelling temperatures of the fuel.

The prospect of suppressing phase transformations in U-Pu alloys within the operating temperature range of the fuel appears promising in the U-Pu-Ti alloy system. In the binary U-Ti system, a compound U_2Ti is formed which undergoes no phase change below 900°C, at which temperature the compound decomposes into a gamma U-Ti phase. Indications are that as much as half of the uranium can be replaced by plutonium in this compound, but at the expense of transformation.

The most exciting development is the discovery that the higher U-Pu-Zr alloys are compatible with Type

304 stainless steel and form no molten reaction products to temperatures above 800°C, some 200°C above the temperature at which molten phases form in the U-Pu-Fz versus Type 304 stainless steel. This development opens a whole new area of jacket combinations, with great potential from both performance and economic standpoints.

In the sections which follow, details of irradiation performance, alloy development and compatibility, and jacket corrosion resistance for U-Pu fuel alloy and jacket systems are discussed. Nothing will be reported on fuel, jacket, and fuel-element fabrication, as these segments of fast reactor fuel-element program are well developed.^(1, 2)

The fuel systems of importance are:

- a) U-Pu-Fz fuel—V alloy jacket;
- b) U-Pu-Ti fuel—V alloy jacket;
- c) U-Pu-Zr fuel—stainless steel and V alloy jacket.

Other refractory alloy jackets, such as Nb-1 w/o Zr alloy, have been used for proof of principle largely because of availability.

III. Irradiation Performance

Uranium-plutonium-fizzium alloys have by themselves very low resistance to irradiation swelling. Breakaway swelling for these alloys, in general, occurs around 370°C for low burnup.⁽³⁾ Alloy modification within the scope of fizzium additions have not proven

effective in improving resistance to irradiation swelling. Alloys which fall outside the fizzium range may show improved swelling resistance through increased melting temperature and suppression of phase transformation. These are subjects of current investigation.

A positive approach has been taken by Argonne National Laboratory for the control of swelling in U-Pu alloy fuels which does not depend upon developing a cure for the swelling phenomenon itself. The approach is confinement of swelling by jacket restraint.⁽⁴⁾ A jacket must possess adequate strength, ductility, compatibility, corrosion resistance, and irradiation stability to perform this function.

The irradiation of metallic U-Pu fast reactor fuels has been carried out with prototype EBR-II fuel rods having reference cross-sectional dimensions but scaled down in length by a factor of seven. A large part of this irradiation work has been carried out in temperature-monitored and -controlled capsules positioned within the tubular fuel of the CP-5 reactor, where an appreciable fast flux exists.⁽⁵⁾ A sketch of a typical irradiation specimen is shown in Fig. 1. Other irradiations have been with full-length fuel rods in temperature-controlled capsules in the MTR reactor. Irradiations have been initiated in EBR-II on encapsulated fuel rods.

A. High-burnup Specimens

The high-burnup experiment cited previously was designed to explore the effectiveness of jacket wall thickness and plutonium content on the restraint of fuel swelling. Two fuel alloys were used in this experiment, U-15 w/o Pu-10 w/o Fz and U-10 w/o Pu-10 w/o Fz, all fuel pins being 0.144 in. in diameter and 2 in. long. The uranium component of these alloys was enriched 7.7% in the 15 w/o Pu alloy and 15.5% in the 10 w/o Pu alloy. The jackets were Nb-1 w/o Zr alloy tubing, 0.156 in. in ID and 0.015, 0.020, and 0.025 in. in wall thicknesses. Sodium was used in the annuli between fuel and jacket as a heat transfer medium. The six specimens were irradiated in a single capsule for 29 months in CP-5. A calculated burnup of 8.7 a/o was achieved in the higher plutonium alloys by the end of September 1965. Irradiation was carried out at maximum temperatures of 625°C for the central fuel and 545°C for the jacket surface. Time-averaged temperatures were, however, lower, being 495°C for that central fuel and 430°C for the jacket surface. Design parameters and irradiation conditions for this capsule are summarized in Table 1.

As shown in Fig. 2, five of the six pins survived this high burnup without defect or deformation. The jacket of one fuel pin suffered a local fracture at

FIG. 1. Cutaway Drawing of CP-5 Irradiation Specimen Showing Components.

TABLE 1 CP-5 IRRADIATION DATA FOR CAPSULE NO CP 33***

Spec No	Fuel Comp, w/o	Jacket Comp, w/o	Jacket Dimensions		Void Vol, %	Estimated Burnup*		Calculated J Burnup, a/o
			ID, in	Wall, in		a/o U + Pu	f/cm ³ × 10 ⁻²¹	
C 115A	U-10Pu-10Fz	Nb 1Zr	0 156	0 015	22 2	7 1	2 8	5 0
C 116B	U-10Pu 10Fz	Nb 1Zr	0 156	0 020	23 2	7 1	2 8	5 9
C 119A	U 15Pu 10Fz	Nb 1Zr	0 156	0 015	23 3	8 7	3 3	5 3 F**
C 118C	U 15Pu 10Fz	Nb 1Zr	0 156	0 025	23 8	8 7	3 3	9 4
C-117A	U 15Pu 10Fz	Nb 1Zr	0 156	0 015	29 8	8 7	3 3	7 2
C 120C	U 10Pu 10Fz	Nb 1Zr	0 156	0 025	24 2	7 1	2 8	6 7

* As of September 30, 1965

** Indicates jacket failure

*** Maximum temperature at the jacket surface was 545°C and at the fuel center 625°C.

Specimen No.	C-115A	C-116B	C-117A	C-118C	C-120C
Composition, w/o	U-10Pu-10Fz	U-10Pu-10Fz	U-15Pu-10Fz	U-15Pu-10Fz	U-10Pu-10Fz
Burnup, a/o	7.1	7.1	8.7	8.7	7.1

FIG. 2. Appearance of U-Pu-Fz Alloys Jacketed in Nb 1 w/o Zr Alloy after Irradiation to High Burnup

FIG. 3. U-15 w/o Pu-10 w/o Fz Alloy Specimen Jacketed in Nb-1 w/o Zr Alloy Showing Jacket Rupture after 8.7 a/o Burnup

some indicated burnup level between 5.0 a/o when last neutron radiographed and 8.7 a/o when examined. The fuel in the failed rod had extruded from the jacket, as shown in Fig. 3, assuming the shape of the jacket opening. This specimen was jacketed in stress-relieved, 0.015-in.-wall tubing. The two specimens jacketed in annealed tubing of the same wall thickness survived without significant change in external dimensions. Success of the specimen jacketed in annealed tubing might be attributed to higher ductility and perhaps higher creep strength. The range in jacket wall thickness was used to study its effect in attaining high burnup. These experiments indicate, however, that a wall thickness of 0.025 in. is not required for the burnup levels reached here.

This experiment has demonstrated, in principle, the effectiveness of a strong jacket upon restraint of fuel swelling to attractive burnup in spite of the fact that Nb-1 w/o Zr alloy is not presently the preferred jacket. This level of burnup in an U-Pu alloy is the highest ever reported for this type of fuel.

For purposes of comparison, 8.7 a/o burnup in the uranium-plutonium-fizzium alloys used in this experiment produced a fission density of 3.3×10^{21} fissions/cm³. This fission density is equivalent to over 100,000 MWd/T burnup in dense mixed oxide fuel, and to 80,000 MWd/T burnup in mixed carbide fuel.

More recent developments have yielded jacket materials, based upon vanadium, which have features more attractive than those for niobium alloys. Among these features are: (a) better nuclear properties, (b) superior compatibility with fuel, (c) higher mechanical properties, (d) good fabricability, and (e) improved corrosion resistance to sodium.

Irradiation experiments involving V-20 w/o Ti alloy jackets were started in September 1964 upon availability of tubing from ANL alloy and fabrication development. A burnup level of 5.7 a/o was reached in a group of six U-15 w/o Pu-10 w/o Fz alloy fuel rods at the end of September 1965. All specimens were jacketed in tubing of 0.016-in. wall thickness having varying inside diameters ranging from 0.157 to 0.169 in. All fuel used was 0.144 in. in diameter and 2 in. long. The varying gaps between fuel and jacket (filled with NaK) coupled with varying allowable axial expansion of the fuel (clearance

between fuel and top restrainer) were established in these specimens so that jacket failure calculated by formulae developed by Foote⁽⁶⁾ would occur at approximately 3.1 a/o burnup. Details of these design parameters and irradiation conditions for these specimens are shown in Table 2. Irradiations of these specimens were carried out at substantially higher temperatures, 620°C maximum at the surface and 720°C maximum for the center temperature (time-averaged temperatures about 70°C lower), than the previously discussed high-burnup specimens. Upon removal from the reactor at 5.7 a/o burnup, the capsule was radiographed by neutron techniques developed at ANL.⁽⁷⁾ Four of the six fuel rods showed evidence of jacket deformation. These specimens as listed in Table 2 (in Capsule No. CP-40) include those having the largest and those having the smallest annuli between fuel and jacket. The significant feature of these irradiations is the fact that two of the specimens certainly far exceeded, and the others probably exceeded, the calculated achievable burnup.

Another capsule of fuel specimens has been irradiated to 4.0 a/o burnup at a maximum surface temperature of 630°C and a maximum fuel center temperature of 740°C (time-averaged temperatures about 75°C lower). Plutonium content was varied in these specimens as well as the gap between fuel and jacket, the gap between fuel and upper restrainer, and the void space above the bond. Both U-15 w/o Pu-10 w/o Fz and U-10 w/o Pu-10 w/o Fz alloy fuels, 0.144 in. in diameter and 2 in. long, were used. The annuli between fuel and jacket in these rods varied from approximately 0.006 to 0.012 in. Fuel jackets of V-20 w/o Ti were used, all having 0.016-in. wall thicknesses. The design of these specimens was such that the calculated burnup at failure would be 6.0 a/o. Design and operating data for the specimens, all in Capsule No. CP-41, are given in Table 2. Neutron radiography of the capsule at 4.0 a/o burnup has indicated jacket deformation in one rod, a 10 w/o Pu alloy.

The burnup already reached in many of these irradiation experiments are at levels attractive for fast reactor fuel systems projected for large-scale plants. The conditions under which these experiments are being conducted, in water-cooled thermal reactors, are

TABLE 2. CP-5 CAPSULE IRRADIATION DATA FOR V-20 w/o Ti JACKETS OF 0.016-IN. WALL THICKNESS

Capsule No.	Specimen No.	Fuel Comp, w/o	Jacket ID, in.	Restrainer above Fuels, in.	Void Vol, %	Max Temp, °C		Estimated Burnup*		Calculated J Burnup, a/o
						Jacket Surface	Fuel Center	a/o U + Pu	f/cm ² × 10 ⁻²¹	
CP-40	N1	U-15Pu-10Fz	0.157	0.001	23.5	620	720	5.7	2.2	3.1 F**
	N2		0.157	0.010	23.6	620	720	5.7	2.2	3.1
	N3		0.165	0.010	26.5	620	720	5.7	2.2	3.1 F
	N4		0.161	0.001	25.0	620	720	5.7	2.2	3.1 F
	N5		0.161	0.010	25.0	620	720	5.7	2.2	3.1
	N6		0.169	0.010	27.5	620	720	5.7	2.2	3.1 F
CP-41	N7	U-15Pu-10Fz	0.165	0.001	41.1	630	740	4.0	1.5	6
	N8		0.161	0.010	39.9	630	740	4.0	1.5	6
	N9		0.157	0.018	36.7	630	740	4.0	1.5	6
	N10	U-10Pu-10Fz	0.165	0.001	41.1	630	740	4.0	1.5	6 F
	N11		0.161	0.010	39.9	630	740	4.0	1.5	6
	N12		0.157	0.018	36.7	630	740	4.0	1.5	6

* As of September 30, 1965.

** Indicates possible jacket failure.

in some instances more severe than those anticipated in a sodium-cooled fast reactor. During the course of irradiation, the specimens are subjected to many and violent thermal cycles, occasioned by reactor scrams and scheduled shutdowns. The number and magnitude of thermal cycles the fuel would receive in a sodium-cooled fast reactor would be substantially less. The severity of irradiation damage to the jacket material is probably substantially less in CP-5 reactor on a comparable temperature level because of the much lower dose sustained for equivalent burnup.

B. Advanced-fuel Irradiations

One characteristic common to most of the failures experienced with U-Pu-Fz alloys is the presence of what appears to be once-molten fuel. Whether this apparent melting occurred before or after jacket rupture cannot be determined from these experiments. In either case, however, increasing melting temperature of the fuel would be helpful.

The development work with advanced fuel is based, in part, upon the desire to increase the melting temperature of U-Pu alloy fuels.

Two alloying elements have been selected from

those that raise the melting temperature of the binary U-Pu alloys. The two more potent elements and the least objectionable from the physics standpoint are titanium and zirconium. Irradiation experiments have been initiated to determine the irradiation stability of fuel rods designed according to calculations to reach about 10 a/o burnup. To obtain a high burnup rate (being limited by the heat transfer capability of the capsule) the capsule contains only a single fuel rod. One capsule contains an U-15 w/o Pu-10 w/o Ti alloy fuel pin jacketed and NaK bonded in 0.015-in.-wall, V-20 w/o Ti alloy tubing. The other capsule is loaded with one rod of U-18.5 w/o Pu-14.1 w/o Zr alloy also jacketed in V-20 w/o Ti alloy tubing. The fuel pins are 0.144 in. in diameter and 4 in. long. The inside diameter of the V-Ti alloy jacket is 0.173 in. for the U-Pu-Ti fuel and 0.175 in. for the U-Pu-Zr fuel, leaving annuli of 0.0145 and 0.0155 in., respectively.

These fuel rods have been irradiated at a substantial rate of about 0.7 a/o per month in CP-5. As shown in Table 3, the U-Pu-Ti alloy rod had reached 4.6 a/o burnup at the end of August; the U-Pu-Zr alloy rod had reached 4.2 a/o burnup. Ir-

TABLE 3. CP-5 CAPSULE IRRADIATION DATA FOR FUEL JACKETED WITH 0.015-IN.-WALL V-20 w/o Ti

Capsule No.	Spec. No.	Fuel Comp, w/o	Jacket ID, in.	Restrainer above Fuel, in.	Void Vol, %	Max Temp, °C		Estimated Burnup*		Calculated Burnup, a/o
						Jacket Surface	Fuel Center	a/o U + Pu	f/cm ² × 10 ⁻²¹	
CP-44	N14	U-15-Pu-10Ti	0.173	0.59	64	570	790	4.6	1.7	9.6
CP-45	N15	U-18.5Pu-14.1Zr	0.175	0.59	73	600	820	4.2	1.4	10.0

* As of September 30, 1965.

radiation temperatures are substantial, being 570°C maximum at the surface and 790°C maximum at the fuel center for the U-Pu-Ti alloy, and 600°C maximum at the surface and 820°C maximum at the fuel center for U-Pu-Zr.

Neutron radiographs of both capsules after CP-5 shutdown indicates that the jackets are intact on both fuel rods. The U-Pu-Ti alloy rod, however, has elongated about 15% to the restrainer, indicating the need for a restrainer to control fuel length. The U-Pu-Zr fuel rod has developed two narrow bands of density reduction, one near the top of the rod and the other somewhat below the center, with some increase in length. The significance of these areas is not known.

The burnup levels reached in these fuels on first attempts are very encouraging, particularly since irradiation temperatures are high for metallic fuels. Both of these capsules were to be returned to the reactor for further irradiation.

IV. Fuel-alloy Development

A. Elevating Melting Temperature

The initial objectives of the uranium-plutonium alloy development are simple and straightforward: (a) to increase the solidus temperature about 300°C above that for U-Pu-Fz alloys, and (b) to suppress phase transformations within the operating temperature range of the fuel. The melting temperature has direct effect upon the thermal performance of a fuel since melting for all intent and purpose limits the

top temperature at which the fuel can be used. The higher the temperature, the higher the power rating for a given fuel and fuel geometry. Melting temperature could also indirectly effect the swelling characteristic of the fuel.

The U-Pu-Fz alloys were taken to be the equilibrium alloys containing fission product elements not removed by the melt-refining process⁽⁸⁾ now being used to pyrorefine a similar U-Fz alloy for EBR-II. The alloy designated 10 w/o Fz contains 2.80 w/o Zr, 2.75 w/o Mo, 2.97 w/o Ru, 0.50 w/o Rh, and 1.00 w/o Pd in the synthetic form. Technetium will be present in the fission-produced alloy. It has been replaced by equivalent amounts of molybdenum and ruthenium in the synthetic alloy. Severe restrictions have been placed upon the operating conditions under which the fission fuels can be used because of its low solidus temperature, 820°C for U-20 w/o Pu-10 w/o Fz, 865°C for U-15 w/o Pu-10 w/o Fz, and 910°C for U-10 w/o Pu-10 w/o Fz.

A number of elements were known or suspected to increase the melting temperature of U-Pu alloys. These include molybdenum, niobium, scandium, titanium, yttrium, and zirconium. Ternary alloys of each of these elements with uranium and plutonium were prepared and screened for their effectiveness in increasing the solidus temperature. Of this group, zirconium and titanium were found to be most effective. It is fortuitous that these elements are the least undesirable from a physics standpoint.

The effect of zirconium and titanium additions upon the solidus temperature of U-Pu alloy is shown in Figure 4. It is evident that U-Pu-Zr and U-Pu-Ti alloys have much higher solidus and liquidus temperatures than do equivalent U-Pu-Fz alloys, as is shown in Fig. 5.

In comparison, an U-10 w/o Pu-10 w/o Zr alloy starts to melt at approximately 1150°C, whereas an U-10 w/o Pu-10 w/o Fz alloy begins to melt at 910°C. This increase in solidus temperature can be put to good use in the application of this fuel.

B. Suppression of Phase Transformation

Phase transformations directly affect the thermal stability of a fuel system because abrupt dimensional (density) changes that take place upon transformation contribute to performance limitations. Phase transformations could also markedly increase the swelling rate of fuel. For these and other reasons phase transformations are to be avoided or at least made as sluggish as possible.

Phase transformation can be suppressed by adding alloying elements which slow the rate of transformation sufficiently to prevent their taking place within

FIG. 4. Effect of Zirconium and Titanium on Solidus of U-Pu Alloys.

FIG. 5. Schematic Comparison of Phase Fields of Some U-Pu-based Alloys.

the life of the fuel. Although some elements retard transformation, none have been found which are sufficiently potent to be effective in temperature ranges of interest in reactor operation. Molybdenum and the fission elements are examples of those which retard transformation.

Another method of suppressing phase transformation is by alloying into a phase field which displays no transformation in the temperature ranges of interest. This stable form is the one of interest. The U-Ti alloy has this feature of interest in a useful composition range. In this alloy system, there is a compound, U_2Ti , having a hexagonal structure which is stable up to about 900°C. Above this temperature this compound transforms into a gamma-phase uranium. The U_2Ti compound exists over a narrow composition range. Plutonium can be substituted for about one-half of the uranium in this compound,⁽⁹⁾ but at the sacrifice of lowering the upper temperature in the range for which the compound exists. Alloys containing less titanium than the compound amount consists of $(U,Pu)_2Ti$ and U-Pu alpha phase and a small amount of zeta phase.

C. Physical and Mechanical Properties

The most pertinent properties have been determined for U-Pu-Zr and U-Pu-Ti alloys of interest. The major influence on properties by additions of zirconium and titanium is the increase in melting temperature of these alloys over that of the binary

U-Pu, as has been discussed above. The microstructure of these ternary alloys, of course, is much simpler than that for U-Pu-Fz alloys.

Thermal cycling of U-Pu-Zr and U-Pu-Ti alloys shows these alloys to be very stable to this treatment. Only very small density and dimensional changes occur after 100 cycles between 450°C and 700°C at a transfer rate of 10 min. This cycle was designed to simulate normal reactor operation and not a scram, however. Density and dimensional changes are slightly less than those measured for U-Pu-Fz alloys and substantially lower than the changes developed in unalloyed uranium and the binary U-10 w/o Pu alloy.

Based on preliminary data, the thermal conductivities of equivalent U-Pu-Zr and U-Pu-Fz alloys appear to be similar. Alloys of higher zirconium content have somewhat lower conductivities. As was expected, the $(U,Pu)_2Ti$ compound and the U-10 w/o Pu-8 w/o Ti alloy have the lowest conductivity of the metal fuels measured.

Mechanical properties of U-Pu-Zr and U-Pu-Ti alloys have been found to compare with those of U-Pu-Fz. Uranium-plutonium-base alloys are weak at elevated temperatures, having ultimate tensile strengths of 2 to 7 kg-mm⁻² between 600 and 700°C. Room-temperature properties, however, can be high and can vary widely depending upon the alloy and its history.

Creep strengths of U-Pu-Ti and U-Pu-Zr alloys

TABLE 4. CREEP IN U-Pu-BASE ALLOYS (TIME IN MINUTES TO ATTAIN 1% STRAIN)

Stress (kg-mm ⁻²)	U-15 w/o Pu-10 w/o Ti			U-18.5 w/o Pu-14 w/o Zr		
	600°C	650°C	700°C	600°C	675°C	700°C
0.28	—	210	64	—	94	70
0.42	170	35	30	3.8×10^4	27	32
0.56	120	30	17	3.7×10^4	13	12

FIG. 6. Diffusion Penetration of U-10 w/o Pu-10 w/o Fz into Prospective Claddings at a Constant Time of 42 Days.

are very low above 600°C, in the temperature range of most interest in reactor operation. These low creep rates are indicated in Table 4. The abrupt change in rate experienced by U-18.5 w/o Pu-14.0 w/o Zr alloy between 600 and 675°C can be associated with a phase transformation which takes place in this temperature range. These low-level mechanical properties can be used to advantage if enhanced performance can be realized from the weak fuel-strong jacket concept. The fuel in a weak condition can conform to the will of the jacket more readily than can a strong fuel.

D. Compatibility of Fuel and Jacket

Compatibility between fuel and jacket is a very important, if not a limiting, factor in the development of high-performance, high-burnup fuel systems. Extensive work has been done at ANL to determine and to understand interactions between fuel and jacket materials appropriate for metallic U-Pu fuel systems. Extensive investigation has been made with U-Pu-Fz fuels, employing diffusion couples with various jacket materials in the temperature range from 550 to 750°C.^(10, 11) Interpenetration between fuel and jacket has been determined principally by metallographic means, with checks made by microprobe analysis.

As shown in Fig. 6, V-Ti alloys, molybdenum, and chromium are most compatible with U-10 w/o Pu-10 w/o Fz fuel of the materials studied. Type 304 stainless steel and Hastelloy-X are considerably poorer in behavior, with Nb-1 w/o Zr alloy being the worst. All these materials follow the parabolic rate law except Nb-1 w/o Zr and Nb, which follow a linear behavior.

For all intents and purposes, iron- and nickel-base alloys must be excluded from reactor use with the fission alloy fuels because of the low melting temperature of the reaction products. For example, molten phases developed in Type 304 stainless steel versus U-Pu-Fz and Hastelloy-X versus U-Pu-Fz at temperatures between 650 and 675°C. This temperature is much too close to reactor operating temperatures contemplated to be tolerated. Extrapolation of the penetration data obtained to longer periods yields the one-year penetration levels shown in Table 5. These extrapolations show V and V-Ti alloys to be superior to all others except chromium, for which limited data are available.

Study of the penetration data indicates that approximate rates may be predicted from examination of the binary phase diagrams of the major elements

TABLE 5. PENETRATION OF U-Pu-Fz ALLOYS INTO POTENTIAL CLADDING MATERIALS AT VARIOUS TEMPERATURES EXTRAPOLATED TO ONE YEAR (cm $\times 10^4$)

Cladding/Fuel (w/o)	550°C	600°C	650°C	700°C	750°C
V-20Ti/U-10Pu-10Fz	3*	6	15	30	75*
V-20Ti/U-15Pu-10Fz		6*	15	50*	100*
V-10Ti/U-Pu-10Fz	4	10	20		
Cr/U-10Pu-10Fz				30*	
Mo/U-10Pu-10Fz		10	15	25	
Mo/U-15Pu-10Fz		15*	25*	35*	
304SS/U-10Pu-10Fz	15	25	60		
304SS/U-15Pu-10Fz			100*		
Hast-X/U-10Pu-10Fz	25	50	110		
Hast-X/U-15Pu-10Fz			190*		
Nb/U-10Pu-10Fz	20	45	700		
Nb/U-20Pu-10Fz	30	75	1300		
Nb-1Zr/U-10Pu-10Fz	25	35	600		

* Extrapolated from a single point.

in the fuel and jackets. Some general indications are that phase systems which have limited solubility, particularly on the jacket side of the system, and form no intermetallic compounds have good compatibility. Conversely, fuel-jacket systems which have extensive solid solubility or which form intermetallic compounds appear to have poorer compatibility.

As was stated earlier, the most exciting feature of the advanced fuel work is the discovery of relatively high compatibility temperatures of U-Pu-Zr alloys with Type 304 stainless steel. Whereas the U-10 w/o Pu-10 w/o Fz alloy forms a molten phase with stainless steel at temperatures slightly above 650°C, zirconium alloys having even higher plutonium contents do not form a molten phase with

Type 304 stainless steel until a temperature of 850°C is approached. This discovery opens a whole new area of otherwise very desirable jacket materials for consideration of use with these alloy fuels.

In addition to raising the temperature at which reaction products between fuel and jacket melt, these zirconium alloys show very low penetration rates into stainless steel at normal operating temperatures (600–700°C). For example, test results at 650°C predict less than 10μ penetration by U-18.5 w/o Pu-14.0 w/o Zr alloy into Type 304 stainless steel in one year. This penetration rate increases slowly with temperature, but is still relatively low at 800°C (less than 100μ) in one year. These changes in rate with temperature are shown metallographically in Fig. 7. Changes in fuel structure in the diffusion

FIG. 7. Compatibility of 304 SS with U-Pu-based Fuels (compositions in w/o) after 7 Days at Temperature.

TABLE 6. PENETRATION OF U-Pu-Zr FUEL INTO 304 SS, HASTELLOY-X, AND INCOLOY-800 CLADDING

Cladding	Temp (°C)	Penetration (cm $\times 10^4$)								
		U-16.6 w/o Pu-6.3 w/o Zr			U-15 w/o Pu-10 w/o Zr			U-18.5 w/o Pu-14.1 w/o Zr		
		7 Days	17 Days	42 Days	7 Days	17 Days	42 Days	7 Days	17 Days	42 Days
304 SS	650							<1		
	700		8			2		1	2	6
	750	Melted*			4			2		
	800							9		17
	850							Melted*		
Hastelloy-X	700		50			10				12
	750	Melted*			Melted*					
	800							Melted*		Melted*
Incoloy-800	750				16		38			
	800				Melted*					

* Melting of the reaction products and accelerated penetration occurred.

couples at higher temperature can be attributed to the fuel being held in the gamma phase.

Compatibility of U-Pu-Zr alloys with some alloys of higher strength have been studied. Hastelloy-X and Incoloy-800 are inferior in both rate of penetration and temperature of melting when compared with Type 304 stainless steel. Data for these systems are given in Table 6. The conclusions to be drawn from results with these couples are: (a) penetration rate decreases and melting temperature increases with increasing zirconium content in the fuel; (b) higher nickel content in the jacket material decreases compatibility.

A number of alloys of higher strength are now under study to determine what levels of nickel, and also of cobalt, can be tolerated while maintaining compatibility: Haynes #56, 16-25-6, 16-15-6, and N-155. Early results with N-155, which contains 20 w/o Ni and 20 w/o Co, indicate satisfactory compatibility at 750°C.

The mechanism that leads to the excellent compatibility between U-Pu-Zr alloys and iron-base alloys is under investigation. Further work is needed to correlate this effect with observation made by others⁽¹²⁾ which show complex diffusion paths are taken in some ternary systems. Experiments are under way with a simplified U-Zr versus Fe system. Melting has been prevented to 800°C in uranium alloys containing greater than 9.7 w/o Zr. Indications are that the addition of zirconium avoids regions having molten phases in the binary U-Fe system and that improvement in compatibility is indeed due to the zirconium.

V. Development of Vanadium Alloys

Vanadium-base alloys possess many qualities required of a good jacket material for metallic U-Pu

fuels. They have acceptable nuclear properties, have excellent compatibility with U-Pu alloy fuels, have good strength, and can be fabricated into high-quality tubing without resorting to exotic methods. The only point in question (aside from verifying its ability to restrain high swelling U-Pu alloy fuels) is its resistance to corrosion by sodium.

The corrosion behavior of vanadium and vanadium-base alloys in sodium at 650°C is very sensitive to the oxygen content of the alkali metal (other contaminants may also effect corrosion rates). Variation in the oxygen content of the corroding sodium from 50 ppm to hot-gettered metal, believed to be under 5 ppm oxygen, changes the corrosion behavior of V-Ti alloys from a linear weight loss at the high-oxygen end to parabolic weight gains in hot-gettered sodium.

The corrosion data for V-20 w/o Ti alloy establishing the parabolic rate are plotted in Fig. 8. These low rates have been substantiated by metallographic examination and hardness traverses made on sections through test samples. After 323 hr the surface oxide phase was less than 1 μ thick. X-ray diffraction pattern indicated the oxide to be single-phase VO₂, with titanium present. Total contamination in the metal evaluated by hardness traverse indicated oxygen penetration to be less than 25 μ .

A parabolic rate constant was determined from the corrosion data and found to be 2.763 (mg/cm²)² hr⁻¹.

The corrosion resistance of V-Ti alloys is not as good as originally believed; however, the performance of V-20 w/o Ti alloy in low-oxygen sodium holds promise, particularly in view of purity levels indicated in the primary sodium systems in EBR-II and Fermi, and in the NaK system at the Dounreay Fast Reactor.

FIG. 8. Weight Change of V-Ti Alloy in Hot-gettered Sodium.

Accelerated screening tests in sodium containing oxygen levels of about 50 ppm indicate improved resistance of other alloys. On a comparative basis, higher titanium alloys up to 50 a/o Ti and some ternary V-Ti-Cr alloys exhibit lower corrosion rates than V-20 w/o Ti alloys.

Metallurgical structure has also been found to affect corrosion rate. Annealed V-Ti alloys (for one hour at 900°C) have shown significantly lower total penetration than the same material in the cold-worked condition. Alloy composition and heat treatment are two areas which will be exploited.

VI. Conclusions

The effectiveness of refractory alloy jackets in restraining high-swelling U-Pu alloy fuels has been demonstrated to high burnup levels of 8.7 a/o. This achievement in itself is substantial, but is even more impressive when compared with burnup levels achievable only a short time ago. Two years ago burnup levels of 1.6 a/o could not be exceeded in U-20 w/o Pu-10 w/o Fz alloys. From that time to the present a fivefold increase in burnup level has been achieved through: (a) modification of design, principally increased wall thickness, increased void space to accept sodium displaced from the bond, and increased void space into which fuel can swell, and (b) substantial improvement in quality of the jacket tubing, mostly resulting from ANL developments in tubing fabrication.

The burnup levels already successfully achieved in metal fuels are considered adequate for economic competition as fuels for the fast reactor.⁽¹³⁾ This burnup level, however, must be duplicated in a truly fast environment and repeated to develop confidence.

The prospects that metallic fuel specimens now in preparation will exceed burnups of 10 a/o are very good by virtue of the effectiveness of zirconium and titanium in raising the melting temperature of uranium-plutonium alloys. The compatibility of U-Pu-Zr alloys with stainless steel is a further promising development since it can effectively improve the economics and safety of metallic fuel systems, and may improve performance.

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Discussion

Mr. Weinberg (ORNL): I wonder if from your experiments you have any way of telling whether the cladding materials undergo the high-temperature embrittlement which is on the minds of so many people, such as the helium-induced embrittlement?

Mr. Macherey: The experiments conducted so far

have been in a thermal environment. Although this is not really indicative of the exposures the material will get in fast environment, at these burnup levels and dose rates the materials appear to have appreciable ductility at the end of exposure. We don't have definitive experiments at this point which would indicate the change in properties of the material.

Mr. Weinberg: You say this because the cans did not split?

Mr. Macherey: Yes.

Mr. Weinberg: Even though they were under a fair amount of stress?

Mr. Macherey: Yes.

Mr. Weinberg: Do you have any idea of the boron contents of your cladding?

Mr. Macherey: No. These experiments were run with refractory metal jackets principally, and not many of these have been analyzed prior to this point.

Mr. Weinberg: Perhaps I could turn the question around and ask: do you understand why your jackets don't show this high-temperature embrittlement?

Mr. Macherey: No; I wouldn't care to guess.

Mr. Kummerer (Karlsruhe): What was the maximum or average power in the pins? Also, what was the basis for your calculations for burnup in relation to swelling?

Mr. Macherey: The burnup calculation relates the pressure developed by gaseous fission products upon the jacket under conditions where no restraint is assumed from the material itself. By means of the ideal-gas equation $PV = nRT$ the amount of fission

gas generated in the fuel was correlated with the void volume available initially in the pin. As the fission gases form, the fuel expands and applies pressure on the jacket. The resulting pressure is a function of the amount of gas produced and the amount of space available in the fuel.

Mr. Kummerer: For this calculation, you assume given strength of the material in this temperature?

Mr. Macherey: Yes, around 30,000 psi or perhaps a little higher for Nb-1 w/o Zr and a little bit less for the V-Ti jackets.

Mr. Kummerer: I would be interested in your answer about the power.

Mr. Macherey: In the high-burnup specimen at the low temperature, the power density was equivalent to, or say, calculated to 7.6 kW/ft for the titanium specimens and to 12.8 kW/ft for the zirconium alloy.

Mr. Campise (AI): In discussing the corrosion resistance of V-20 w/o Ti you mention an oxide buildup on the surface. I was wondering whether in the presence of a high-intensity fast-neutron flux this surface protection that you are getting would be affected or stripped off by high-energy neutrons and therefore increase the corrosion rate of the clad.

Mr. Macherey: It is possible, but I think that most definitive experiments aimed at improving or accelerating reaction rates haven't been particularly fruitful. Stress in the jacket could certainly affect this, as well as fission fragments. We do have experiments underway in EBR-II to determine effects of neutron environment on this corrosion rate.

Compatibility of Modified Carbide Fuels with Stainless Steel

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I. Introduction

The monocarbides of uranium and plutonium are potential reactor fuels. The high density of heavy metal atoms in the monocarbides recommends use of a mixture of the two in fast breeder reactors (FBR), especially since the high thermal conductivity of these compounds and their admixtures permits operation at high power density. To utilize this latter advantage fully, fuel-element designs incorporating carbide materials include a liquid-metal thermal bond, e.g. Na or NaK, at the fuel-clad interface.

A factor limiting the utility of carbide fuels is incompatibility with stainless steel clad materials. Control of the carbon content in contemporary procedures used to prepare uranium, plutonium, or mixed uranium-plutonium monocarbide is limited to ± 0.1 w/o. Since uranium and carbon have negligible solubility in the monocarbide,⁽¹⁾ fuels fabricated from nominally stoichiometric compositions invariably include a secondary phase. The secondary phase, free metal in the case of less than stoichiometric carbon contents (hypostoichiometric) and higher carbides (e.g., U_2C_3 , Pu_2C_3 , or UC_2) in the case of greater than stoichiometric carbon contents (hyperstoichiometric), contributes to incompatibility with stainless steels. The incompatibility is exaggerated in liquid-metal-bonded systems wherein the liquid metal may act as a vehicle to accelerate reaction.

The free metal present in hypostoichiometric compositions may react with iron-base-alloy cladding materials. The product of this reaction with uranium metal is a eutectic composition melting at 727°C .⁽²⁾ If plutonium metal is present, the eutectic composition may melt as low as 425°C .⁽³⁾ The presence of a eutectic melt will greatly accelerate the rate of subsequent reaction and contribute to clad failure. Accordingly, the free metal present in hypostoichiometric compositions is dangerous or, at best, limiting.

The mechanism of clad interaction with hyperstoichiometric compositions involves decarburization of the fuel and subsequent carburization of the clad alloy.⁽⁴⁾ The resulting embrittlement and reduction in

the ductility of the clad material will contribute to premature clad failure. A liquid-metal thermal bond will accelerate the progress of these reactions. Liquid sodium will decarburize the higher carbides in hyperstoichiometric compositions and the carbon will precipitate in the clad alloy. The rate of this reaction is prohibitive above 540°C .⁽⁵⁾ An oxygen-contaminated thermal bond will further accelerate the progress of carburization, even in nearly stoichiometric compositions, through oxidation of the carbide.

Considerable experimental effort has been expended in attempts to synthesize single-phase monocarbides. In the case of uranium monocarbide, UC, these studies have sought to expand the range of carbon contents over which the single phase can be obtained. Alloying additions which themselves form stable monocarbides over a range of carbon contents, e.g., zirconium carbide, have been made to UC.⁽⁶⁾ No system has been identified that does not result in prohibitive dilution of the fission-fertile atom density.

The Westinghouse Atomic Power Division has postulated a significant departure from the usual goal of a single-phase fuel to improve the compatibility of carbide fuels with stainless steel.⁽⁷⁾ Elemental or compound modifying additions may be made to off-stoichiometric compositions to react or combine with the normally occurring secondary phases. The resultant polyphase composition contains secondary phases more stable and refractory than the uranium, plutonium, or mixed uranium-plutonium metals or their higher carbides. The required addition should satisfy the following criteria:

- The addition should effectively combine with the secondary phases initially present, forming a stable compound that precludes formation of low-melting eutectics with the cladding constituents.
- The phases present in the final modified composition should have high melting points.
- The final secondary phases must be compatible with the monocarbide.
- Uranium and plutonium should be mutually sol-

FIG. 3. Enlarged Area of the U-C-Fe System Showing How Iron Additions May Be Made to Initially Hypostoichiometric Uranium Carbide to Modify the Final Phase Composition.

comparable to that of the stoichiometric monocarbide is expected.

Figure 5 shows the dilution effects and relative volume of secondary phases introduced by the modifying additions as a function of the initial carbon content of uranium monocarbide. In each instance these effects are minimal and tolerable.

An experimental program is currently in progress at WAPD with the objective of (a) demonstrating that off-stoichiometric uranium monocarbide can be modified to the phase compositions cited above by the addition of Fe, Cr_{23}C_6 , UFe_2 , or Cr as indicated, and (b) to prove that the so-modified carbide compositions possess the hypothesized improved compatibility with stainless steels.

II. Experimental Procedures

A. Materials

Finely divided ($<44 \mu$) hypostoichiometric, depleted uranium carbide powders were obtained from the Vitro Laboratories. The powders were prepared by milling the product of a high-intensity arc process.⁽¹⁰⁾ Two powders (WAPD-1 and -2) varying in effective carbon content were obtained. The results of the vendor's chemical and spectrographic analyses are given in Table 1. Compositions with carbon contents between WAPD-1 and -2 were prepared by mechanically mixing weighed fractions of WAPD-1 and -2 in an agate mortar and pestle.

FIG. 4. Enlarged Area of the U-C-Cr System Showing How Cr_{23}C_6 Additions May Be Made to Initially Hypostoichiometric Uranium Carbide to Modify the Final Phase Composition.

Iron^(a) and chromium carbide (Cr_{23}C_6)^(b) were prepared as powders with less than a 44μ particle size. Other elements specifically sought but not found or present in quantities below the limits of detection of the method used include:

Ag, Al, Ar, Au, B, Ba, Be, Bi, Ca, Cd, Co, Cr, Ga, Ge, Hf, In, Li, Mg, Mn, Na, Nb, Ni, Os, Pb, Pt, Rh, Ru, Sb, Si, Sn, Sr, Ta, Te, Tl, Ti, V, W, Zn, Zr.

B. Pellet Fabrication

Pellet fabrication was carried out in a modified, inert gas weld box, continuously purged with dry argon. The oxygen and moisture contents of the box effluent were continuously monitored. Fabrication procedures were terminated when the level of either of these constituents in the effluent exceeded 40 ppm. A contaminated box atmosphere was removed by evacu-

FIG. 5. Volume of Secondary Phases Contributed by Iron and Chromium Modifying Additions to Initially Hypostoichiometric Uranium Monocarbides.

^(a)Atomergic Metals Co., 1001 Franklin Ave., Garden City, N. J.

^(b)Consolidated Astronautics, Inc., 41-45 Crescent St., Long Island 1, N. Y.

ation and repeatedly backfilling with clean argon before fabrication procedures were reinitiated.

It was recognized that incomplete removal of forming aids during sintering would contribute significant

TABLE 1. CHEMICAL AND SPECTROGRAPHIC ANALYSES OF URANIUM CARBIDE POWDERS USED FOR FABRICATION AND COMPATIBILITY STUDIES

Element	WAPD-1	WAPD-2
C, w/o	4.20	4.54
O, ppm	2240	1905
N, ppm	<100	<100
Fe, ppm	30	30
Mo, ppm	300	300
Cu, ppm	10	10

carbon to the final pellet composition. Therefore, pellets were fabricated from the as-received powders without binder or lubricating additions.

The pellets were cold pressed on a laboratory press at variable pressures in a carbide-lined die. The forming pressure used was adequate to impart a degree of mechanical strength to the compact sufficient to resist disintegration during subsequent handling, but not so great as to produce laminations or other recognizable defects in the sintered pellet.

Sintering was done in vacuum or argon in an alumina (Al_2O_3) retort in a molybdenum wire resistance furnace.

FIG. 6. Test Capsule Used to Ascertain the Compatibility of Monocarbide Compositions with 316 Stainless Steel.

TABLE 2. PROCEDURES FOR METALLOGRAPHIC IDENTIFICATION OF SECONDARY PHASES IN HYPOSTOICHIOMETRIC AND MODIFIED URANIUM CARBIDE COMPOSITIONS

ANTICIPATED PHASES	METALLOGRAPHIC PROCEDURES	INTERPRETATION
(a) U IN UC	(1) CHEMICAL ETCH. HNO_3 : HAc. H_2O (1:1:1). t - 8 SECS (MAX.)	UC: STAINS TO A VARIETY OF COLORS AND SHADES. U HIGH REFLECTANCE, WHITE MATRIX AREA
(b) U, UFe_2 , Fe IN UC	(1) CHEMICAL ETCH. HNO_3 : HAc. H_2O (1:1:1). t - 8 SECS. (MAX.) (2) OXIDIZE AT 50°C FOR 17 HRS.	UC STAINS TO A VARIETY OF COLORS AND SHADES. U OXIDIZES TO RED-BROWN. UFe_2 . UNALTERED, HIGH REFLECTANCE PHASE. Fe. ATTACKED, BY ETCHANT . . . BLACK IN EUTECTIC STRUCTURES.
(c) Cr, Cr_{23}C_6 IN UC	(1) ELECTROLYTIC ETCH. V - 5-0 VOLTS. ELECTROLYTE OXALIC ACID (25 GMS) IN METHYL ALCOHOL (100 ml)	UC. STAINS TO A VARIETY OF COLORS AND SHADES. Cr_{23}C_6 ATTACKED, REDUCED REFLECTANCE, MATRIX AREA. Cr. UNATTACKED, HIGHER REFLECTANCE PHASE.

TABLE 3. SINTERED DENSITIES OF HYPOSTOICHIOMETRIC AND MODIFIED URANIUM MONOCARBIDE COMPOSITIONS

COMPOSITION	SINTERING TEMPERATURE	ATMOSPHERE ^(a)	TIME	DENSITY gms / an ³
UC _{1-x}	1150°C	V	2	10.00
UC _{1-x}	1300°C	V	2	10.15
UC _{1-x} + Fe	1300°C	V	2	11.84
UC _{1-x} + Fe	1300°C	V	3	11.45
UC _{1-x}	1425°C	A	4	13.0
UC _{1-x} + Fe	1425°C	A	4	12.5
UC _{1-x} + Cr ₂₃ C ₆	1425°C	A	4	11.0
UC _{1-x} + Cr ₂₃ C ₆	1450°C	A	4	12.3

(a) - V - VACUUM A - ARGON

C. Compatibility Tests

Details of the test capsule assembly used for compatibility studies are given in Fig. 6. The difference in thermal expansion between the Inconel outer capsule and the stainless steel plunger served to load the pellet-disc couple and insure intimacy of contact at the test temperature. The sodium-filled test capsules were assembled by inserting the stainless steel liner and then adding sodium. A test disc, a modified pellet, a second test disc, an unmodified pellet, a third test disc, and the plunger followed in that order. The end caps were finger tightened and then welded closed. The furnaces used for compatibility testing accommodated two capsules of this design. Normally one sodium-filled and one argon-filled capsule constituted the test complement for each furnace.

FIG. 7. Microstructure of Sintered, Hypostoichiometric Uranium Monocarbide. White Phase is Uranium Metal. (Specimen Sintered in Argon at 1450°C for 4 Hr. Density: 13.2 gm/cm³.)

FIG. 8. Microstructure of Sintered Iron-Modified Uranium Monocarbide. Secondary Phases are UFe_2 (White) and $\text{UFe}_2\text{-Fe}$ Eutectic. (Specimen Sintered in Vacuum at 1300°C for 2 Hr. Density: 11.0 gm/cm^3 .)

D. Analytical Procedures

Sintered pellets were dimensioned and weighed for density determinations.

The sintered pellets were also examined metallographically. The compatibility test couples (pellets and discs) were also examined by metallographic techniques. Procedures employed to prepare metallographic sections and identify significant phases in the various microstructures are given in Table 2.

X-ray diffraction studies were also performed on sintered pellets to identify the important secondary phases. Greatest success was realized by this method in the case of chromium carbide (Cr_{23}C_6) modified compositions. The secondary phases in these compositions were concentrated by leaching away the uranium carbide (UC) with an acid wash. The finely divided residue, enriched in the secondary phases,

was studied with a Debye-Scherrer, 114.6-mm camera, and $\text{Cu K}\alpha$ radiation.

III. Results and Discussion

A. Pellet Fabrication

Specimen history is important to the interpretation of compatibility test results. Accordingly, a discussion of the specimen-preparation procedures is required. Initial sintering experiments were done in vacuum. The results of these studies were inconsistent and aggravated by repeated failure of the ceramic retort in which the sintering was conducted. Test specimens sintered in argon gas produced reliable data, and most of the results discussed herein were obtained with specimens sintered in this atmosphere.

The hypostoichiometric and modified compositions sinter via a liquid-phase mechanism. The minimum

FIG. 9. Microstructure of Sintered, Cr_{23}C_6 -Modified Uranium Monocarbide. Secondary Phases Are Cr (Higher Reflectance) and Cr_{23}C_6 . (Specimen Sintered in Argon at 1450°C for 4 Hr. Density: 12.4 gm/cm^3 .)

sintering temperature was defined by the first-melting constituents among the reactants. The first-melting constituent (barring significant solid-state reaction at lower temperatures) is uranium.

Test specimens sintered at 1150°C , i.e., slightly above the uranium melting point, did not densify appreciably. It was concluded, after several sintering trials, that adequate densification concomitant with development of the desired phase equilibria could be accomplished at 1300°C and 1450°C in the iron- and Cr_{23}C_6 -modified compositions, respectively. The densification of several compositions sintered under variable conditions is summarized in Table 3. Figures 7, 8, and 9 are photomicrographs showing the microstructure of the hypostoichiometric, iron-modified and Cr_{23}C_6 -modified compositions after heat treatment for 4 hr in argon. There was no metallographic

evidence of free uranium metal in any of the sintered, modified compositions.

An experiment was conducted to ascertain the effect of variable carbon content on the sintering behavior of powder compacts to which a constant quantity of modifying addition was made. An amount of Cr_{23}C_6 was added which was adequate to modify fully the least stoichiometric composition. The resultant densification and microstructures of the modified and unmodified compositions are compared in Figures 10, 11, and 12. The degree of densification among the compositions was almost equal. The anticipated phases were observed in approximately the relative amounts expected for each composition. It was concluded that densification of variable compositions was not difficult to control over a range of carbon contents attainable by contemporary synthe-

FIG. 10. Microstructure of (a) Hypostoichiometric Uranium Monocarbide (Approximate Composition: $UC_{0.95}$) and (b) $Cr_{23}C_6$ -modified $UC_{0.95}$.

FIG. 11. Microstructure of (a) Hypostoichiometric Uranium Carbide (Approximate Composition: $UC_{0.97}$) and (b) $Cr_{23}C_6$ -modified $UC_{0.97}$. Note Two-phase Nature of Matrix regions.

FIG. 12. Microstructure of (a) Hypostoichiometric Uranium Monocarbide (Approximate Composition: $UC_{0.99}$) and (b) $Cr_{23}C_6$ -modified $UC_{0.99}$.

TABLE 4. SUMMARY OF MODIFIED CARBIDE COMPATIBILITY TESTS COMPLETED OR IN PROGRESS AT WAPD

TEST NO.	CAPSULE DESIGNATION	TEMPERATURE °C	TIME HRS	ENVIRONMENT		TEST PELLET COMPOSITION	OBSERVATIONS	
				Na	A		UNMODIFIED PELLET	MODIFIED PELLET
0	A	815	24		X	UC _{I-X} + Fe	REACTION, DEPTH OF REACTION ZONE IN 316 TEST DISC: 150 μ	NO REACTION
00	B	815	72		X	UC _{I-X} + Fe	REACTION, DEPTH OF REACTION ZONE IN 316 TEST DISC: 110 μ	NO REACTION
1	C	790	500	X		UC _{I-X} + Fe	REACTION, DEPTH OF REACTION ZONE IN 316 TEST DISC: 175 μ	NO REACTION, MECHANICAL ADHERENCE OF PELLET TO TEST DISC.
	D					UC _{I-X} + Fe	REACTION, DEPTH OF REACTION ZONE IN 316 TEST DISC: 150 μ	NO REACTION, MECHANICAL ADHERENCE OF PELLET TO TEST DISC.
2	E	730	1000	X		UC _{I-X} + Fe	NO REACTION	NO REACTION
	F				X	UC _{I-X} + Fe	NO REACTION	NO REACTION
3	-	760	700	X		—	Cr ₂₃ C ₆ POWDER IN Na IN A THIN WALLED (330 μ) STAINLESS STEEL TUBE. NO CARBURIZATION OF THE STAINLESS STEEL	
4	G	815	500	X		UC _{I-X} + Cr ₂₃ C ₆	316 TEST DISCS. SHOWED EVIDENCE OF CARBURIZATION FROM REACTION WITH BOTH MODIFIED AND UNMODIFIED COMPOSITIONS INDEPENDENT OF THE ENVIRONMENT.	
	H				X	UC _{I-X} + Cr ₂₃ C ₆		
5	I	830	1200	X		UC _{I-X} + Fe	TEST CAPSULE IN TEST APPARATUS UPSIDE DOWN. NO Na IN THE VICINITY OF THE TEST COUPLE. NO REACTION.	
	J					UC _{I-X} + Fe	TEST CAPSULE LEAKED. NO REACTION	
6	K	730	—	X		UC _{I-X} + Fe	IN PROGRESS	IN PROGRESS
	L				X	UC _{I-X} + Fe	IN PROGRESS	IN PROGRESS
7	M	830	200	X		UC _{I-X} + Cr ₂₃ C ₆	NONE	NONE
	N			X		UC _{I-X} + Cr ₂₃ C ₆	NONE	NONE
8	O	730	—	X		UC _{I-X} + Cr ₂₃ C ₆	IN PROGRESS	IN PROGRESS
	P			X		UC _{I-X} + Cr ₂₃ C ₆	IN PROGRESS	IN PROGRESS

sis procedures. Complete phase modification was also easily realized in every instance of compositional variation.

B. Compatibility Experiments

Compatibility studies completed or still in progress are summarized in Table 4. Most of this work has been concerned with the iron-modified composition. Figure 13a shows the surface of a 316 stainless steel disc tested in contact with an iron-modified uranium carbide pellet at 790°C for 500 hr. Figure 13b shows a typical cross section through the same disc. There is no evidence of reaction between the disc and the modified carbide pellet.

The surface of a 316 stainless steel disc tested in contact with an unmodified uranium carbide pellet under the same conditions is shown in Figures 14a and 14b. Significant reaction with the pellet is evidenced by the extensive area of pitting. Figure 14b shows the

penetration of the reaction zone ($\sim 150 \mu$) at a cross section taken through the reacted area. Metallographic examinations of the fuel pellet indicated that the reaction involved metal throughout the pellet volume and was not limited to surface concentrations.

Although reaction was evidenced in nearly every instance where the unmodified composition was tested, the extent of the reaction was not a well-behaved function of the test variables, i.e., time and temperature. Accordingly, while failure by the hypothesized reaction mechanism may be concluded to be a certainty in a stainless clad-hypostoichiometric monocarbide system, the kinetics of the failure are not readily predicted.

The absence of evidence of a reaction at the lowest test temperature ($\sim 730^\circ\text{C}$) suggests that the reaction with the clad is accelerated in the presence of a liquid phase. This temperature very nearly approxi-

mates the melting point (725°C) of the lowest-melting iron-uranium eutectic. This eutectic occurs at approximately 35 a/o iron, and compositions containing greater than 14 a/o iron will form a liquid phase at this temperature. At higher temperatures a liquid phase will form at a composition containing significantly less iron (e.g., 25 a/o at 816°C).

Compatibility tests with chromium carbide-modified compositions have only recently begun. Test 3 was run to demonstrate the stability of the Cr_{23}C_6 and its resistance to decarburization in sodium. Finely divided Cr_{23}C_6 and liquid sodium were added to the capsule liner and held at 760°C for 700 hr. There was no evidence of carburization of the liner. Oxygen contamination of the sodium or a leak in the capsule may have contributed to the progress of the carburi-

FIG. 13. (a) 316 Stainless Steel Disc Annealed for 500 Hr at 790°C in Contact with Unmodified, Hypostoichiometric Uranium Carbide in Sodium; (b) Transverse Section through the Test Disc Shown in (a) Taken Along the Line Indicated. Note depth of reaction zone, $175\ \mu$.

FIG. 14. (a) 316 Stainless Steel Disc Annealed for 500 Hr at 790°C in Contact with Iron-modified, Hypostoichiometric Uranium Monocarbide in Sodium; (b) Transverse Section through the Test Disc Shown in (a) Taken Along the Line Indicated. Note absence of reaction. [Notch on right hand side of (a) was for identification. Artifact at left is mechanically adhered carbide.]

zation noted in Tests 4 and 7. These tests are being repeated.

IV. Summary

The compatibility of liquid metal-bonded, non-stoichiometric, mixed monocarbide (U,Pu)C fast breeder reactor fuels with a leading candidate clad material, stainless steel, is poor. A compositional innovation designed to improve the poor compatibility of this material combination has been defined. This innovation involves the addition of controlled amounts of a modifying element or compound to off-stoichiometric compositions. The polyphase composition re-

sulting from heat treatment includes no low-melting eutectic formers or unstable higher carbides.

Experimental study of iron and chromium carbide (Cr_{23}C_6) modified, initially hypostoichiometric, uranium monocarbide (UC_{1-x}) compositions has been initiated. Modified compositions were successfully pelletized by cold pressing and sintering. Sintering in these compositions proceeds via a liquid-phase mechanism at relatively low temperatures. Development of the requisite phase equilibrium also occurs in short times at the same temperatures.

Improved compatibility was experimentally demonstrated in argon and liquid metal environments between contact-bonded, iron-modified, initially hypostoichiometric uranium monocarbide and 316 stainless steel. Initial experiments with gas- and sodium-bonded Cr_{23}C_6 -modified UC_{1-x} are inconclusive and continuing.

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Discussion

Mr. Siegel (AI): I gather that the formation of the alloys is by powder metallurgy techniques. Can they be made directly from a single arc melting process?

Mr. Herbst: We haven't considered the problem at any length. There appears to be difficulty with plutonium in that it exhibits a variable volatility. We

thus directed our efforts at a practical scheme for fabricating the material, liquid-phase sintering at a reasonably low temperature.

Mr. Siegel: Do you know whether that process is likely to add significantly to the fabrication cost?

Mr. Herbst: We believe that this represents a cost reduction effectively from a fabrication standpoint. Our experience with oxide, for example, has been by powder metallurgical procedures.

Mr. Siegel: With hypostoichiometric UC in contact with stainless steel we have observed basically no practical incompatibility up to 1600°F, with what amounts to a sodium-bonded couple.

Mr. Stahl (UNC): You say you have liquid-phase sintering. Have you investigated at all the stability of the fuel under long-time high-temperature treatments? What happens to your second phase?

Mr. Herbst: No, we haven't carried out this kind of investigation. The composition is obviously tailored to the Westinghouse Fast Breeder Reactor design. Maximum operating temperatures of the Westinghouse system will include a finite volume of liquid phase at operating temperatures, and this is considered a disadvantage. The chromium addition leads to a higher melting point and will get us over this, so that we should have no liquid phase under reactor operating conditions.

Mr. Stahl: Have you investigated all the diffusion layers between the fuel and cladding so that you don't have to worry about stoichiometry?

Mr. Herbst: No, our whole program is based upon the modifying additions of secondary phases. We haven't initiated any work in the area of diffusion barriers, for example.

Mr. Walter (ANL): Have you irradiated any of this material?

Mr. Herbst: No irradiation experiments have been conducted yet. We do have an irradiation experiment proposed for both iron- and chromium-modified compositions for EBR-II.

Mr. Walter: What is your feeling on the effect of changes of stoichiometry in the carbide due to formation of fission product carbides and things like this? This may throw your phase relations completely off and change compatibility. Also, even though you are forming higher-melting material, it looks like it is mostly in grain boundaries rather than in U-Pu alloy. The melting point is still considerably lower than that of carbide. One would think there might be some problems with just irradiation damage in the fuel in comparison to a normal hyperstoichiometric carbide.

Mr. Herbst: We believe, for example, that in the case of chromium-modified carbide we have no fissile

secondary phases in grain-boundary regions. Thus we anticipate improved compatibility or improved irradiation performance comparable to the stoichiometric composition, which has been demonstrated to be good in other irradiation studies. The dilution or effective modification of the composition of the carbide under irradiation has been considered, but the answer escapes me at the moment.

Mr. Kummerer (Karlsruhe): You have carried out this investigation with pure carbide fuel. We are more interested in the mixed carbide fuel, for which

the compatibility may be quite different than for pure carbide. To what extent do you consider these results as a simulation for the real mixed carbide fuels?

Mr. Herbst: Our feeling particularly with regard to the iron modification is that it is a pretty good mockup, based primarily on the Harwell work, which has included the quasiternary $U_{0.7}$ and $Pu_{0.3}$. The phase relations derived from these studies are not significantly different from what has been reported for UC-Fe.

Mixed-carbide Fuels for Fast Reactors

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Introduction

Carbide fuels have long been recognized for their potential superiority over metal and oxide fuels. This potential is based on superior irradiation behavior over metals and better thermal conductivity, higher density, and lower release of fission gases than in the case of oxides. The realization of this potential, however, has been hampered by the problem of cladding compatibility. It is toward the solution of this problem that much of the work on carbides is directed.

For the purpose of defining carbide compositions of interest as fast reactor fuels, the term "hypostoichiometric" will be used in this paper to designate alloys that contain metal in equilibrium with the monocarbide, (U,Pu)C phase. The term "hyperstoichiometric" will refer to compositions with monocarbide in equilibrium with higher carbon phases, (U,Pu)₂C₃ and (U,Pu)C₂. Oxygen has been shown to have substitutional solubility with (U_{0.85}Pu_{0.15})C to the extent of 40 a/o of the carbon content,⁽¹⁾ and nitrogen forms a complete substitutional solid solution.⁽²⁾ As a result, single-phase monocarbide is produced when the non-metal-atom C + O + N content totals 50 a/o. A hyperstoichiometric amount of C + O + N apparently produces a microstructure with monocarbide in equilibrium with higher carbide, but it is also conceivable that an oxide phase could also be present in three-phase equilibrium.

It appears that the most desirable microstructure is stoichiometric or hyperstoichiometric.⁽³⁾ The questionable irradiation behavior of a free metal phase coupled with its possible eutectic reaction with the cladding precludes the use of hypostoichiometric carbide with present technology. At present, hyperstoichiometric carbide must be used with a helium contact bond between itself and the cladding because a sodium bond will transfer carbon to the cladding. The contact bond, however, will create higher fuel temperatures than the sodium bond. The higher temperature with its deleterious effects is cited as the principal disadvantage of helium contact bonding and, hence, of hyperstoichiometric fuel. Nevertheless, until proven conclusively that hyperstoichiometric carbide has a

significant temperature limitation, this composition is considered a better choice than hypostoichiometric. Stoichiometric carbide suffers from none of the aforesaid disadvantages. Its major drawback is the inability to produce it in large-scale quantities because of its narrow range of carbon content.

The carbide development program of the Metallurgy Division at Argonne National Laboratory has concentrated on making and studying stoichiometric or slightly hyperstoichiometric mixed carbides with a uranium-plutonium ratio of 4:1. This paper will discuss some of the results of this program.

Synthesis

The techniques for synthesis of the mixed carbides have, in general, been extensions of processes used for the preparation of uranium monocarbide. Four techniques that have been used extensively for this purpose are: (1) high-temperature fusion of metal-carbon or oxide-carbon mixtures, either under arc or by a plasma, (2) carbothermic reduction of the oxides, (3) reaction-sintering of mixtures of uranium-plutonium hydrides with carbon or mixtures of higher uranium-plutonium carbides, and (4) gas-metal reactions. Most of these methods for the synthesis of mixed carbides have been investigated on a laboratory-scale at Argonne. A fifth method, that of direct sintering of mixtures of uranium-plutonium carbides, has also been tried. The advantages and disadvantages of each process depend primarily on the desired composition of the fuel and its final physical form: sintered pellets, cast slugs, or powder with particle sizes that are needed for vibratory compaction.

Arc-melting⁽⁴⁾ and carbothermic reduction of the mixed oxides⁽⁵⁾ were the first methods used at Argonne to prepare cast and pressed-and-sintered pellets for early irradiation tests.⁽⁶⁾ More recently the arc-melting process has been used to synthesize material for sintering of pellets and vibratory compaction. This process was used because homogeneous material with reproducible carbon composition and low oxygen content was desired as a sintering material for preparation of pellets. Previous work had shown that arc-

melting produces such a product on a laboratory scale.

Buttons of a $(U_{0.8}Pu_{0.2})C$ composition weighing about 50 gm were prepared by fusion of the elements with a tungsten-tipped electrode. The homogenized buttons were always very close to stoichiometric composition, and they usually contained less than 0.03 w/o oxygen plus nitrogen. The melts were kept small to avoid appreciable contamination from the tungsten electrode. The time in the liquid state was kept short to avoid detectable plutonium losses. This material was then crushed and sintered into pellets by a procedure described later.

Buttons of approximately 100-gm weight were also made by the above procedure with the exception that a carbon-tipped electrode was used instead of tungsten. As expected, the compositions of the first few melts were hyperstoichiometric because of the high affinity of plutonium for carbon. The carbon content of the starting materials was then adjusted, and a significant reduction in the carbon content of the homogenized button resulted. This procedure, however, does not appear promising for production of large quantities of material with reproducible compositions because of the difficulty in controlling the carbon content.

Fabrication

A parallel study is being made of two concepts of fuel elements. One concept uses pressed-and-sintered pellets contact bonded to the jacket, and the other uses powder vibratorily compacted in fuel tubes. Although the pellet-making process is relatively simple, the large number of pellets required to fuel and refuel a reactor suggest a need for a bulk-processing technique. Vibratory compaction^(7, 8, 9) is considered a likely competitor for the pellet process.

Sintered Pellets

Arc-fused buttons of $(U_{0.8}Pu_{0.2})C$ were crushed to powder that was screened to various particle sizes in gloveboxes that contained an inert atmosphere of nitrogen with impurities of oxygen and water vapor of about 0.005 and 0.01 w/o, respectively. The powder was coated with 0.5 w/o Carbowax binder and pressed into pellets at a pressure of about 21 kg/mm².

Powders with high oxygen contents were undesirable for fabrication because they produced dimensional variations in the form of an hour-glass shape in the sintered pellets. The lowest attainable oxygen content was considered best for preparation of pellets with uniform diameters over their entire length. High oxygen contents also produced wide variations in phases from the center to the surface of pellets. Loss of car-

FIG. 1. The Change in Density with Sintering Temperature for $(U_{0.8}, Pu_{0.2})C$ Pellets Prepared from $<44\mu$ Powder and Fired in Argon.

bon at the surface through the CO reaction resulted in a hypostoichiometric composition at the surface and hyperstoichiometric composition in the center of a pellet.

The changes in density of the pellets were investigated as a function of particle size and sintering temperature. Preliminary work showed that pellets from the solid-solution material lost plutonium when fired under vacuum at temperatures of 1600°C and above for 2 hr. To reduce plutonium vaporization, pellets were fired in a flowing, high-purity argon gas at a pressure slightly above atmospheric. No plutonium loss was detected, and all subsequent studies were carried out under these conditions.

Pellets prepared from powder with a particle size of about 10μ attained densities of 80 to 85% of theoretical when sintered under argon for 2 hr at 1600°C. Densities of 90% of theoretical were obtained when the sintering temperature was increased to 2000°C for 2 hr. The fine particle size of the powder caused oxygen pickup to the extent of 1 to 2 w/o. Carbon contents of the pellets were low, which indicated loss by reaction to form CO, and values ranged from 3.2 to 3.9 w/o carbon. In comparison, pellets prepared from powder with a particle size of about 40μ and sintered at 1600°C had lower densities (about 76% of theoretical) but the oxygen content was usually less than 0.15 w/o and the amount of carbon was always about the same as that present in the arc-melted material. Because of this problem of severe oxygen contamination of fine powders, all further work was conducted with powder with a particle size of about 40μ .

The changes in density of pellets sintered at increasing temperatures for 2 hr are shown in Fig. 1. Pellets of 90% of theoretical density were obtained by sintering at 2100°C. Metallographic and X-ray examination showed that only the solid-solution (U,

FIG. 2 Microstructure of $(U_{0.8}Pu_{0.2})C$ Made by Arc melting and Then Sintered ($\times 200$)

$Pu)C$ phase was present in these pellets. There was no detectable loss of plutonium from sintering under argon at this temperature.

The above procedure was used to make pellets 6.42 mm in diameter by 12 to 15 mm long for irradiation studies in MTR, test fuel elements for EBR-II, and compatibility studies. Densities of about 85% of theoretical were desired for the irradiation experiments. From the curve of Fig. 1 it can be seen that a density of 85% of theoretical is obtained after sintering at about $1900^{\circ}C$ for 2 hr. The shrinkage of these pellets was controlled to ± 0.05 mm so that they could be loaded into fuel jackets without grinding to size. Some of the pellets for the MTR irradiations were made with axial holes to accommodate thermocouples. Every effort was made to prepare single-phase $(U_{0.8}Pu_{0.2})C$ that was well-characterized through chemical analysis and metallographic examination. Chemical analysis showed the pellets contained 4.64 to 4.72 w/o carbon, 0.11 to 0.12 w/o oxygen, and 0.014 to 0.020 w/o nitrogen. The microstructure of a typical pellet is shown in Fig. 2.

Vibratory Compaction

Two vibrantly compacted uranium-plutonium carbide systems are being studied at Argonne: solid-

solution powder, and mixed powders of UC and PuC. The solid-solution powder is made in the same way as the feed for sintered pellets. The latter procedure (the UC and PuC can be handled separately until the mixing step), although simplified, is made at the cost of a significantly reduced Doppler effect. These factors will have to be considered in the final analysis of this system.

Small, 50-mm-long fuel specimens of physically mixed UC-20 w/o PuC powders have been made for capsule-irradiation studies⁽¹⁰⁾. Three size fractions of powder were blended and then vibrated *in toto*. The coarse (20/40 mesh) and fine (-325 mesh) fractions were UC, and the middle (120/200 mesh) fraction was PuC. The PuC was made at Argonne by the reaction of elemental plutonium and carbon⁽¹¹⁾. The UC was purchased commercially. The average packing efficiency in the specimens was 80%. The small specimen size and short compaction times minimized component separation which is common in a pre-blended system. It was realized that this would not be the case with full-size fuel rods.

A basic study of particle packing in systems of spheres⁽¹²⁾ provided the mathematical relationships necessary to predict the primary, binary, and ternary packing efficiencies in full-scale rods. This work has

FIG. 3. Void Packing Efficiency vs Ratio of Diameter of Container to First Fraction, First Fraction to Second Fraction, etc.

since been extended to include the packing characteristics of irregularly shaped particles.⁽¹³⁾ The information gleaned from these studies has been applied to the loading of a series of uranium carbide-plutonium carbide fuel specimens for irradiation in EBR-II.⁽¹⁴⁾ These specimens, the first full-scale vibratory-compacted rods made at Argonne, were made by an infiltration technique.⁽¹⁵⁾

Eleven mixed carbide rods containing 80 w/o uranium carbide and 20 w/o plutonium carbide and five solid-solution uranium-plutonium carbide rods were made by the new technique. The density of the mixed carbide system was specified at 80% of theoretical. Since the distribution and concentration of PuC in the solid solution with UC was uniform at 20 w/o PuC, the only requirement on the solid-solution fuel body was uniform density. In the case of the solid-solution specimens, as high a density as might be practically attained was allowed.

Before loading any of the jackets with the two carbide materials, it was necessary to determine the quantities and sizes of grains to be loaded. The determination was based upon the results of newly developed information on particle packing.⁽¹³⁾ Experiments with malleable, nonfissile materials compacted into rigid tubes indicated that the maximum packing fraction for a two-component system of angular particles in a small-diameter, 6.5-mm tube would be about 80%. Since the particle density of the carbides was about 97.5% of theoretical, such a packing fraction would yield a fuel density of only 78%. An obvious solution to the problem of raising the packing

density would, in a model system, be the use of three components. However, in the past it was found that efforts to produce uniform density in ternary systems of angular particles were singularly unsuccessful. An alternative would be to use the popular procedure of blending three components and pouring them into the jackets while vibrating.^(7, 8) This solution was rejected because of nonuniform density and particle distribution resulting from the rapid settling of the finest fraction and subsequent levitation of the coarser particles.

The procedures to yield an acceptable product involved a compromise between the infiltration technique and the blending approach. A three-component system was devised in which settling and levitation were prevented and the infiltration of the finest fraction came about with little difficulty. To accomplish this, a blend of two size components was made. The coarse size was chosen to yield the maximum packing efficiency of angular particles in a 6.5-mm tube according to the equation⁽¹³⁾

$$Pe_1 = 0.635 - \frac{1.260}{d_1} - 0.072e^{-0.207D/d_1},$$

where Pe_1 is the packing efficiency of the coarse component, d_1 is the particle dimension in microns, and D is the inside diameter of the tube, 6500 μ . The relationship between packing efficiency and particle size is shown in Fig. 3, which was derived from experimentation with angular particles.

The optimum size for the coarse fraction, d_1 , was found to be 1200 μ . The determination was made by

FIG. 4. Gamma-radiographs of Vipac UC-20 w/o PuC Showing Top Areas Depleted of Fines.

taking the derivative of Pe_1 with respect to d_1 in the above equation, setting the derivative equal to zero, and solving for d_1 . The maximum packing efficiency of the coarse component in this system was 58.6%.

In order to increase the packing fraction of the first component to about 66%, a second component was added. The size of the second component was selected such that it would be small enough to fit into the large voids in the coarse-component matrix and large enough so that it could not migrate through the paths between large voids. The second-component size was determined to be $600\ \mu$, one-half the coarse-component size.

Several runs were made in which angular blast-cleaning grit was used as a stand-in for fissile materials. The two-component first fraction was blended, added to a sample jacket, held in place by a "thin-ble,"⁽¹²⁾ and infiltrated with -325 mesh powder. The model runs yielded densities of about 82% in which 20% of the material loaded was -325 mesh. The -325 mesh fraction was a substitute for PuC in the mixed carbide system.

The ten specimens of mixed UC and PuC, if well-behaved, should have been homogeneous fuel columns of 80% of theoretical density. Radiography of the actual fuel columns showed void regions at the tube wall near the restrainer. The voids were indicative of low density resulting from the absence of the fine fraction at the top of the column. This condition was confirmed by gamma radiography, as shown in Fig. 4.

The low density at the top of the fuel column was caused by the deviation of the carbide system from the model. The most significant difference between the theory and practice was that the model system was made up of angularly shaped particles of malleable material, while the carbides were highly brittle. The effect of the brittle nature of the fuel was to provide a wider range of particle size during settling of the matrix than was introduced into the jacket as feed. Experience gained with some rejected specimens showed that 20% of a carbide fraction could be reduced to a smaller size during the loading of the matrix material alone. Since the loading of the -325 mesh fraction took as much as 8 hr, a considerable attrition could be expected. Although one would not expect the generation of additional void volume by particle cleavage, a similar effect does exist.

It was previously reported that the packing efficiency of approximately tetragonal shapes may be increased by rounding the edges of the particles.⁽¹³⁾ The increase in packing efficiency of milled particles is brought about by at least three conditions: (1) the

rounding of edges reduces the surface-to-volume ratio (the particle approaches spherical geometry), (2) the voids in the matrix have rounded walls that reduce resistance to the entrance of the fine fraction, and (3) extremely fine powders that result from attrition add another size fraction to the particle size distribution. Since the extent to which the carbides are affected by abrasion is not known, it is difficult to predict the degree of this effect.

Five specimens of solid-solution (U,Pu)C were assembled in which maximum uniform density was desired. Maximum density was achieved by adding the -325 mesh fraction until the rod would accept no more. The void packing efficiency reached in these specimens was $86.7 \pm 1.6\%$, which yields a theoretical density of $84.4 \pm 1.5\%$ for the 97.5% dense particles. Gamma and autoradiography of these specimens showed uniform density over the entire length of the fuel column. Uniform distribution of plutonium, inherent in solid-solution feed materials, was confirmed by autoradiography.

The loading of the solid-solution rods gave information on the quantity of -325 mesh material which could be added to the blended matrix of coarse particles.

One rod containing mixed carbide was made on the basis of this information. The -325 mesh fraction, made up of 18.4% UC and 81.6% PuC, was added until no additional material could be infiltrated into the UC matrix. The finished fuel rod showed no voids in the gamma radiograph. The total packing fraction was 86.0%, the theoretical density was 83.8%, and the plutonium carbide content was 19.3%.

The determinations of plutonium distribution and uniformity of density by the techniques employed were qualitative, at best. Therefore, the densities achieved are "smear" densities and indications of homogeneity are relative only. Investigations are now under way to evaluate systems for the determination of particle distribution in vibratory-compacted bodies. When such capability is realized, quality control of compacted fuel columns will become possible. At present a fuel rod is completely assembled before being inspected for fuel distribution and density. Capability of examination in a glovebox before closure welding of the fuel rod will permit adjustment of the loading and guarantee final assembly of an acceptable product.

Compatibility

The initial phase of the Argonne compatibility studies on (U,Pu)C consisted of surveying the interaction of a number of cladding materials, including

refractory metals, and iron- and nickel-base alloys with stoichiometric, single-phase (U,Pu)C over the temperature range from 650 to 1100°C. This portion of the program is concerned with contact bonding in the absence of sodium and represents the extreme contact conditions found in a helium contact-bonded fuel element. The emphasis has been placed on iron- and nickel-base claddings at 650 to 800°C. Later portions of the program will investigate the effect of stoichiometry, stabilizing additions, sodium environment, and irradiation.

The (U,Pu)C was prepared by sintering pellets pressed from crushed, arc-melted stock as described in the preceding section. The U/Pu ratio was 4:1, and a typical chemical analysis showed a carbon content of 4.74 w/o, with oxygen and nitrogen impurities of 0.09 and 0.03 w/o, respectively. Examination of the pellets showed single-phase monocarbide of approximately 86% of theoretical density.

Each compatibility assembly consisted of a pellet of the carbide fuel between two disks of cladding material held in contact with a molybdenum frame. In addition, a third metal disk was added to the assembly to ensure that the fuel-cladding assembly had a sufficiently greater thermal expansion than that of its molybdenum holder to maintain continued contact between fuel and cladding at elevated temperatures.

Approximately 0.8 mm was ground off each end of the carbide pellets to reduce the chance of surface-impurity phases. The fuel-cladding contact surfaces were then polished flat by use of 4/0 emery paper on a glass plate and a special specimen holder. Immediately after polishing the specimens were assembled and pressure applied by a screw in one end of the molybdenum holder. The assembly was then placed in a capsule, evacuated, backfilled with one-third atmosphere of helium, and sealed. Stainless steel capsules were used for the 800°C heat treatments; quartz capsules were used for the heat treatments at higher temperature. Equivalent results were obtained in each capsule material.

The results of the compatibility tests made to date should be considered as a preliminary survey. The extent of carbide reaction with the various cladding materials are summarized in Table 1 and are discussed briefly below. Some representative reactions with these materials are illustrated in Fig. 5.

Type 304 Stainless Steel—The results show no reaction of single-phase (U,Pu)C with Type 304 stainless steel at 800°C for 42 days. At 1100°C, heavy grain-boundary penetration was noted, with eventual melting occurring between 17 and 42 days.

TABLE 1. REACTION OF $(U_{0.3}Pu_{0.2})C$ WITH POTENTIAL CLADDING MATERIALS

Material	Temp (°C)	Reaction Zone Width (μ)			Direction of Reaction Zone	Remarks
		7 Days	17 Days	42 Days		
304 SS	800	Nil	Nil	Nil	—	—
	950	3	—	—	Cladding	—
	1100	130	380	Melted	Cladding	Heavy grain-boundary penetration
Hastelloy-X	800	—	5	8	Fuel	Continuous band
	950	45	60	75	Fuel	Continuous band
	1100	Melted	Melted	Melted	—	—
V-20 w/o Ti	800	—	100 (270)*	—	Cladding	Nonuniform continuous band, poorly defined due to reaction along stringers
	950	160 (320)*	—	350 (700)*	Cladding	
	1100	750 (>1300)*	—	>1300	Cladding	
Vanadium	950	Nil	Nil	45	Cladding	Irregular second-phase growth along grain boundaries
	1100	165	270	450	Cladding	
Niobium	950	2	—	—	Cladding	Uniform continuous band
	1100	8	—	—	Cladding	Uniform continuous band
Molybdenum	1100	6	—	—	Cladding	Uniform continuous band
Tantalum	1100	4	—	—	Cladding	Uniform continuous band
Chromium	1100	Nil	—	—	—	—
Tungsten	1100	Nil	—	—	—	—

* Values in parentheses indicate extent of reaction along stringers.

Hastelloy-X—At 800°C, slight reaction (8 μ at 42 days) was noted with Hastelloy-X at the interface. At 950°C, the reaction zone is shown to be clearly in the direction of the fuel.

Vanadium-20 w/o Titanium—This alloy appeared to react quite extensively with (U,Pu)C at 800°C. The reaction zone was rather irregular and appeared to follow stringers (particles aligned in the extrusion direction) observed in as-received vanadium-titanium material.

Vanadium—No reaction was noted at 950°C up to 17 days, but a few small grain-boundary penetrations were observed after 42 days. This second phase was present in much greater amounts at 1100°C.

Refractory Metals (Ta, W, Mo, Nb, Cr)—Tantalum, niobium, and molybdenum showed slight reaction (4–8 μ) at 1100°C for 7 days; no evidence of reaction was observed with chromium and tungsten under these conditions.

The program of compatibility studies also includes new concepts in claddings and in fuel composition. These are described below.

Cladding Materials—Stainless steel, because of its low cost, good compatibility with sodium, and ease of fabrication, will continue to receive attention even

though its strength is significantly lowered at temperatures above 600°C. Increased nickel content, although imparting greater strength to iron-nickel-base alloys, appears also to lessen the compatibility of these alloys with carbide fuels. High nickel alloys, such as Inconel or Nimonic, have been reported^(16, 17) as incompatible with carbide fuels. Hastelloy-X (47% Ni) has shown some reaction at 800°C with stoichiometric material. Because of these factors, the investigation is being expanded to include high strength iron-nickel-chromium and iron-nickel-chromium-cobalt alloys with relatively low nickel contents (13–25%). These alloys represent an interesting range of composition while having a significant increase of strength over stainless steel at 700°C. The compositions of the potential cladding materials receiving immediate attention are listed in Table 2. Three of these alloys contain substantial amounts of cobalt or manganese, the influence of which is unknown with regard to compatibility with uranium-plutonium carbides or to resistance to corrosion by sodium.

Effects of Carbide Stoichiometry—Although all material considerations favor the use of stoichiometric monocarbide, its permissible range of carbon content is so narrow that large-scale production is not eco-

Hastelloy X
7 Days at 950°C

Vanadium 20 w/o Titanium
42 Days at 950°C

304 Stainless Steel
7 Days at 1100°C

Vanadium
7 Days at 1100°C

Niobium
7 Days at 1100°C

FIG. 5 Reaction of Single phase $(U_{0.5}Pu_{0.5})C$ (Bottom) with Various Materials

nomically feasible at this time. Hyperstoichiometric carbide in helium contact-bonded systems appears to be the next choice. Although stainless steels are reported to be compatible with slightly hyperstoichiometric $(U,Pu)C$ at 816°C for 4000 hr, the compatibility of hyperstoichiometric $(U,Pu)C$ and newer potential cladding materials is yet to be determined.

Additions to Carbide Fuels—The lower thermodynamic stability of the carbide phases $(U,Pu)_2C_3$ and $(U,Pu)C_2$ in comparison to $(U,Pu)C$ can present compatibility problems in hyperstoichiometric carbide fuels because of their tendency to transfer carbon to the cladding, especially in the presence of a sodium bond. The carbon that is transferred either

forms a cladding carbide at the interface or diffuses into the cladding and degrades its mechanical properties, notably ductility. It is therefore desirable to eliminate these higher carbides. This might be accomplished in two ways: (1) the addition of an element which would reduce $(U,Pu)_2C_3$ and $(U,Pu)C_2$, and form a compound that would give the fuel greater overall thermodynamic stability, and (2) the addition of an element that would form a very stable carbide existing as a single-phase compound over a relatively wide composition range. In the latter method it is hoped that the fuel pellet would consist of either uranium-plutonium monocarbide plus a metal carbide with a wide range of stoichiometry or

TABLE 2. POTENTIAL CLADDING MATERIALS FOR (U,Pu)C

Alloy	Composition (w/o)										
	Fe	Ni	Cr	Co	Mn	Mo	Nb + Ta	W	C	N	Si
Type 304 Stainless Steel	67	10	19	—	2	—	—	—	0.06	—	1
Hastelloy-X	18	48	22	1.5	1	9	—	0.6	0.10	—	1
16-25-6	50	25	16	—	1.3	6	—	—	0.08	0.15	0.70
16-15-6	55	15	16	—	7.5	6	—	—	0.07	0.35	0.50
Haynes 56	45	13	21	11.5	1.5	4.5	0.75	1.5	0.28	0.10	1
N 155	30	20	21	20	1.5	3	1	3	0.15	0.15	0.5

TABLE 3. SUMMARY OF VIPAC CARBIDE IRRADIATIONS

Specimen No.	Fuel	Jacket Material	C in PuC, w/o	Burnup		Linear Heat Rating, W/cm		Jacket Surface Temp, °C		Fuel Center Temp, °C ⁽¹⁾		Maximum % ΔDia	% ΔL	% ΔV	Gas Release, % Theoretical
				a/o	Fiss/cc × 10 ²⁰	Avg	Max	Avg	Max	Avg	Max				
F-7	PuC	304 SS	4.24	1.95	5.1	285	310	450	495	1000	1095	1.0	0.1	1.1	35.2
F-6	PuC	304 SS	6.22	5.43	13.1	390	505	550	625	1305	1610	0.8 ⁽²⁾	Specimen Melted		
F-8	PuC	304 SS	4.24	6.0 ⁽³⁾	16.0	195	210	605	675	980	1080	14.3	2.0	Jacket Ruptured	
C-44	PuC	Nb-1Zr	3.40	2.18	5.3	295	335	515	625	1070	1255	0.5	Neg	0.3	45.9
C-43	PuC	Nb-1Zr	4.24	5.3 ⁽³⁾	14.0	165	170	510	550	820	870	0.1	Neg	0.3	31.0
F-18	UC-20PuC	304 SS	6.42	1.35	3.4	190	200	455	505	820	890	0.3	Neg	0.6	8.8
F-21	UC-20PuC	304 SS	3.40	1.40	3.5	195	220	395	430	770	820	0.5	Neg	0.2	25.7
F-19	UC-20PuC	304 SS	6.22	5.8 ⁽³⁾	15.0	170	180	525	570	855	915	2.8	0.3	3.5	<1 ⁽⁴⁾
F-20	UC-20PuC	304 SS	4.24	6.8 ⁽³⁾	18.0	195	205	600	645	975	1040	4.7	1.3	7.3	40.0
C-69	UC-20PuC	Nb-1Zr	6.42	1.21	3.1	170	185	370	405	690	750	0.5	Neg	0.3	5.7
C-74	UC-20PuC	Nb-1Zr	3.40	1.57	4.1	225	260	400	455	820	945	0.3	Neg	0.5	21.5
C-72	UC-20PuC	Nb-1Zr	6.22	6.8 ⁽³⁾	18.0	190	195	575	635	925	1000	0.2	Neg	0.4	<1 ⁽⁴⁾
C-70	UC-20PuC	Nb-1Zr	4.24	6.8 ⁽³⁾	18.0	195	200	600	650	965	1025	0.4	Neg	0.5	20.0

⁽¹⁾ Calculated assuming a fuel-jacket interface conductance of 0.136 cal/sec-cm²-°C (1000 Btu/hr-ft²-°F).

⁽²⁾ Measured on remaining pieces.

⁽³⁾ Burnup estimated from heat-generation rates.

⁽⁴⁾ Based only on xenon release.

of a uranium-plutonium-metal monocarbide with a wider range of stoichiometry than that of (U,Pu)C. Titanium and zirconium additions are being tried by the French.⁽¹⁸⁾

Since these additions will be made to the hyperstoichiometric powders before sintering of the pellets, phase studies of the fired pellets should indicate the feasibility of these methods before compatibility tests are run. Preparations are now being made for these phase studies.

Irradiation

The irradiation program has as its objective the investigation of fuel behavior with respect to swelling, fission-gas release, micro- and macro-structural changes, and cladding compatibility. Both pellet fuel and vibratorily compacted fuel are being studied.

So far, the emphasis has been on vibratorily compacted physical mixtures of UC-20 w/o PuC powders. A few vibratorily compacted PuC powder specimens have also been irradiated to obtain information on the single-carbide system. The specimen fabrication has been discussed previously under "Vibratory Compaction." These specimens were 70 mm overall with a 50-mm-long fuel section and a small, 13-mm gas plenum (20% of the fuel volume) above the fuel. The fuel diameters were 3.96 or 6.53 mm. The jackets were either Nb-1 w/o Zr or 304 stainless steel, 0.23 or 0.30 mm in thickness. The carbon content of the UC was 4.8 w/o; that of the PuC ranged from 3.4 to 6.4 w/o. Free plutonium was present in the low-carbon batches and sesquicarbide was evident in the high-carbon batches. The uranium was not enriched since the irradiations were to be made in thermal reactors.

FIG. 6. From Left, UC-20 w/o PuC Specimens F 19 (304 SS), F-20 (304 SS), C 72 (Nb-1 w/o Zr), and C-70 (Nb-1 w/o Zr) after Irradiation (13X).

The PuC was then the only component undergoing significant fissioning, and the results are characteristic of the PuC powder and not the UC.

The irradiation program for the vibratorily compacted specimens called for nominal burnup levels of 2, 5, and 10 a/o at jacket surface temperatures of 600, 800, and 1000°C. The irradiations are done in the CP-5 and MTR reactors in instrumented capsules. The MTR capsules are also temperature-controlled, but at the sacrifice of high linear heat ratings. Irradiations have been completed at the 2 and 5 a/o burnup levels at 600°C, although the 2 a/o specimens did not all quite achieve this burnup or temperature. Specimens for the 10 a/o level at 600°C and 800°C are now in MTR, and 2 and 5 a/o, 1000°C capsules are being prepared.

The irradiation data and postirradiation examination results on specimens that have been examined are given in Table 3. All specimens had 0.23-mm-thick jackets and a fuel diameter of 3.96 mm. Burnups ranged from 1.2 to 6.8 a/o (3.1×10^{20} to 18×10^{20} fissions/cc) with average surface temperatures between

370°C and 605°C. Maximum linear heat ratings were between 170 and 505 W/cm (5.2 and 15.4 kW/ft, respectively). The burnups were determined from Tc⁹⁹ analyses unless noted otherwise. The heat-generation rates were in turn calculated from the burnups. The higher burnup specimens of UC-PuC and PuC are shown in Figures 6 and 7, respectively.

Inspection of the data indicated the following:

1. No significant diameter, length, or volume changes occurred when Nb-1 w/o Zr was the jacket material for UC-20 w/o PuC. This was independent of fuel material, carbon content, burnup, heat rating, or temperature.
2. Changes in diameter, length, and volume occurred on stainless steel jackets with UC-20 w/o PuC. Primary dependence appeared to be on burnup, with secondary dependence on temperature and heat rating.
3. Stainless steel jackets with PuC had diameter increases apparently dependent on burnup.
4. Fission-gas release was dependent on the carbon content of the PuC.

FIG. 7. Specimens C-43 (Left) and F-8 (Right), PuC in Nb-1 w/o Zr and 304 Stainless Steel, Respectively, after Irradiation (1.5X).

All mixed-carbide specimens were intact. Two stainless-jacketed PuC specimens failed from different causes. Specimen F-6 melted in a large area of the fuel section, as shown in Fig. 8. The calculated center temperature of 1610°C in only 40° below the melting point of PuC. Because of the nature of the assumptions, the calculation can be off enough to make up the difference. Preliminary metallography indicated the fuel melted and reacted with the jacket. It is not known when during the irradiation this occurred, because the maximum thermocouple reading indicated a jacket surface temperature of only 625°C.

The jacket of specimen F-8, shown in Fig. 7, apparently ruptured because of high internal gas pressure. The failure is typical of those observed in tube-

burst tests. From the gas collected from specimen C-43, it was estimated that the maximum cladding stress might have been 4.5 kg/mm². The specimen also had a double bow, which is evident in Fig. 7. Bowing was also evident to a lesser degree of specimen F-20 (shown in Fig. 6). These specimens have not been metallographically examined as yet, but the fuel just inside the split of specimen F-8 appears to be massive and not particulate.

Only cursory metallography of the low-burnup specimens has been done. Other than evidence of melted fuel in the remains of specimen F-6, no gross fuel changes have been noted. In-pile sintering of UC and PuC has so far been found only to a small degree.

FIG. 8. Specimen F-6 (PuC in 304 Stainless Steel) Shown Reassembled after Meltdown during Irradiation (1.5X).

Figure 9 shows a small PuC particle to which smaller UC particles have become attached. Extensive sintering was found in specimen C-44, which was all PuC. It is expected that sintering of UC and PuC will be evident on a larger scale in the higher-temperature specimens. Other than the melted specimen, the only fuel-jacket reaction noted was a thin intermittent layer on the fuel side of the fuel-cladding interface in PuC specimen C-44. The layer is as yet unidentified, but may be free plutonium metal from the hypostoichiometric carbide.

The fission-gas results are indicative of the effect of a free metal phase. Hypostoichiometric carbide released significantly more gas than the hyperstoichiometric. The quantities of gas released are commensurate with the irradiation temperatures and are not considered excessive.

As a logical step in the irradiation program, the vibratorily compacted fuels are being tested in full-scale fuel rods in EBR-II, where they will be in a fast flux and have heat ratings of interest for direct reactor application. This phase of the program now includes solid-solution powders and also pressed-and-sintered pellets. The fabrication of all these fuel specimens has been previously discussed in this paper.

Three vibratorily compacted fuel rods are now under irradiation in EBR-II at heat ratings of 650 to 790 W/cm (20 to 24 kW/ft) and jacket surface temperatures of about 600°C. Jacket materials are 304 stainless steel, Hastelloy-X, and Nb-1 w/o Zr. Cladding thicknesses have been increased to 0.30 mm for Nb-1 w/o Zr, 0.38 mm for Hastelloy-X, and 0.51 mm for stainless steel. These rods will be removed after 5 a/o burnup. Other specimens will be irradiated to 2 and 10 a/o, as well as 5 a/o.

Fig. 9. PuC Particle with Adhering UC Particles in Specimen C-74 ($\times 400$).

Conclusions

The present difficulty in making large quantities of stoichiometric (U,Pu)C leaves slightly hyperstoichiometric carbide as the favored composition for fuels with today's technology. Techniques have been demonstrated for producing high-quality pressed-and-sintered pellets and vibratorily compacted fuel columns. Although compatibility tests have shown stainless steel to be an acceptable jacket material from this standpoint, there is still a need for making nonstoichiometric fuel more compatible with the jacket and for finding stronger jacket materials. The promising irradiation results on vibratorily compacted carbide indicate that this system is worthy of consideration for future reactors.

Acknowledgment

The authors wish to acknowledge the endeavors of many of their coworkers which made this paper possible.

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Discussion

Mr. Persiani (ANL): There have been two statements made about the mass transfer between fuel and clad. Mr. Neimark resorts to a gas-bonded system and thus, he says, carbon transfer is avoided or minimized; Mr. Siegel stated that he observed no transfer of metal to the clad with sodium bonding. I was wondering if Mr. Siegel might comment on any observation as to the carburization of the clad in the presence of a sodium bond.

Mr. Seigel (AI): We have observed carburization of the cladding with hyper materials. This occurs fairly rapidly. The comment I made earlier was that up to 1600°F we did not observe significant reaction between hypo material and stainless cladding to form low-melting eutectics.

Mr. Stachura (AI): You had excluded the sodium-bonded hypostoichiometric carbide on the basis of the possible free uranium-stainless reaction and on the basis of the questionable nature of having a fissioning material as the uranium phase in the grain boundaries. Is there firm experimental evidence that indicates that these are indeed serious problems?

Mr. Neimark: It would be difficult to so conclude from the work that we have done ourselves, but I think that it has been shown by the British that there is a significant amount of swelling attributed to a metal phase in grain boundaries. This was pointed out in a paper recently at the Detroit ANS meeting that, in a temperature range and at power densities necessary for fast reactors, this would be a considerably larger problem than it is in the thermal reactors for which a metal phase is now acceptable. I believe there is significant evidence to this effect.

Mr. Runnalls (AECL): It is possible to calculate from the available thermodynamic information that, as one irradiates plutonium carbide, the fission products require more carbon than can be provided by PuC and, therefore, one of the things that one might expect in a long-irradiated fuel is the possibility that plutonium metal could be freed. Perhaps it is more important than considerations of compatibility with the sheet (clad) to find out whether or not the plutonium in a liquid phase flows by gravity towards the

bottom of the fuel, that is, relocates itself. Do you have any experimental evidence from your irradiations of PuC which would seem to settle this question as to whether or not plutonium segregates towards the bottom of the element?

Mr. Neimark: We have just cursorily examined by metallography a few specimens. The high-burnup ones have not even been sectioned for examination. The problem you mention about needing an excess amount of carbon to take care of both the initial metal phases that are present when you load the rod and also those that are formed as fission products has been considered; it is what may be considered a secondary reason for going to a hyperstoichiometric fuel to begin with, to get some excess carbon in there. We will look for plutonium segregation, but have not seen it as yet.

Mr. Kittel (ANL): A question came up a few minutes ago about whether we had evidence that would lead us to prefer hyper mixed carbide over hypo material. Certainly fission-gas release is much higher in the hypo material as compared to the hyper.

Mr. Skavdahl (Battelle-Northwest): I am interested in your view of the Doppler effect and the rate of response of the Doppler effect in physically mixed PuC-UC. What relative response rate do you anticipate between, say, solid-solution carbide versus the physical mixture where the PuC is a separate pure phase?

Mr. Neimark: I cannot answer your question.

Mr. Benedict (MIT): It seems to me that one might anticipate a slight delay in the Doppler effect in the fertile material, but it would only be a few milliseconds at most. Why is it thought that there is any reduction in the overall effect when a new temperature is established?

Mr. Neimark: The uranium and plutonium atoms are separated by a finite amount, a greater amount than would be found in a solid-solution material, so there would be a time lag due to heat transfer.

Mr. Benedict: Yes, I can see that. But the end result, I would think, once the fuel had reached its new temperature distribution, would be to give about the same amount of reactivity change, would it not?

Mr. Persiani: The Doppler coefficient might be divided into two parts: the delay component and the prompt component. I think the prompt effect is somewhat compromised. The delayed component is somewhat increased when the fissile and fertile material is separated, but I don't think you compromise the whole Doppler effect.

Mr. Kummerer (Karlsruhe): One takes carbide fuel in order to have an importantly increased thermal conductivity. I wonder whether it is consistent to use

this carbide fuel in powder compaction form because every powder has a bad thermal conductivity?

Mr. Neimark: The increased thermal conductivity of the carbide allows you to use vibratory compaction, which is a desirable process. I would not make the same statement for an oxide.

Mr. Skavdahl: I am interested in your negative approach on the oxide, and I would like to hear some comment upon that.

Mr. Neimark: It was not a negative approach on the oxide; it was in defense of the carbide.

Mr. Gibson (AI): I note your comments on the hypo based upon the swelling problems of the hypo with the thermal bond; surely the swelling isn't such a serious problem. It would seem the principal disadvantage that you attribute to the hypo would be mostly restricted to the release of fission gases. Is that right?

Mr. Neimark: This was brought out; yes, it is one point. It is a major contributing factor why hyper would be considered better.

An Account of Some Work on the Safety Aspects of Carbide Fast Reactor Fuels

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1. Introduction

1.1 *The Choice of Fuel for a Fast Reactor*

Ceramic materials are becoming increasingly important as fast reactor fuels. Such fuels should be capable of achieving a high burnup in the region of 100,000 MWD/te to be economically attractive.

A current design for fast reactor fuel elements allows for gas release by voidage.^(1, 2) This means that a plenum space is included, and the volume of this space is calculated to allow for the effects of combined pressure stresses and thermal stresses. Such designs take account of the fact that irradiation results obtained with oxide fuels, at the heat ratings and to the burnups relevant to fast-reactor operation, have indicated that a large percentage of the fission gases is released from the fuel. Thus, in a fast reactor where the fuel is highly rated, i.e., ~ 200 W/g fuel, even with a small-diameter oxide pellet, the poor thermal conductivity of this fuel gives rise to high centre temperatures of the fuel, and this in turn leads to high percentages of gas release.

Carbide fuel offers two main advantages over oxide: it has a higher fissile density and a higher thermal conductivity. Thus, a carbide-fuelled fast reactor could have a more compact core, giving rise to a smaller inventory. The higher fissile density of carbide, and the fact that its structure comprises one nonmetallic atom per molecule compared with two for oxide, leads to better breeding characteristics. Improved performance may be expected to result from its higher thermal conductivity, but the question of how to take advantage of the relatively high thermal conductivity compared with oxide is still open.

One possible way is to design to the same parameters as for oxide fuel, so that low temperatures at the fuel centre are obtained with the possibility of low release of fission gas. A considerable amount of information on the irradiation behaviour of U-C (up to 30,000 MWD/te)^(3, 4) and (UPu)-C (up to 113,000 MWD/te)^(5, 6) has been reported by a number of workers. Their main conclusions are as follows:

- (1) There are no important differences in irradiation behaviour between UC and (UPu)C.
- (2) Hypostoichiometric material, containing free metal, shows a high rate of swelling and large gas releases.
- (3) Single-phase and hyperstoichiometric material is dimensionally stable to 50,000 MWD/te if subjected to some restraint. The swelling rate observed corresponds to about 1.6 % density decrease per 1 % burnup at the highest burnups.
- (4) Up to 30,000 MWD/te for UC and 40,000 MWD/te for (UPu)C and at moderate centre temperatures, fission gas release, with some exceptions, is low. At burnups of $\sim 100,000$ MWD/te and fuel temperatures of $\sim 1200^\circ\text{C}$, Strasser⁽⁶⁾ has recently reported appreciable gas release ($\sim 30\%$) from mixed carbide.
- (5) Examination of mixed carbides irradiated up to 113,000 MWD/te has not revealed any evidence for gross changes in the carbon-metal ratio or for the separation of solid fission products into a discrete phase.
- (6) Replica and thin-film electron microscopy have shown evidence for gas bubble formation in UC.^(7, 8)

In current work at Harwell, a gas-retentive carbide fuel is being examined, with the object of attaining a basic understanding of the behaviour of the fission gases in UC and (UPu)C in relation to their effects on gas release and swelling. The first part of the programme comprises six experiments which are designed to compare the irradiation behaviour of sintered and arc-cast fuel of composition $(\text{U}_{0.85}\text{Pu}_{0.15})\text{C}$. Burnup levels between 40,000 and 100,000 MWD/te are being achieved. Four of the experiments are being irradiated under fast reactor conditions and the remaining two at high ratings in a thermal flux. Irradiations have been completed for three experiments, and some of the postirradiation results are reported here. In all experiments, the carbide was prepared from arc-melted stock and the oxygen content of the fuel was generally $\lesssim 0.2$ w/o.

An attractive way of preparing monocarbides is by the carbothermic reduction of oxides. The second part of the irradiation programme, which is now getting underway, is designed to study the irradiation behaviour of such carbides at high burnup as a function of oxygen content. Oxy carbides, defined here as monocarbides containing $\lesssim 0.3$ w/o oxygen, can be prepared at lower temperatures or in higher CO pressures than monocarbides. These materials have obvious interest as reactor fuels provided that there is no significant deterioration in those properties which determine fuel behaviour under irradiation. Thus, in support of the irradiation work on these materials, uranium-plutonium oxy carbides prepared by the carbothermic reduction process have been pressed and sintered into pellets containing ~ 0.5 w/o oxygen, and used in tests which compared their high-temperature stability with that of uranium oxy carbides. The compatibility of (UPu) carbides with various cladding materials has also been studied.

In summary, three aspects of fuel and cladding behaviour of importance to the safety of an operating fuel element are under investigation:

- (1) the compatibility of (UPu)-C and U-C with 316L stainless steel cladding material under both gas-bonded and sodium-bonded conditions. The compatibility of (UPu)-C with a number of nickel-based alloys has been investigated;
- (2) the gas release and swelling behaviour of (UPu)-C of varying composition, using sintered, arc-cast, and powder fuel. Irradiations in both thermal and fast fluxes and being carried out;
- (3) with the growing interest in plutonium-bearing oxy carbides, some initial experiments have been completed which give some indication of the stability of these fuels at high temperatures.

The experimental results so far obtained are described and discussed in this paper.

2. High-burnup Miniature Fuel-element Irradiation

2.1 Introduction

As stated previously, one of the major objectives of the irradiation programme is to study the effect of fabrication and composition variables on the irradiation behaviour of $(U_{0.85}Pu_{0.15})$ -C. In one experiment, a sample has recently been irradiated in the Dounreay Fast Reactor (DFR) to a maximum fuel burnup of 5.2%. The results obtained from this experiment are described here.

2.2 Fabrication of the Fuel

Sintered Fuel. This fuel was prepared by crushing and milling arc-melted carbide, which was formed into

compacts at pressures up to 20–40 tsi. The pellets were sintered in flowing argon at 1550°C for 4 hr. Densities and analyses for Pu, C, O, and N were carried out on selected pellets.

Pre-irradiation details of the fuel are given in Table 1, and Fig. 1 shows typical fuel microstructures.

Powder Specimens. Two powder specimens were irradiated. These were prepared by crushing arc-melted fuel to $-30 + 200$ mesh ($500\text{--}74\mu$), and tapping this into the irradiation cans so that it occupied 60% of the internal volume of the cans.

Arc-cast Fuel. The experimental technique which was used for the preparation of the irradiation specimens has been described elsewhere.⁽⁹⁾ A 600-g skull was used, and precautions were taken to ensure uniform composition. Casting was carried out by the Durville method, using massive copper moulds. A typical pre-irradiation microstructure of the arc-cast fuel is shown in Fig. 2.

2.3 Irradiation Details

Figure 3 shows the general assembly of the fuel cans, and Table 1 gives the irradiation parameters for each specimen. Eleven specimens, each 1 in. long, were irradiated in 0.030-in.-thick Nimonic 90 alloy cans which were lined with tantalum, fabricated from co-extruded tubing. The ends of the cans were closed with Nimonic 90 end caps, which were seal welded into the can. A tantalum liner was also provided for the end caps integrally with tantalum spacers, the latter serving to position the specimens longitudinally in the can. Nimonic 90 cans were chosen to provide strong cans to counteract the effect of irradiation, but subsequently it was demonstrated that nickel-base alloys undergo considerable irradiation embrittlement. Since there was some doubt concerning the compatibility of (UPu)-C with Nimonic alloys at 800°C and above, the tantalum liners were incorporated to prevent reaction between the fuel and can, and thus ensure the maximum chance of achieving the target burnups without fuel-can interaction.

It was, however, also suspected that some incompatibility might exist between tantalum and Nimonic 90 in view of the information available on the Ni-Ta phase diagram.⁽¹⁰⁾ The specimens were a very close diametral fit in the cans, the radial clearance being about 0.0003 in., and the cans were filled with reactor-grade helium. Heat removal from the specimen cans was through close fitting matrix blocks of steel, selected for their thermal conductivities in relation to the required temperature differences across them. These blocks were in turn a good fit in the outer sheath, so that a solid metal path of controlled thermal resistance

TABLE 1. FAST REACTOR EXPERIMENT—SPECIMEN DETAILS

Can Number and Fuel Type	Specimen Number	Specimen Length and Diameter (in.)	Specimen Composition (w/o)					Equivalent Carbon Content (w/o)	Microstructure	Density (g/cm ³)		Specimen Rating (W/g)	Estimated Surface Temp (°C)	Estimated Centre Temp (°C)	Estimated Burnup (% heavy atoms)	Estimated Fission Gas Release (%)
			C	O	N	Pu	U			Sintered Bulk	Theoretical					
1 Sintered	8139	1.024 0.32	4.76	0.24	0.04	13.0	82.0	4.97	MC + M ₂ C ₃	12.9	13.4	144	1040	1610	3.95	—
2 Sintered	8140	1.018 0.29	5.32	0.07	0.02	12.6	81.99	5.38	MC + M ₂ C ₃	12.8	13.3	162	940	1440	4.45	—
3 Sintered	8141	1.015 0.27	4.73	0.09	0.07	13.2	81.90	4.86	MC + M ₂ C ₃	13.0	13.4	180	950	1460	4.95	—
4 Sintered	8142	1.013 0.26	5.32	0.07	0.02	12.6	81.99	5.38	MC + M ₂ C ₃	12.8	13.3	190	930	1430	5.20	—
5 Arc-cast	8143	1.068 0.25	4.76	—	—	—	—	—	MC + trace of free metal	—	—	199	940	1420	5.40	—
6 Sintered	8144	1.024 0.25	4.76	0.24	0.04	13.0	82.0	4.97	MC + M ₂ C ₃	12.9	13.4	201	950	1430	5.50	—
7 Powder	8145	1.023 0.25	5.32	0.07	0.02	12.6	82.0	5.39	—	60% T.D.	13.3	199	800	1270	5.40	0.2-0.5
8 Powder	8146	1.016 0.26	5.32	0.07	0.02	12.6	82.0	5.39	—	60% T.D.	13.3	190	800	1250	5.20	1.25-2.5
9 Arc-cast	8147	1.017 0.27	4.75	—	—	14.2	—	—	MC + trace of free metal	13.36	13.6	180	930	1410	4.95	—
10 Sintered	8148	1.022 0.29	4.00	0.07	0.10	14.3	81.53	4.14	MC in metal matrix	13.5	14.2	162	990	1530	4.45	—
11 Arc-cast	8149	1.016 0.32	4.80	—	—	—	—	—	MC + trace of free metal	13.54	13.6	144	1020	1610	3.95	—

FIG. 1 Pre irradiation Microstructures of Sintered ($U_{0.8}Pu_{0.1}$)C (a) Typical of Cans 1 and 6 ($MC + MC_3$) ($\times 235$) (b) Typical of Can 3 ($MC + M_2C_3$) ($\times 94$) (c) Typical of Cans 2 and 4 ($MC + MC_3$) ($\times 235$) (d) Typical of Can 10 (MC in Metal Matrix) ($\times 940$)

FIG. 2. Pre-irradiation Microstructure of Arc-cast ($U_{0.85}$ - $Pu_{0.15}$)C. Typical of Cans 5, 9, and 11. ($\times 500$)

carried the heat from the specimens to the reactor coolant.

Because of the marked longitudinal variation in the reactor flux, the specimen diameters were varied from 0.30 in. at either end of the rig to 0.26 in. at the centre, so that the linear ratings of each specimen were roughly constant. In the period prior to insertion of the rig into the DFR, the operating power was increased from 30 to 58 MW, and this could only be partially accommodated by moving the rig to a different position in the core. In fact, as a result of this, the operating conditions for the rig were far more severe than those for which it was designed, and this has caused many failures in the Nimonic 90 cans. These will be discussed later.

2.4 Postirradiation Examination

Radiography and Macroscopic Examination. Radiography showed that four cans (samples 4, 6, 10, and 11) had failed very severely; in all but one case (can 11), one end cap had fractured from the can body. Can 10, which contained sintered hypostoichiometric carbide, was a particularly serious failure. Gross swelling of the fuel and the matrix block (see Fig. 4) was found. This is not surprising in view of the large proportion of free metal in this specimen, as shown in the pre-irradiation

structure in Fig. 1D. Can 11 was extracted from its matrix block, by machining down the mild steel, to investigate the general condition of the fuel can. The fuel can was found to be badly cracked both circumferentially and along its length, and eventually fell into two pieces during handling (see Fig. 5). At this stage, it was decided to carry out any further investigations with the fuel cans still in position in the matrix blocks; seven cans remained to be examined, two of which (cans 7 and 8 containing powder fuel) were known to be intact.

Release of Fission Gas. The remaining seven cans were punctured to determine the levels of gas released from the fuel. Gas samples were obtained only from cans 7 and 8, suggesting that the other 5 cans containing solid fuel had failed. Table 1 gives the estimated gas releases from cans 7 and 8, which were very low.

Metallography of the Fuel. Individual fuel cans were recovered from their matrix blocks, and selected pellets (or slices of fuel in the case of the arc-cast material) and sheathing were extracted from near the centre and ends of each fuel element. The sheathed pellets or slices of fuel were then prepared for metallography.

The powder fuel in cans 7 and 8 was first exposed by slitting the cladding at one end, near to the tantalum end shim. The fuel was found to have densified and was no longer a fine powder. Examination of thin slices taken from the end and centre of the element revealed that the powder had sintered during irradiation to form a porous mass. This effect was most pronounced at the centre of the fuel sections, whereas in the circumferential regions the original particle size persisted (see Fig. 6).

Examination of pellets from all the sintered elements indicated that, with the exception of Can 10, there was little change in the microstructure, other than cracking of the fuel. Can 10, which contained hypostoichiometric fuel, was found to be severely distorted (as shown in Fig. 4 previously). This dimensional change is believed to be caused by the free metal present in this fuel, although postirradiation microscopy indicated that the metal phase had migrated during irradiation (see Fig. 7).

The three elements containing arc-cast fuel have also been examined. In all cases, the fuel had not undergone any serious changes, although there was a marked increase in the amount of grain-interior porosity. There is no doubt due to the agglomeration of fission gases into resolvable gas-filled voids.

Can 11 containing arc-cast fuel exhibited a serious circumferential crack in the fuel can and subsequently fell into two pieces during handling. The smaller half was sectioned transversely and longitudinally. The transverse section is shown in Fig. 8 and the longitudinal

FIG. 3. General Assembly of Irradiation Fuel Cans.

FIG. 4 Matrix Block Containing Can 10 Showing Distortion due to Swelling of the Hypostoichiometric Fuel

FIG. 5 Failure of Can 11 due to Serious Circumferential Crack

section in Fig. 9. From Fig. 8 it appears that little change, other than cracking, had occurred in the fuel, the columnar grain structure which was evident in the circumferential regions of the fuel before irradiation

persisted. For the hotter regions of the fuel, there is some evidence in Fig. 9 for reaction between the hot fuel and the tantalum end spacer, and between the tantalum spacer and the Nimonic 90 end cap. Figure 10 illustrates

FIG. 6 Microstructures Showing Partial Densification of the Powder Fuel in Can 7 ($\times 153$)

the high concentration of grain interior porosity in the fuel structure

2.5 The Compatibility of Nimonic 90 and Tantalum

As stated before, Nimonic 90 cladding was selected to provide strong cans, and the tantalum interlayer was incorporated because the compatibility of (UPu) C with Nimonic alloys at temperatures $>800^{\circ}\text{C}$ was doubtful. However, it was also suspected that some incompatibility might exist between tantalum and Nimonic 90. Because of these doubts, a series of laboratory compatibility tests⁽¹¹⁾ were started on Nimonic 90 and tantalum very soon after insertion of the rig into the reactor.

Since there was a possibility that nickel might diffuse right through the tantalum and react with the carbide, couples consisting of Nimonic 90 and carbide separated by 0.006 in thick tantalum sheet were maintained in contact by differential expansion in a capsule of the kind shown in Fig. 11. In this way, any incompatibility between the tantalum-Nimonic diffusion products and

the carbide could also be noted if penetration of the tantalum occurred. Initially, three test temperatures were used: 760, 830, and 900°C , to enable approximate inter- and extrapolation of the results to be made. However, during the course of the tests, it became clear that the actual irradiation tests were running at higher temperatures than originally envisaged, in an attempt to produce 1000°C data, couples already running at lower temperatures were rerun at the higher temperature and the time at the lower temperature converted to an equivalent time at 1000°C , using the activation energy derived from the low temperature runs.

The heat-treated couples were examined by metallography and electron-microprobe analysis. No reaction was observed at 760°C . The results for the higher temperatures are plotted in Fig. 12; the derived diffusion coefficients and activation energy are given in Table 2. The growth appeared to follow the expected parabolic law initially, but then slowed down, although the zones were slightly irregular and the plotted data, which

relate to the maximum penetration depth into the tantalum, are therefore somewhat uncertain, the effect seems to be genuine. This has complicated the interpretation, and the pairs of parabolas shown at 830 and 900°C in Fig. 12 relate, firstly, to the best fit to the initial points (<120 days) and, secondly, to all the points on the basis of minimum deviations. It is doubtful if a simple Arrhenius plot is applicable in the present case, and it is not surprising therefore that the parabolas fitted to the derived points at 1000°C give values for the diffusion coefficient which are rather greater than the calculated diffusion coefficient from the data at lower temperatures.

Microscopic examination revealed that for the Nimonic side of the couple, depletion of Ni_3Al and Cr_{23}C_6 occurred in the surface layers (see Fig. 13) whilst on the tantalum side a narrow, and probably isotropic, surface zone was followed by a wider anisotropic zone (see Fig. 14). The compositions of these zones have not been fully determined, but microprobe analysis indicated that cobalt is present in large amounts in the anisotropic zone which also contains nickel to an ap-

preciable level. Chromium is also present, but the Cr/Ni ratio appears to be lower than in the Nimonic. No data were obtained for aluminium or titanium.

In view of the depletion of Cr_{23}C_6 , it is tempting to suggest that the narrow surface zone is tantalum carbide; the formation of such a zone might explain the gradual departure of the zone depth below the value expected from a parabolic growth law. (There is, however, no confirmatory evidence of the composition of this zone.) Such a theory might also account for the relatively slow diffusion of Nimonic 90 into tantalum compared to the reported behaviour of pure nickel.

In spite of the uncertainties in interpretation, the experiment clearly demonstrated that there was no danger of the 0.006-in. tantalum foil being penetrated by the Nimonic during the carbide-irradiation experiments described herein. Even at 1000°C, the time required to penetrate the tantalum would be about 1 year. In fact, postirradiation metallographic examination of sections from all eleven cans revealed that the depth of penetration was very slight in all cases.

2.6 The Cause of Radial Cracking in Can 11

Postirradiation examination of the fuel cans from this rig has revealed a variety of can failures, and the cause of failure in all cases is being studied in some detail. Only can 11 has been considered so far. Looking specifically at this can, two main modes of failure were apparent:

- (1) complete circumferential fracture of the Nimonic can (see Fig. 5) which may have been caused by fuel swelling and axial tensile failure;
- (2) a number of radial cracks in the can walls, which seem to propagate inwards from the outer surface. Some of these cracks do not propagate entirely across the wall thickness, but terminate at varying distances across the section (see Fig. 8). This mode of failure is due to thermal stressing and is discussed in more detail below.

The can was designed to have a wall thickness such as to minimise the stresses arising from the radial transmission of heat across the wall, and from the containment of an internal pressure resulting from release of fission gas.

The initial stress system was predominantly thermal, and it was well within the capabilities of the unirradiated clad material to withstand these stresses. Indeed, a substantial allowance had been made for any deterioration in the material strength with neutron irradiation.

As stated previously, the operating conditions for the rig were far more severe than anticipated, and thermal stresses in the can wall were excessive. In fact, the can was originally designed to operate at 612°C, but actually achieved a temperature of 870°C, and

FIG. 7. Postirradiation Microstructure of Metal-rich Fuel in Can 10.

Note absence of free metal compared with pre-irradiation structure. ($\times 342$)

FIG. 8 Transverse Section of Al₂O₃ cast Fuel in Can 11
Note failures in Nimonic 90 can (X14)

the can was subjected to a maximum tensile thermal stress of 8.3 ksi. At this higher temperature, the thermal stress exceeds the 100 ksi rupture stress for the Nimonic 90 cladding material, and it is therefore reasonable to suppose that the cracks observed in the can wall were initiated within the first week of irradiation, when the mechanical properties of the clad material would have been relatively unaffected by radiation damage (the neutron dose rate was $\sim 5 \times 10^{18}$ neutrons/cm²/hour).

As the cracks propagated through the wall, the effective thickness of can material ahead of each crack was reduced. If we ignore stress concentrations at the

crack tips, an approximation for the changing stress pattern can be obtained by calculating the stresses in successively smaller can wall thickness.

The thermal stresses in the can wall can be obtained using an equation derived by Timoshenko ⁽¹²⁾

$$(\sigma_t)_{r=r_2} = \frac{E\alpha\Delta T}{2(1-\mu) \log_e \frac{r_2}{r_1}} \left(1 - \frac{2r_1^2}{r_2^2 - r_1^2} \log_e \frac{r_2}{r_1} \right), \quad (1)$$

FIG. 9. Longitudinal Section of Fuel, End Spacer, and End Cap in Can 11.
Note regions of reaction between fuel and spacer, and between end cap and spacer. ($\times 16$)

where σ_t is the thermal stress, r_1 is the can inner radius, r_2 the can outer radius, E is Young's modulus, α is the coefficient of thermal expansion, μ is Poisson's ratio, and T is the temperature drop across the can wall.

Poisson's ratio can be determined from E and the rigidity modulus G by means of the expression

$$\mu = \frac{E}{2G} - 1.$$

The thermal stress values obtained from these calcula-

tions are as follows:

Thermal Stress in Outer Fibres of Can Wall or at the Tips of Cracks (tsi)	Crack Propagation as % of Wall Thickness (%)
8.3	0
6.05	31
2.60	69

Figure 15 is a plot of the rupture and proof stresses for Nimonic 90, in which the calculated values shown above have been superimposed. The calculated values

FIG. 10 Grain interior Porosity in Arc cast Fuel (Can 11) ($\times 150$)

in Fig. 15 confirm the belief that failure in can 11, by radial cracking in the can wall, would have occurred during the first week of irradiation. Calculations similar to those presented above are at present being made for the other ten cans in this irradiation assembly

3 Compatibility Studies of $(U_{0.85}Pu_{0.15})-C$

3.1 Introduction

The compatibility of arc cast specimens of $(U_{0.85}Pu_{0.15})-C$ with 316L stainless steel and certain higher nickel alloys has been studied at temperatures up to 900°C . In addition, a limited number of tests have been carried out for both $U-C$ and $(UPu)-C$ with 316L steel in the presence of a sodium bond. The dry compatibility tests were undertaken with couples sealed in welded, thick wall Nimonic 80A cans using expansion rods of 347 steel to ensure that contact was maintained between the constituents of the couples at operating temperatures. A case of known incompatibility was used to test the cans at the maximum temperature employed, 900°C , to ensure that creep of the Nimonic

80A was not causing loss of contact. In the sodium tests, cans of 316L steel were used, in which was placed the carbide surrounded by a sodium interlayer, the inner surface of the can acted as the specimen.

In the dry tests, evidence has been sought for possible transfer of carbon to the steel and also for plutonium or uranium transfer, with associated diffusion and compound formation.

An upper limit to the temperature at which the materials can be compatible is provided by the formation of liquid phases. Thus, for hypostoichiometric $(UPu)-C$, a plutonium rich metal phase is present which can form a eutectic with nickel at temperatures of less than 500°C . With stoichiometric and hyperstoichiometric $(UPu)-C$, eutectics occur between the monocarbide and the transition metals. For $UC-Fe$, the eutectic temperature is 1113°C ⁽¹³⁾ and is lowered by addition of chromium or nickel,⁽¹⁴⁾ the relevant value for 316L stainless steel being 1003°C . Surprisingly, the value for $(U_{0.85}Pu_{0.15})-C$ with 316L is slightly higher, being 1029°C .

FIG. 11 General Assembly of Compatibility Capsule

PENETRATION OF NIMONIC 90 INTO TANTALUM.

FIG. 12. Penetration of Nimonic 90 into Tantalum.

Curves 1: best fit to experimental points up to 50 days; Curves 2: best fit to experimental points up to 200 days.

3.2 (UPu)-C/316L Compatibility

Compatibility studies with 316L steels are complicated by the variable response to heat treatments of steels from different manufacturers. In the present work, vacuum melted steels supplied by Samuel Fox, Firth-Vickers, and Telcon, together with an air melted Willan steel, have been studied at various times. The as-received steels, after ageing at 650–900°C, all showed a surface layer of cold-work-induced precipitation, the precipitates being mainly carbide with some σ phase (see Fig. 16). The recommended solution treatment of 20 min at 1050°C⁽¹⁵⁾ eliminated cold-work effects in the Telcon and Firth Vickers steels, but not in that from Samuel Fox; indeed, extending the heat treatment times to 24 hr and the temperature to 1100°C failed to remove the effect completely in the latter steel.

Details of the heat-treatment conditions for the compatibility couples are given in Table 3. Transverse sections through the interface zone, at points where carbide had stuck to the steel, did not show any reaction products in either the steel or the carbide (see Fig. 17), nor was there any evidence of depletion of MC_2 or M_2C_3 phases in the hyperstoichiometric carbides.

Some localised carburisation was noticed at points of poor contact, but the zone depth was slight and showed no increase with time or temperature. There is

TABLE 2. DIFFUSION DATA FOR PENETRATION OF NIMONIC 90 INTO TANTALUM

Temperature (°C)	D(cm ² /sec) × 10 ⁻⁷		Activation Energy (kcal)	
	Curves 1	Curves 2	Curves 1	Curves 2
830	2.0	3.4	48.9	40.5
900	7.7	10.3		
1000	40–68*	39.6–65.5*		

* First figure is value derived from data at lower temperature; second figure from 1000°C parabolas.

evidence that this is not an effect due to cold working; it could be due either to gas-phase carburising associated with the presence of hydrogen in the carbides, as suggested by Batey,⁽¹⁶⁾ or to surface diffusion. On the available evidence it is believed that gas-phase carburising is the most probable mechanism. (This phenomenon is discussed in detail in a paper by French and Hodkin to be presented at the Third International Conference on Plutonium, London, November 1965.)

Autoradiographic studies confirmed that no diffusion of plutonium into the steel had occurred, which may be taken as further evidence of the essential compatibility of stoichiometric and hyperstoichiometric ($U_{0.85}$ – $Pu_{0.15}$)-C with 316L steel at temperatures up to 900°C.

FIG. 13. Ta-Nimonic 90 Interface after Heating for 117 Days at 830°C.
Shows typical reaction zone on Nimonic 90 side ($\times 750$)

FIG. 14. Ta-Nimonic 90 Interface after Heating for 117 Days at 830°C.
Shows typical reaction zone on Ta Side ($\times 750$)

3.3 The Behaviour of U-C/316L and (UPu)-C/316L in the Presence of Sodium

In view of the carburisation of stainless steel by hyperstoichiometric U-C in the presence of sodium as reported by Webb,⁽¹⁷⁾ an attempt has been made to compare the behaviour of (UPu)-C containing MC_2 needles plus a grain-boundary precipitate of the M_2C_3 phase with U-C of a similar carbon content but with all excess carbon present as UC_2 .

For periods of up to 3 months at temperatures up to 800°C, depletion of the MC_2 phase occurred in both materials, but with no depletion of M_2C_3 in the mixed

carbide (see Figs. 18 and 19). Fine globular precipitates were seen on slip planes in the stainless steel, together with coarser precipitation in the surface layer (see Fig. 20). The zone depth is plotted in Fig. 21; the exact carbon equivalence of each alloy is unknown, but undoubtedly they are all hyperstoichiometric, oxygen and nitrogen contents totalling upwards of 500 ppm. In general, the rate of carburisation was less for (UPu)-C than for U-C for the same carbon content, presumably due to the smaller amount of MC_2 present in the mixed carbide.

Metallographic examination of the lowest carbon

content quoted for the mixed carbide, namely, 4.79 w/o C, suggests a composition equivalent of about 4.9 w/o C. Detectable carbon transfer can thus occur in a material which is only slightly hyperstoichiometric, provided that the excess carbon is present to some extent as MC_2 . It should be noted that the structure of the second phase in sintered material is very different from that in arc-cast, and further work is needed to

TABLE 3. MAXIMUM HEAT TREATMENT TIMES FOR (UPu)-C/316L COUPLES

Steel	Carbon Content of (UPu)-C (wt %)	Heat Treatment Temperature (°C)	Time (days)
Willan (Air-melted)	4.72	800	28
	4.72	900	28
	4.88	800	56
	4.88	850	28
	4.88	900	28
	5.06	800	86
Samuel Fox and Firth Vickers (Both Vacuum-melted)	4.88	650	56
	4.88	800	56
	4.82	900	56

determine the influence of such differences on compatibility in the presence of sodium.

3.4 Compatibility of (UPu)-C with Nickel-based Alloys

Hyperstoichiometric mixed carbide reacted with nickel within 1 week at 800 or 900°C. The reaction products consisted of free carbon and (UPu)-Ni compounds. The depth of the reaction zone was 0.003 in. at 800°C after a heat treatment for 1 week.

The nickel alloy EPE7 (36.7 Ni, 18.1 Cr, 4.7 Mo, 2.5 Ti and Al, 0.17 Si, with Fe to balance) showed a fine globular carbide precipitate after 14 days at 800°C, accompanied by depletion of the MC_2 phase in the surface of the carbide.

Nimonic 80A showed localised reaction after 56 days at 650°C, again accompanied by depletion of the MC_2 in the carbide. At temperatures of 800–900°C, severe local pitting of the Nimonic occurred, and a

FIG. 15. Relationship between the Thermal Stresses and Material Strength in Can 11.

FIG. 16. Typical Precipitation in the Cold-worked Layer of 316L Stainless Steel. (×250)

FIG. 17 Interface of (UPu) 4.88 w/o C/316L Stainless Steel after Heating for 56 Days at 900°C. No Reaction ($\times 750$).

FIG. 18 Interface of U 4.89 w/o C/Na/316L after 28 Days at 700°C

Shows Typical Depletion of MC_2 Phase from Carbide Surface ($\times 250$)

continuous reaction zone was observed (see Fig. 22) that reached a depth of 0.0016 m after 28 days at 900°C.

The behaviour of such clads with (UPu) C appears to be worst at intermediate nickel contents, this may

FIG. 19 Interface of (UPu) 5.22 w/o C/Na/316L after 84 Days at 800°C

Shows Typical Depletion of MC_2 Phase from Carbide Surface. No Depletion of M_2C_3 Phase ($\times 250$)

be compared with the behaviour with U C, which improves steadily as the nickel content of the clad is raised⁽¹⁸⁾ and with Pu C which worsens with rising nickel content⁽¹⁹⁾

FIG. 20. Interface of U-4.89 w/o C/Na/316L after 84 days at 700°C.
Shows typical carburisation of steel. ($\times 1000$)

4. The Stability of Oxycarbides at High Temperatures

4.1 Introduction

Oxycarbides are known to be unstable at very high temperatures, and this property is used to reduce the oxygen content of carbides to low levels by melting. However, the temperatures encountered in operating carbide fuel pellets are likely to be well below 2000°C (at least in the case of a 0.25-in.-dia pellet operating at 200 W/g), and thus it is of interest to determine the conditions under which decomposition of oxycarbides is likely to occur.

The pressure-temperature-composition curves in Fig. 23 indicate that at 1700°C, uranium oxycarbides containing more than 10 m/o UO will tend to decompose by loss of CO to yield uranium and oxycarbides of lower oxygen content when the CO pressure falls below 10^{-2} mm. Whilst this relationship has not been precisely determined, the general behaviour is supported by reports from various workers. Anselin *et al.*,⁽²⁰⁾ for example, state that whereas UC containing <5 m/o UO₂ is stable in vacuum at 1800°C, decomposition of oxycarbides containing >15 m/o UO occurs in a vacuum of 10^{-5} mm Hg at 1600°C.

4.2 Recent Experimental Results

At Harwell, Roberts and Regan⁽²¹⁾ have studied the behaviour of 1-3-g pellets of UC_{1-x}O_x and (UPu)-C_{1-x}O_x containing 0.5 w/o oxygen during heat treatment in tantalum containers in vacuum or argon, at temperatures between 1600°C and 1800°C. The tests

were performed in separate furnaces, A for UC_{1-x}O_x and B for (UPu)C_{1-x}O_x, and the single cross check which was carried out with a uranium oxycarbide containing 0.4 w/o oxygen yielded different weight losses, which were 25% in A and 12.8% in B after 65 hr at 1800°C. The ultimate pressure attained in both furnaces was 5×10^{-6} mm, but the geometries of the furnaces were different, and this could well account for the observed discrepancy. Nevertheless, although the absolute values of results obtained may not be in close agreement, the trends which emerge from these tests, which are described below, are believed to be real.

Thus, the rate of weight loss from these oxycarbides when heated at 1800°C in vacuum is initially high, but falls with increase of heat-treatment time. Increasing the amount of plutonium over the range 10-25% of (UPu)C_{1-x}O_x for a constant initial oxygen content of 0.4-0.5% appears to increase the stability quite markedly, so that the weight loss after 65 hr for (U_{0.75}-Pu_{0.25})C_{1-x}O_x is 0.16 of that for (U_{0.85}Pu_{0.15})C_{1-x}O_x, and is 0.4-0.08 of that for UC_{1-x}O_x. This is illustrated in Fig. 24. As shown in Fig. 25, a considerable decrease in the weight loss is found when the heat-treatment temperature is lowered to 1600°C, the relative decrease being greater for the specimens containing plutonium.

An investigation into composition changes taking place during heat treatment revealed that the period of greatest weight loss corresponds to the period during which the oxygen content is decreasing most rapidly (see Fig. 26). The (U_{0.85}Pu_{0.15})C_{1-x}O_x specimens showed the same general behaviour as UC_{1-x}O_x, but the final much higher. This observation provides further evi-

FIG. 21. Curves Illustrating the Rate of Carburisation of Steel. (Sodium Compatibility Tests.)

dence for the increased stability of plutonium-containing oxycarbides as compared with uranium oxycarbides. The period over which most of the oxygen was lost also corresponds to the period over which plutonium

losses were highest. This suggests that the principal mechanism for plutonium loss may involve the volatilisation of either PuO or an oxygen-rich plutonium oxycarbide. Losses of plutonium during the reaction of

FIG. 22. Interface of (UPu)-4.89 w/o C/Nimonic 80A after 28 Days at 800°C. Typical reaction product in carbide and localised pitting in Nimonic 80A. ($\times 750$)

oxides with carbon in vacuum have also been reported by Horspool *et al.*,⁽²²⁾ and others.

There is an initial decrease in carbon content as CO is lost, but thereafter the carbon content shows a tendency to increase, particularly after long heat treatments at high temperatures. The microstructures of heat-treated pellets tend to be single phase at their centres and show evidence of free metal near their outer surfaces (see Fig. 27). These microstructures support the results of chemical analyses which show that the equivalent carbon content, i.e., w/o C + (12/16) w/o O, has remained essentially constant during heat treatment, and that the decrease in oxygen content has been balanced by an increase in carbon content.

The dependence of the stability of oxycarbides on oxygen content has not been studied in any great detail, but one recent experiment carried out in vacuum at 1800°C showed that, whereas $(U_{0.85}Pu_{0.15})C_{1-x}O_x$ containing 0.49 w/o oxygen lost 7.8 w/o after 65 hr, a sample of similar composition but containing 0.10 w/o oxygen lost only 0.95 w/o over the same period. Calculations based on the Langmuir evaporation equation indicate that the weight loss from uranium oxycarbides is greater than that anticipated from oxygen-free UC by vapourisation of uranium. However, application of the same equation to (UPu)C indicates that the weight loss may be lower in oxycarbides than in oxygen-free material, but in this case the values for the vapour pressure of uranium over (UPu)C, as measured by Stahl and Strasser, are 10–100 times higher than the values used for the vapour pressure of uranium over UC.

Since the conditions inside an operating fuel can are

very different from those existing inside a vacuum furnace at 1800°C, most of the above-mentioned results will tend to give a pessimistic impression of the in-pile stability of oxycarbides. With a fuel centre temperature of 1100–1300°C, and a fuel surface temperature of $\sim 900^\circ\text{C}$, very little decomposition would occur, even if the atmosphere in the fuel can was maintained at 10^{-6} mm CO or if the fuel element was of a vented design.

The tendency to decompose during operation will be further reduced by the presence of other gases, such as helium filler gas, and xenon and krypton fission gases, and by the rapidly established equilibrium CO pressure, unless some fuel-element components act as getters for the CO. To investigate the influence of another gas on the suppression of volatility, a test was performed in which a sample of $(U_{0.85}Pu_{0.15})C_{1-x}$ containing 0.49% oxygen was heated for 20 hr at 1700°C in 1 atm of argon. Under these conditions there was no detectable change in composition, and the weight loss was 0.47%. When a similar pellet was heated in a helium pressure of 0.3 atm inside a sealed tantalum can, one end of which was maintained at 1600°C for 65 hr, and the other maintained at $\sim 800^\circ\text{C}$, the pellet which was situated at the hot end lost only 0.11 w/o, whereas a pellet of similar composition lost 0.51 w/o when heated for the same time at 1600°C in vacuum.

5. Discussion

The work described in this paper is mainly relevant to the behaviour of individual fuel elements in a fast reactor. It must, however, be appreciated that the work has wider implications, in that changes within a single

FIG. 23. CO Pressures in Equilibrium with UC_{1-x}O_x.

fuel element may influence the behaviour of other fuel elements in a cluster or subassembly, and thereby affect the behaviour of the reactor core itself. Examples of such interactions are axial redistribution of plutonium within the fuel element and geometrical changes in the

fuel element caused by fuel swelling. Such effects could perturb the coolant flow through the narrow passages between the fuel elements and cause a progressive change in other fuel elements comprising a subassembly. A consideration of the consequences of the deterioration

FIG. 24. Weight Loss in Vacuum at 1800°C from Uranium- and Uranium-Plutonium-Oxycarbides.

of a fuel element are outside the scope of this paper, in which it is assumed that the criteria for safety of the fuel element are that it should remain intact and geometrically stable throughout its life, and that no major redistribution of the fuel or its constituents should occur.

In a fast reactor, in which the fuel is required to go to high burnup, the production and behaviour of the gaseous and nongaseous fission products will probably determine the useful life of the fuel. Thus, even in oxide fuel, for which centre temperatures are high and a large proportion of the fission gases is released, those fission products which are retained in the cooler regions of the fuel may eventually cause gross fuel swelling.

The fact that carbide seems to be a gas-retentive fuel means that it is very important to understand the behaviour of the retained gases and solids, especially as they may increase fuel swelling and limit the burnup attainable. For any fissile material, there is a practical limit to the concentration of foreign atoms, e.g., fission product atoms, which it can accommodate and remain dimensionally stable. This limit will depend upon fuel rating and temperature, but even in the lower-temperature regions of the fuel, swelling and gas release will eventually occur. At Harwell, the irradiation work on carbides is concerned with the behaviour of

these fuels under conditions where fission product gas release may be kept at a minimum level ($<10\%$ of the gases produced). This requirement dictates that the fuel centre temperature should not exceed 1500–1600°C, which is the temperature at which diffusion release becomes significant. Fortunately, the thermal conductivity of (UPu)-C is sufficiently high to fix fuel centre temperatures of 0.25-in.-diameter pellets operating at ~ 200 W/gm at about 1100–1300°C. This may give a reasonable margin of safety sufficient to permit short-term involuntary increases in reactor power, but more work is required to demonstrate this fact. The two powder specimens irradiated to $\sim 5.2\%$ burnup were found to give low gas-release values, and it seems reasonable to conclude from this that the gas releases from the remaining nine elements containing solid fuel were also low. In this connection, Strasser⁽⁶⁾ has reported that at centre temperatures of 1200°C, gas releases from $(U_{0.80}Pu_{0.20})$ -C are low ($<1.5\%$) up to 40,000 MWD/te, but become appreciable ($\sim 30\%$) at 100,000 MWD/te.

More recently at Harwell irradiation of $(U_{0.85}Pu_{0.15})$ -C to burnups of 90,000 MWD/te at fuel centre temperatures of $\sim 850^\circ\text{C}$ has given gas releases of $<1\%$.⁽²³⁾

In the light of these results, and on the basis of the

FIG. 25. Weight Loss from Oxycarbides after Heat Treatment for 65 Hr at Various Temperatures.

FIG. 26. Change in Oxygen and Plutonium Contents after Heat Treatment at 1800°C in Vacuum.

preceding arguments which imply an enhanced swelling rate for a fission product-retentive fuel, possible means of accommodating fuel swelling must be sought. Several ways can be considered:

- (1) Strengthen the can by increasing its wall thickness, and thereby restrain the swelling fuel. This method may affect the neutron physics of the system; in any case, high thermal stresses in the can may preclude it as a useful technique.
- (2) Use a stronger clad, avoiding neutron-absorbing materials such as tantalum and molybdenum. The favoured cladding materials are type 316 or 304 stainless steel, although in a thermal flux their ductilities decrease markedly above 600°C and exposures of 10^{20} nvt. This embrittlement is believed to be caused by the agglomeration of helium or lithium atoms, which are produced by the transmutation of boron. The effect of

neutron irradiation on various cladding materials is considered in detail elsewhere.⁽²⁴⁾

- (3) Develop a fuel composition which can accommodate swelling when subjected to sufficient restraint. Preliminary data suggest that oxycarbides containing ~ 1 w/o oxygen may be considerably more plastic than monocarbide at $\sim 1000^\circ\text{C}$, and this may have important consequences in this connection.
- (4) Develop the sodium-bonded element concept to accommodate either fission product release or fuel swelling due to fission product retention. Because of the strong possibility of carbon transfer from hyperstoichiometric carbide, this method necessitates very close control on fuel stoichiometry.
- (5) Control the movement and growth of fission gas bubbles by making minor additions to the carbide.

FIG. 27. Microstructure of $(U_{0.53}Pu_{0.47})C_{1-x}O_x$ Pellet (Initial Oxygen Content 0.49 w/o after Heat Treatment for 120 Hr in Vacuum at $1800^{\circ}C$). (a) Surface Showing Metal (White). (b) Centre. ($\times 250$)

Because of the improved plasticity of oxycarbide, and also for the reasons stated earlier in the paper, the high-burnup irradiation behaviour of this fuel is being studied at Harwell. Most of the concepts described above will be examined in this programme.

A further aspect of the safety of a fuel element concerns the compatibility of fuel with clad both under normal operating conditions and under fault conditions leading to power transients. From the work described in this paper and other published information, it is clear that no compatibility problems arise between stoichiometric carbide and stainless steel at probable operating temperatures in the absence of a sodium bond, the slight localised carburisation seen at points of poor contact being of no practical significance. It would seem that short-term excursions of the clad in excess of $900^{\circ}C$ can also be tolerated, but that the formation of liquid phases above about $1000^{\circ}C$ sets an absolute limit to any permissible excursion lasting more than a few seconds. With hypostoichiometric carbides, the existence of low-melting phases with nickel above $500^{\circ}C$ makes the compatibility of this material somewhat uncertain. The material used in this work in some instances contained slight traces of free uranium without there being evidence of incompati-

bility, but no testing of marked hypostoichiometric material in dry contact with stainless steel has so far been reported in the literature.

The presence of a sodium bond radically alters the compatibility of hyperstoichiometric carbides with stainless steel. Sinizer⁽²³⁾ established that serious carburisation of steel cladding occurred when excess carbon was present as dicarbide, and Webb has shown⁽¹⁷⁾ that this effect is reduced if the sesquicarbide is present rather than dicarbide. Similar behaviour was reported by Batey *et al.*⁽¹⁶⁾ for mixed carbides in which the second phase is predominantly sesquicarbide. In the present work, the proportion of second phase was small compared with that in other investigations, such as might be expected with a fairly rigid specification of carbon content in nominally stoichiometric fuel. These preliminary results suggest that for such material carburisation may be kept to a low level if dicarbide can be eliminated from the microstructure, but that otherwise it is a serious problem. Further work is needed to establish the precise behaviour of sesquicarbide, particularly if present in a different morphology, as in sintered material where it is not normally found in a continuous form. The influence of oxygen content on

compatibility through its effect on the occurrence of second phases is also worth further study.

The use of tantalum/Nimonic 90 co-extruded tubing as a cladding in the experiments described here may have wider application if high-strength nickel-bearing alloys become of importance for constraint of swelling. This work suggests that the reaction between tantalum and nickel, confined as it largely is to the tantalum component, may be kept within acceptable limits for applications to fuel elements.

As far as the concept of a moderate-centre-temperature, gas-retentive carbide fuel is concerned, the experiments reported here on the behaviour of carbide fuels at 1800°C are, in fact, tests of these materials under conditions which are extremely unlikely to arise. If they did, the conditions would be of very brief duration and not at all comparable to treatments of the order of 100 hr which were used here.

The monocarbides of uranium and uranium-plutonium have proved to be remarkably stable as far as composition changes during irradiation are concerned. Also, Strasser⁽⁶⁾ has shown that (UPu)-oxycarbides containing up to 0.4 w/o oxygen undergo no microstructural changes and that the amounts of MC and M_2C_3 phases present in these materials remain sensibly constant to 10 a/o burnup at centre temperatures of 1100–1500°C. Further, there is no evidence for the formation of metallic phases.

There is, however, a report of a significant deterioration of uranium oxycarbide during irradiation at a temperature of 1000°C, which suggests that under the influence of irradiation, decomposition of the oxycarbide can occur some 800°C lower than it would have done under normal conditions. In fact, Accary and Delmas⁽²⁶⁾ report that this decomposition process eventually led to the formation of metallic uranium by the loss of CO. The subsequent substantial swelling which occurred is believed to have resulted from the precipitation and growth of gas bubbles in the free metal, and could limit the value of oxycarbides as fuels. As mentioned earlier, a further problem would arise from the presence of hot uranium or uranium-plutonium metal in contact with the canning material.

Further work is urgently needed to study the effect of irradiation on the stability of (UPu)-oxycarbides. Until this work has been performed, the results of Strasser and those reported here indicate that, provided the temperature of the fuel is kept at the levels required to give low gas-release rates, the fuels should be stable both with regard to the redistribution of plutonium and to composition changes resulting from the loss of CO.

Acknowledgments

The authors wish to thank all their colleagues in the Metallurgy and Research Reactors Divisions at Harwell

whose efforts have made this paper possible. In particular, they thank Mr. J. D. L. Harrison, Mr. W. G. Roberts, Mr. J. W. Isaacs, Mr. J. M. North, and Mr. B. Riley for fabrication of the fuel, Mr. M. W. George for designing and commissioning the irradiation rig, Mr. J. Sheldon for the careful preparation and assembly of the irradiation cans, and Mr. D. Hodkin for the compatibility experiments.

They also extend their thanks to Mr. D. Gilling who was responsible for the gas-sampling and metallographic examination of the fuel pins, and to the personnel of the Active Handling Laboratories in Metallurgy Division, particularly to Mr. J. A. E. Olden, Mr. G. Comley, Mr. L. Thorn, Mr. E. Fidler, and Mr. E. Gerard.

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Discussion

Mr. Zebroski (GE): I'd like to repeat the question which has been asked in two different forms already, which is, do you have some feeling of as to how you might accommodate, in the case of stoichiometric material, the shift in stoichiometry which you can calculate with burnup? I think this shift unfortunately might go either way, because even though the fission products become in excess of the carbon, the binding energies on the average, or for some of them I should say, may be lower than the heavy metal that they displace. Thus you might have a problem of coping with excess carbon locally as well as with excess metal.

Mr. Bradbury: Unfortunately we haven't got the

necessary thermodynamic data to say which way things will go. I think we need to measure or to plot $RT \log p$ against $1/T$ and compare this for the various solid fission products which are produced with, say, the cladding. This is one thing we must do in the immediate future. One other thing is that, looking at Dr. Strasser's work at United Nuclear, the metallography appears to me to be completely different from that of the oxide; if you take U or U-Pu oxide in excess of 4% burnup, say, we start to see segregation of second phases which we know are solid fission products, in what form is beside the point at the moment. In the case of carbide we see nothing like this, so that the solution of the fission products in carbides relative to those of oxides is completely different. I think we ought to try to find something more about this.

Clad Compatibility with Mixed Oxide Fuel and Sodium

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I. INTRODUCTION

Compatibility is a term frequently used by materials technologists to describe the chemical stability of materials in close contact. As a design criterion, compatibility is of importance since it is one of the factors which determines the selection and useful lifetime of materials in the reactor system.

Although high metal temperatures are found in many areas of a sodium-cooled fast reactor, the highest are associated with the fuel cladding. It is there that diffusion processes and chemical reactions are most likely to be limiting.

The need for system compatibility is clearly demonstrated by considering the possible effects of reactions between the clad material and its environment. As reactions occur between the fuel and cladding, new phases may form at the interface or penetrate locally, e.g., along grain boundaries. These, in turn, can affect the useable strength or ductility of the cladding, with consequent increased frequency or severity of fuel-element failures.

Reactions between the cladding and sodium coolant may result in either general (uniform) or localized attack on the outer clad surface, as well as changes in alloy chemistry. Thus the properties of the cladding can be modified both by changes in the effective loadbearing cross section, and by changes in composition and microstructure.

The maintenance of clad integrity under service conditions for fast reactor fuel-element lifetimes of 2 to 4 yr is the primary objective in the screening, selection, and optimization of a fuel-cladding material. Modifications in the structural characteristics of the cladding, at the fuel-clad or clad-coolant interfaces, can be used to give sensitive indications of the relative merit of different materials prior to mounting large-scale proof-test programs.

It is the purpose of this paper to present several observations of fuel-clad and clad-coolant reactions, to analyze some of the factors which influence them, and to evaluate the effect of such reactions on the expected performance characteristics of cladding materials, especially for applications with oxide fuel.

II. REACTIONS AT CLAD-COOLANT INTERFACE

A. Structural Observation

The hot-zone environment of a dynamic sodium-cooled system promotes specific changes in the surface composition of system materials. A leaching process, in which various alloy constituents are selectively removed from the alloy surface by the liquid sodium and taken into solution, strongly influences the iron-chromium-nickel ratios at the surface of the stainless steel. The reverse process, in which impurity elements in the sodium are deposited on and absorbed into the stainless steel surface, is also commonly observed. The carburization of stainless steel in the hot zone is a well-documented⁽¹⁻⁶⁾ example of this effect. Studies at General Electric have been made to determine the magnitude and nature of the localized phenomena at FCR conditions and their general effect on mechanical properties.⁽⁷⁾

A.1. Elemental Leaching

The process of elemental leaching has been described by Epstein⁽⁸⁾ and Mottley⁽⁹⁾ as a combination of dissolution and chemical reaction mechanisms which remove alloy constituents at various rates. Observations⁽¹⁾ made on type 316 stainless steel samples in 1200°F pumped sodium loops have verified this process. Analyses of sample surfaces by electron microprobe have shown⁽¹⁰⁾ iron-chromium-nickel ratios which are notably different from the original compositions (see Fig. 1). This change in surface composition produces a visible surface layer. If the layer continues to develop and the depletion of alloy constituents persists beyond a specific minimum threshold level, the layer becomes ferritic.

Austenitic type 316 stainless steel has been tested in reactor-grade sodium (calcium controlled to less than 50 ppm) under the loop conditions given in Table 1. The depth of the layer under these conditions is dependent on several variables, especially oxide content and specimen temperature.⁽¹⁾ For the oxide levels of 12 and 50 ppm, average depths of

FIG. 1 Typical Structural and Compositional Changes in Type 316 Stainless Steel Associated with Elemental Leaching in Sodium

MICROPROBE
SCAN PATH

Material = 316 S.S.
Temperature = 1200 °F
Exposure = 5704 hrs.
Oxygen level in Sodium = 12 ppm

TABLE 1. RANGE OF LOOP CONDITIONS TESTED

Sodium Velocity, fps	Max Temp, °F	Δt , °F	Oxide Contents, ppm	Material Combinations		Time, hr
				Hot Leg	Cold Leg	
7 and 21	1200 and 1300	500	12 and 50	316 SS	316 SS	700-33,000
7 and 21	1200 and 1300	500	12 and 50	316 SS	2¼-1 Mo	700-33,000
7 and 21	1200 and 1300	500	12 and 50	316 SS	5Cr-½ Mo-T1	700-33,000

TABLE 2. FERRITIC LAYER FORMATION IN TYPE 316 STAINLESS STEEL AFTER EXPOSURE FOR 2800 HR

Temperature, °F	Average Depth after 2800 Hr, mil	
	12 ppm Na ₂ O	50 ppm Na ₂ O
1000	Trace	Trace
1100	Trace	0.1
1200	0.2	0.3
1300	0.3	0.8

the ferritic layer in Type 316 stainless steel after 2800 hr at various test temperatures are listed in Table 2.

Analysis of the test data indicated that the ferritic layer reached a limiting depth under a specific set of conditions within 1000 hr. The quasi-equilibrium state was maintained over a test period of at least 5000 hr unless the environmental conditions were altered.

An additional factor affecting layer formation is cold work. Figure 2 shows a cold-rolled sample exposed for 400 hr in 1300°F sodium at an oxide concentration of 50 ppm. The unusually rapid layer formation and growth is probably caused by increased diffusion rates associated with severe lattice deformation.⁽¹¹⁾ The number of lattice vacancies would be greatly increased, allowing the elemental alloy constituents to diffuse more rapidly to the liquid metal-solid interface. A similar acceleration of layer growth should also occur if the test coupon is subjected to an applied stress.⁽¹²⁾

The direct influence of the surface layer on material properties is effectively minor. Small amounts of surface ferrite are subject to rusting if air is allowed to enter the system during shut-down periods. Rust particles might flake off during subsequent reactor operation and restrict small flow passages. Control of the reactor cover gas during operational shut downs or liquid sodium coolant filtration could rectify the problem.

Because of the volume change accompanying the alloy depletion and structural transformation to the body-centered cubic lattice from the denser face-centered cubic structure, the ferritic layer contains numerous structural defects. In this regard, the surface

FIG. 2. Ferritic Layer Formation on Cold-worked Coupon after 400 Hours in Sodium at 1330°F.

layer may act as a nucleation site for damage mechanisms. The layer often extends along grain boundaries to penetration depths greater than the average bulk penetration. We may speculate that such penetrations may act as notches to promote thermal fatigue and act as stress concentration sites during strain cycling. Although the austenitic stainless steels are not particularly notch sensitive in their annealed state, they may become increasingly so with irradiation due to severe embrittlement. Differences in thermal expansion characteristics of the ferritic layer (6.5×10^{-6} in./in./°F) to that of the austenitic matrix (10.6×10^{-6} in./in./°F) also suggest a possible mechanism for initiating localized structural damage.

A.2. Elemental Absorption

The absorption of carbon by stainless steel has been the subject of several investigations.^(2-4, 7) At temperatures above 1000°F, austenitic stainless steel has been shown to carburize readily if carbon is present in the sodium stream. Carbon has been found in as-received sodium as graphite particles which have sloughed off from electrodes in the electrochemical process of sodium production. Since carbon is slightly soluble in sodium, the graphite probably dissolves slowly and either forms as yet unidentified compounds or enters into an "active" state which readily engages in the carburization of stainless steel. Other carbon sources stem from organic materials which may accidentally be introduced into the sodium and from cover gas containing carbon monoxide or dioxide. The availability of a major carburization source is inherent if bimetallic systems are considered which combine austenitic and unstabilized ferritic steels. The ferritic alloy 2¼ Cr-1 Mo has been observed^(1, 2, 4, 6) to donate "active" carbon to the sodium coolant until the carbon content in the ferritic alloy is reduced below analytically detectable limits. Carburization of the austenitic material in this case is dependent on the total quantity of unstabilized ferritic alloy in the system. Carbide stabilizers such as niobium and titanium have proven effective in reducing or eliminating this problem, as was demonstrated with 5 Cr-½ Mo-Ti^(1, 2) and the German alloy 10 Cr Mo Nb 910,⁽⁴⁾ a niobium-stabilized 2¼ Cr-1 Mo ferritic.

Prior work at General Electric on general carbon movement has been reported by Plumlee.⁽²⁾ This report will cover more recent work comparing the carburization of types 304, 316L, 316, and 347 stainless steels and Incoloy 800 at 1050°F in an isothermal pumped sodium loop. Tubular cladding samples were exposed for periods up to 700 hr in carbon-contaminated sodium. The source of carbon contamination was a coil of carburized plain carbon steel placed in the sodium stream. Thin (~0.001-in.) foils of type 304 stainless steel were also suspended in the sodium stream so that a more accurate estimate of the surface carbon composition of the thicker tubular samples could be obtained. The tubes were subsequently burst-tested at room temperature, metallographically examined, and analyzed for gross carbon content. The carburization results (see Fig. 3) were similar for types 304, 316L, and 316, whereas type 347 showed pick-up response. Incoloy-800, with its higher nickel content, showed significantly lower rates of carburization than did the other alloys. (The lower carbon levels at 700 hr were associated with exhaustion of the carbon source.) In the room-tem-

perature burst tests (see Fig. 4), Incoloy 800 exhibited the maximum increase in strengths, with the 300 series alloys showing slightly smaller gains.

Analysis of the combined results shows a general relationship between carbon pickup, room-temperature burst strength, and the distribution of the absorbed carbon in the microstructure. The higher-nickel alloy Incoloy 800 showed the least amount of carbon pickup, but the largest increase in hoop strength. The absorbed carbon in Incoloy 800 (see Fig. 5) is heavily concentrated at the surface, the area of maximum fiber stress, thereby magnifying the strengthening effect of the carbon. This concentration of carbon also had a minimal effect on the fracture strain of Incoloy 800, as shown in Table 3. This small reduction in ductility is associated with the small percentage of the cross-sectional area affected by the carbon.

The 300 series of stainless steel alloys absorbed larger quantities of carbon, but these were distributed to greater depths (see Fig. 6) than in Incoloy 800. Therefore, a larger percentage of the cross-sectional area was affected, resulting in lower fracture strains and smaller increases in burst strength.

Other trace impurity elements, such as hydrogen and nitrogen, are commonly present in sodium systems and may cause effects similar to those associated with carbon. Only isolated instances of this type have been reported, and there has not yet been a requirement for a systematic effort to evaluate the importance of such effects.

B. Localized Corrosion

General corrosion or mass-transfer phenomena have been the subject of intensive investigations. In general, these studies have served to point out the significance of several factors:

(1) For temperatures of 1200°F or lower and oxygen levels ≤ 10 ppm, general corrosion* *per se* of austenitic stainless steels does not appear to be of sufficient magnitude to warrant concern for the system integrity for anticipated lifetimes of the order of 20 yr or more.

(2) The overall performance of the test material appears to be dependent on: its location within the system, the test temperature, the temperature distribution, the coolant chemistry, and the system geometry and flow characteristics.

(3) The test parameters which have exerted a strong influence on the alloy behavior in test loops

* Defined as a uniform reaction between a material and the corrosive environment, resulting in a reaction product which does not contribute to the structural integrity of the material.

FIG. 3. Increase in Carbon Level of Selected Austenitic Alloys after Exposure to Contaminated Sodium.

have often been representative of operating extremes or casualty conditions in a large plant.

Recent observations have added another factor which is common in other systems, but which has not been systematically explored in sodium, namely, localized attack. Localized effects can influence the performance of an alloy in the sodium environment in two ways: (a) structural modifications associated with the selective elemental leaching and surface impurity concentrations (discussed previously), and (b) localized corrosion effects occurring under conditions for which general corrosion is small.

Recent examinations of the piping from the sodium mass-transfer test loops have revealed the occurrence of localized intergranular penetrations in addition to general corrosion effects. This attack was first observed in an austenitic stainless steel type 316 which

operated at 1200°F (maximum) for approximately 30,000 hr. The grain-boundary penetrations, to a maximum depth of 0.007 in., were invariably found only in the piping between the last hot-leg test section and the first cooler (neither of which exhibited localized attack). The extent of penetration observed would be of little concern in thick-walled regions of a sodium system (unless also subjected to strain cycling). For thin members, such as fuel cladding, such penetration would define a serious operating limit. There is, accordingly, considerable interest in determining the nature of the attack and the parameters which control or influence it.

B.1. Experimental Observations

A section of loop piping from the attacked region was examined metallographically and revealed ex-

FIG. 4 Hoop Strength of Selected Austenitic Alloys after Exposure to Carbon-contaminated Sodium.

FIG. 5. Carburized Surface of Incoloy 800 after 700 Hours in Carbon-contaminated Sodium at 1050°F.

tensive intergranular penetration (see Fig. 7). Since the attack was characteristically intergranular, the differences in topology and precipitate morphology between the attacked and unattacked grain boundaries were examined. This was accomplished by fracturing a section of the pipe at liquid nitrogen temperature to produce a predominantly brittle (intergranular) rupture in the unattacked bulk of the pipe section.

A fractographic analysis, utilizing both two-step

TABLE 3. FRACTURE STRAIN OF TEST ALLOYS AFTER 300 HR EXPOSURE

Alloy	Strain	
	Pre-exposure, %	Post-exposure, %
Incoloy 800	17	14
Type 304	40	17
Type 316	38	19
Type 347	25	9

FIG. 6. Carburized Surface of Type 316 Stainless Steel after 700 Hours in Carbon-contaminated Sodium at 1050°F.

FIG. 7. Intergranular Penetration in Type 316 Stainless Steel.

carbon and carbon-extraction replicas, was carried out and yielded the following observations

(a) All replicas of the fracture surface away from the attacked area (see Figures 8 and 9) were typical of intergranular fracture and showed severe sensitization in the form of continuous sheets of minute, angular particles which were identified by electron diffraction as $M_{23}C_6$

(b) Examination of the replicas of the attacked intergranular surfaces (see Fig 10) revealed "steps" or "serrations" which were not present on the original unexposed surface and are typical of a heavily etched

surface. This suggests that the corrosive environment preferentially attacked and exposed certain crystallographic facets of the grains.

(c) The distribution of carbides on the attacked surfaces was substantially different than on the unattacked surfaces. The presence of large angular carbides indicated that the carbide particles themselves were not chemically attacked. The smaller carbide particles were probably dislodged from the surface during the fracture process after the carbide-matrix bonding had been weakened and disrupted by the corrosive attack.

FIG 8 Transmission Electron Micrograph of Plastic carbon Replica of Intergranular Fracture Surface Away from Attacked Area, Showing Complete Grain boundary Coverage by Precipitation

FIG 9 Transmission Electron Micrograph of Carbon extraction Replica of Intergranular Fracture Surface Away from Attacked Area, Showing Complete Sensitization

(d) No positive evidence of any corrosion reaction product in the grain boundary could be established. Particles of different sizes and shapes were observed (see Figures 11 and 12) and examined by electron diffraction, but no positive identification could be made.

B.2 Pertinent Considerations

Several facets of the observed penetration are similar in appearance and character to data reported from basic corrosion studies⁽¹³⁻¹⁴⁾ and applied problem areas such as failure analysis⁽¹⁵⁾. In a theoretical work⁽¹⁶⁾ on the structural mechanisms associated with

intergranular corrosion, two criteria were proposed for this mode of attack:

(1) the existence of a continuous structural path along which the chemical reactivity is high relative to the surrounding matrix,

(2) the existence of a normal stress across the path of high chemical reactivity.

B.2.1 Structural Considerations The most obvious structural consideration is that of chromium depletion in the alloy lattice adjacent to the carbide precipitates, providing a path of reduced corrosion resistance. Experimental measurements of the attack rate suggest a typical value of 1.7 mils/yr ,⁽⁵⁾ determined over

FIG. 10 Transmission Electron Micrograph of Carbon extraction Replica of Attacked Grain boundary Surface.

FIG. 11 Transmission Electron Micrograph of Aggregate of Particles Removed from the Attacked Grain boundary Surfaces and Thought to be Corrosion Products

FIG. 12. Transmission Electron Micrograph of Polycrystalline Particles Removed from the Attacked Grain-boundary Surfaces and Thought to be Corrosion Products.

a period of 3 yr. Although an initial depletion of chromium may exist at the metal-carbide interface, the diffusion of chromium over long periods of time will certainly tend to reduce any concentration gradients [assuming a finite supply of insoluble carbon available for formation of $(\text{Fe}, \text{Cr})_{23}\text{C}_6$].

Electron microprobe studies of various alloy systems^(17, 18) have indicated that, within the resolution capabilities of this technique, the region adjacent to an intergranular solute-rich precipitate was not solute depleted, especially at time and temperature conditions under which solute diffusion was significant ($T > \frac{1}{2} T_m$).

Several studies have been reported which discuss the morphology of carbides in austenitic steels and its influence on corrosion resistance. Stickler and Vincikier⁽¹⁹⁾ reported a direct correspondence between corrosion behavior and the morphology of the grain-boundary carbide. Their data indicated that poor corrosion resistance was associated with sheets of connected small particles, and good corrosion resistance with isolated larger particles. The morphology and distribution of the precipitate were strongly dependent upon time and temperature.

TABLE 4. RELATIVE COMPOSITIONAL RANGES OF TYPE 316 MATRIX AND COMPLEX CARBIDE M_{23}C_6

Element	Matrix, w/o	M_{23}C_6 , w/o
Cr	16-18	60-70
Fe	68-70	15-20
Ni	10-12	4-6
Mo	2-3	10-15

Corroborating data were presented by Bendure *et al.*⁽²⁰⁾ in a study of the carbide-precipitation characteristics of austenitic stainless steels. Their data also showed that susceptibility to intergranular attack was much more pronounced after sensitization at 1250°F (sheets of interconnected small particles) than after similar exposure at 1500°F (large, widely spaced precipitates).

Further work by Stickler and Vincikier⁽²¹⁾ indicated that corrosion attack of sensitized austenitic stainless steel in the Strauss ($\text{CuSO}_4\text{-H}_2\text{SO}_4$) solution proceeded by a dissolution of the matrix along a narrow region adjacent to the intergranular carbide particles, which remained essentially unattacked. These experimental data suggested that an electrochemical process was operative between the noble carbide and the less-noble matrix. However, exposure of the same steel in the Huey solution (65% HNO_3) resulted in intergranular corrosion which advanced by attacking and dissolving the grain-boundary carbides. Therefore, it is evident that the corrosive environment is as important a factor in determining the corrosion mechanism as the structural characteristics of the steel.

The principal cation in the complex carbide M_{23}C_6 is Cr, but small amounts of Fe, Ni, and Mo can be taken into solution. When compared with the nominal composition of the 316 alloy matrix (see Table 4⁽²²⁾), the complex carbide is rich in alloying constituents which would make it more noble than the adjacent matrix.

The conclusions reached in these experimental studies indicated that intergranular corrosion of a sensi-

tized austenitic stainless steel was associated with an electrochemical reaction between the noble carbide and less-noble matrix, and was strongly dependent upon the morphology and distribution of the intergranular precipitates and the chemical nature of the corrosive environment.

B.2.2 Considerations of Applied Stress. Since the observed attack appears to be a selective and localized phenomenon, many variables within the material structure may assume importance, such as orientation effects, coherent precipitates, stacking faults, and other dislocation barriers. Therefore, relatively low macrostresses may induce locally high microstresses to initiate and propagate cracking.

As postulated by Robertson and Tetelman,⁽¹⁶⁾ a stress normal to the chemically reactive path is required to satisfy fracture criteria. Stress also provides strain energy at the metal-carbide interface, increasing the chemical reactivity there. A certain degree of strain energy is also present due to the lattice mismatch at the metal-carbide interface. The alloy matrix has a face-centered cubic structure with a lattice parameter of 3.584 Å, while the carbide has a modified face-centered cubic structure with a lattice parameter of 10.64 Å, approximately 3 times that of the matrix.

The opportunity for substantial stresses to develop in the loop piping was present during loop startup. During the initial stages of the preheat cycle, a circumferential temperature gradient of 260°F (maximum) was impressed upon the 1/2-in. Schedule 40 loop piping. A thermal-stress analysis revealed that the average stress on the ID surface of the piping was of the order of 35,000 psi. As the preheat time increased, the temperature of the piping approached an equilibrium level of 700°F, reducing the thermal stresses to an insignificant level. During loop operation at 1200°F, the temperature differential across the piping wall was approximately 10–15°F, producing minor thermal stresses.

B.2.3 Considerations of Corrosive Environment. A study of the procedures of loop operation indicated that during loop shutdown, the internal piping surfaces, coated with a residual sodium film, were subjected to limited atmospheric contamination. Under these conditions the sodium film quickly reacts to form Na₂O and NaOH. The combination of the highly corrosive film, the high thermal stresses, and increasing temperature during the preheat cycle presented an environmental condition favorable for corrosive attack.

Attack of the type observed in the loops had been noticed previously⁽²³⁾ under two slightly different sets of circumstances:

(a) When a sodium system has sprung a leak, and sodium has dripped out, solidified on the outside of the steel, and subsequently undergone atmospheric oxidation, inspection of the leak region frequently discloses intergranular attack.

(b) When corrosion tabs of steel samples have been withdrawn from a sodium system and stored in a supposedly inert atmosphere without washing or dissolving away the surface film of sodium, air in-leakage in time produces a white layer of Na₂O on the sample, beneath which intergranular penetration is observed upon subsequent examination.

Several experimenters⁽¹³⁻¹⁵⁾ have studied the corrosion of stainless steels in caustic solutions at temperatures up to 400°C. Their data show that caustic solutions will produce intergranular corrosion in both unsensitized and stabilized austenitic stainless steels. The magnitude of the attack in caustic solutions was dependent upon and increased with increasing solution concentration, applied stress, and test temperature.

Snowden⁽¹⁴⁾ also pointed out several other interesting aspects of caustic corrosion which have direct application to the analysis of the present problem. His data showed a difference in the appearance of the attack as a function of solution strength. Oversaturated solutions tended to produce cracks more slowly, and with a broader and stubbier appearance, than the more dilute solutions, which usually produced a fine-branched crack. Further tests with NaOH at high concentrations near its melting point revealed mixed inter- and transgranular cracking.

Snowden also evaluated the effect of low (0.03%) and higher (0.07–0.11%) carbon levels in stainless steels under similar corrosive conditions. The low-carbon alloy exhibited transgranular cracking while the higher-carbon alloys revealed intergranular cracking. This difference in behavior was attributed to grain-boundary segregation of the carbon atoms. This observation is in direct agreement with the later results of several workers⁽¹⁹⁻²¹⁾ who showed the direct correlation between corrosion behavior and the morphology of the grain-boundary carbide.

B.3 Discussion

Review of the published data indicates that the observed loop penetrations are very similar in character to those observed in basic corrosion studies and applied problems such as failure analysis. These data show that intergranular attack can occur in unstressed metal beneath a layer of Na₂O at room temperature and also in dilute to medium-strength solutions of NaOH at temperatures of 250–300°C in stressed coupons. Comparison of these and comple-

mentary data with the observed attack suggests: (1) that the intergranular penetration of the loop piping was associated with the presence of Na_2O and NaOH , (2) progressed through an electrochemical-mechanical mechanism, and (3) occurred during the preheat cycle of loop startup. The first two suggestions are based upon the close similarity of published data and metallurgical observations of the loop piping. The same type of cracking has been reliably reproduced under controlled environmental conditions similar to those which can be reasonably postulated as present on the internal surfaces of the piping. The third suggestion is based upon an examination of loop-operating procedures. Only during loop startup can substantial thermal stresses and a concentrated caustic environment be present. When the flowing sodium is introduced into the system, the pipe walls become essentially isothermal, and any Na_2O - NaOH layer on the wall surface is rapidly washed away or taken into solution. Therefore, progression of the attack during high-temperature operation is unlikely. It would then appear that the penetration occurred in steps with each shutdown-startup period representing an incremental advance.

III. FUEL-CLAD INTERFACE REACTIONS

Modifications in the structural characteristics of the cladding may occur by a number of possible fuel-clad, clad-coolant, or irradiation-induced reactions, most of which tend to reduce the effective high-temperature strength of the cladding, either by chemical thinning or embrittlement.

The work reported here is a compilation of recent observations from several irradiation tests in which reactions at the fuel-clad interface were observed.

A. Examination of a High-burnup Fuel Pin

A.1. General

A test capsule containing $(\text{Pu}, \text{U})\text{O}_2$ fuel was fabricated to evaluate the in-pile swelling behavior of mixed oxide fuels under typical fast reactor operating

TABLE 5. EXPERIMENTAL PARAMETERS OF HIGH BURNUP FUEL PIN

Pu (Pu + U) Ratio	20/3
Stoichiometry (O/M)	2.04
Pellet Density (Avg)	96% Theoretical
Smeared Density (Avg Cold)	92% theoretical
Cladding	347 SS
Clad Thickness	15 mils
Diametral Gap (Avg)	1.9 mils
Environment	NaK
Neutron Flux	9×10^{14} n/v
Burnup (MWd/T)	70,000
Clad OD Temperature ($^{\circ}\text{F}$)	1,000

FIG. 13 Failure in Type 347 Stainless Steel Cladding.

conditions. The fabrication and irradiation parameters are contained in Table 5.

Postirradiation examination⁽²⁴⁾ of this pin revealed a longitudinal clad rupture immediately adjacent to a spacing wire attached to the outer surface of the cladding. Clad failure was attributed to a reduction in clad yield strength due to local overheating in this region. Overheating is believed to have been initiated by incomplete sodium bonding of the test capsule, or growth of a gas bubble in the NaK annulus. Metallographic examination of the failed region revealed considerable necking at the OD surface (see Fig. 13), suggestive of ductile fracture. The remainder of the cladding failed intergranularly.

Metallography of the ID surface of the cladding near the failure showed a region consisting of heavy grain-boundary precipitation extending to a depth of approximately 3 mils. Although this surface was highly irregular due to the breakaway of precipitate-enveloped surface grains (see Fig. 14), no gross chemical attack was observed at any point along the fuel-clad interface. The OD surface of the cladding showed no abnormal exposure effects.

A.2 Analysis of Precipitates

Precipitates were extracted from four regions along the clad cross section: (1) the ID region of high-

density grain-boundary precipitation, (2) the region adjacent to and including the OD surface, (3) the central portion, and (4) the complete radial clad cross section. Electron-microscopic examination of the extraction replicas showed that the OD surface and central region of the cladding contained a random distribution of massive globular precipitates (see Fig. 15) characteristic of those found in a stabilized stainless steel. The ID surface, however, exhibited a continuous grain-boundary array of thin angular precipitates (see Fig. 16) as well as the globular forms.

Analysis of the bulk-extracted precipitates by X-ray (Debye-Scherrer) diffraction revealed only one discrete phase: a face-centered cubic structure with a lattice parameter of 4.434 ± 0.005 Å. This value agrees fairly well with that for NbC, but suggests nonstoichiometry. Pearson⁽²⁵⁾ reports that a lattice parameter of this value would match a composition of approximately NbC_{0.72}. The possibility exists, however, that the phase contains nitrogen in solution, corresponding to a composition Nb(C,N) which contains approximately 30 m/o nitrogen. The fact that the primary constituent of the phase is niobium was substantiated by X-ray fluorescence analysis of the extracted precipitates, which identi-

FIG. 14. Grain-boundary Precipitation, ID Surface of Type 347 Stainless Steel Cladding.

FIG. 15. Transmission Electron Micrographs of Carbon-extraction Replica Showing Carbides Characteristic of Midradial Region of Cladding.

FIG 16 Transmission Electron Micrographs of Carbon extraction Replica Showing Carbides Characteristic of ID Region of Cladding

FIG 17 Fuel clad Interface Reaction after a Burnup of 77,400 MWDT

fied niobium as the only major element present. A weak trace of iron was also detected (X-ray fluorescence does not detect carbon or nitrogen).

Since the X-ray diffraction of the bulk extraction failed to show the presence of a second precipitate, an electron-diffraction analysis of the two precipitates observed in the carbon-extraction replica from the clad ID surface was performed. Although the large precipitates were too thick for electron transmission, the thin angular particles yielded good diffraction patterns, the spot spacings of which corre-

spond to the compound $M_{23}C_6$. Since this compound could not be resolved by X-ray diffraction of the precipitates removed by bulk extraction, it is reasonable to assume that a much larger volume of niobium-base particles than $M_{23}C_6$ particles was present in the fuel cladding. A subsequent X-ray diffraction analysis of an extraction replica of the ID surface revealed both phases.

It was, therefore, concluded that the majority of the $M_{23}C_6$ precipitates occur in the narrow zone at the inside surface of the cladding and constitutes

FIG. 18 Reaction at Fuel clad Interface of a (Pu-U) O_2 Pin Irradiated to a Burnup of 28,000 MWd/T.

25 μ

FIG. 19 As polished Appearance of Fuel clad Reaction Shown in Figure 18.

FIG. 20 Grain boundary Precipitation in 347 Stainless Steel Cladding Adjacent to Fuel-clad Interface

the heavy grain-boundary precipitate phase observed in optical metallography.

Precipitation effects of this type are essentially those which would be expected due to the thermal exposure alone, without regard to neutron exposure or compatibility considerations. However, the overall characteristics of the precipitation, including the well-defined boundary, suggest that the mixed oxide fuel is exerting an influence on the diffusion rates of alloy constituents and, therefore, the rate and characteristics of structural transformations.

The observation of a preferential buildup of carbides at or near the ID surface of the cladding suggests that a carbon gradient exists in the material. However, a microhardness survey, prompted by the fact that an increase in carbon content in most steels is reflected in increased hardness, failed to reveal any substantial gradient across the cladding.

B. General Observations of Fuel-clad Interface Reactions

The clad-fuel interfaces of a series of fuel pins irradiated to burnups up to 99,000 MWD/T were examined by optical microscopy. The results indicated that, for exposures as high as 99,000 MWD/T and maximum ID clad surface temperatures of 1260°F, little or no general reactions occurred between fuel and cladding in either pellet-filled, 347 stainless steel pins or swaged-over 304 stainless steel pins. In several instances, however, localized reactions were observed in a number of the 347 stainless steel pins containing pelleted fuel, although none of the pins failed.

A section of a pin (V-2-P), which sustained a burnup of 77,400 MWD/T, shows a thin surface layer (6 to 7 μ) on the clad ID in a region of intimate fuel-cladding contact (see Fig. 17). (The maximum clad ID temperature of this pin was 1180°F.)

A duplex layer of possible fuel-clad reaction product is evident on the transverse cross section of a (Pu-U)O₂ pin (VIII-2-P) (see Fig. 18), which had been irradiated to a burnup of 28,000 MWD/T and attained a maximum clad ID temperature of 1220°F. The layer had a thickness of approximately 14 μ and was contiguous with the cladding, but had the gray as-polished appearance of nonporous fuel (see Fig. 19). A thin, 4- μ uniform layer with well-defined interfaces, sandwiched between the gray phase and the bulk of the cladding, was revealed by etching. The grain structure of this layer could not be fully resolved by optical metallography although the grain-boundary network of the adjacent undisturbed cladding appeared to be continuous across the layer.

Grain-boundary precipitation along the ID surface of the cladding, similar in appearance to that

noted on the previously described high-burnup capsule, was observed in a number of pins. However, such precipitation was generally restricted to regions where the fuel-clad gap was nonexistent or sufficiently narrow to assure that intimate contact existed at the test conditions (see Fig. 20).

The mechanisms responsible for the localized reactions observed in this investigation have not been firmly established. A number of possible reaction mechanisms⁽²⁶⁾ may be operative at the fuel-clad interface, the most likely of which are (1) transport of such impurities in the fuel as water, carbon compounds, or halogens to the reaction interface, (2) vapor transport of oxide, iodide, or other volatile fission products or their daughters to the interface, (3) selective oxidation of some clad alloy constituents, and (4) fission product recoil from fuel surface entering clad surface with disruption of the protective film.

IV. CONCLUSIONS

The following conclusions are offered:

(1) Localized corrosion which exhibited characteristics similar to caustic corrosion has been observed in a pumped sodium system. The attack was associated with intermittent atmospheric contamination of sodium-coated interior surfaces and with large thermal stresses present during startup.

(2) Compatibility studies of mixed oxide fuel and austenitic stainless steels showed general interactions of less than 1 mil, and localized grain-boundary disturbances up to 3 mils. These reactions were not observed in all fuel pins, nor were they observed uniformly along the fuel-clad interface in any one pin.

(3) Application of electron microscopy and microprobe techniques resulted in specific improvements in the quality and accuracy of diagnoses of such reaction and structural effects, and afford promise of a firmer basis for screening and performance analysis of candidate cladding alloys.

ACKNOWLEDGMENTS

The authors are grateful to the Atomic Energy Commission for permission to publish this work, to E. L. Zebroski for his many helpful suggestions, and to U. E. Wolff for the electron microscopy.

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Discussion

Mr. Kummerer (Karlsruhe): My question is related directly to the fuel-clad interaction; I want to know whether you could find any change of stoichiometry of the fuel directly on the surface.

Mr. Comprelli: We did not look directly for that in our observations.

Mr. Mustelier (CEA, France): What do you think of the compatibility between stainless steel and mixed oxide in the presence of a sodium bond, for example in the case of a rupture of cladding?

Mr. Comprelli: We did not carry out a direct experiment on this particular item. I assume that you are referring to a fuel element to which has been intentionally added sodium and sodium does not enter due to rupture. Is that correct?

Mr. Mustelier: I think the result is the same. I just want to know if you happen to see transfer of fission products or an attack when sodium is placed between UO_2 - PuO_2 and stainless steel.

Mr. Comprelli: If I understand you correctly, you refer to fission products leaving the fuel, entering sodium, and then acting as a corrosion medium with respect to the cladding. We have not analyzed this, but it will be in our present program. We have taken a cursory look at the effect of sodium-fuel reaction products on the cladding. Our present results, which are preliminary, indicate that there is a minor reaction but nothing of catastrophic measure.

Mr. Okrent (ANL): Would you expect any severe compatibility problems if you were running oxides at higher ratings and higher burnups?

Mr. Comprelli: The observations reported today are pretty representative of the general trend which we have observed in our fuel pins, and we have irradiated these pins up to 99,000 MWD/T. The magnitude of the effects, as we have seen them, do not indicate that this problem is insurmountable. We have had interface temperatures of approximately 1250°F, and the magnitudes of the reactions under those conditions were not substantially larger than those observed under the slightly lower temperatures. However, I do expect a slight increase with increasing temperature.

Mr. Okrent: How much margin do you think you have between satisfactory operation and rapid deterioration? In other words, does one have to really

have a very good handle on the compatibility question?

Mr. Comprelli: I don't believe I'm prepared to say we have a real good handle on it, based on the observations we've made to date. We've made essentially cursory examinations of the pins we have available.

Mr. Stahl (UNC): In one of your micrographs there was no gap between your fuel and clad. Did you start out with a reasonable cold clad gap? Did you have any failures because of swelling?

Mr. Comprelli: We do not have any failures that can be attributed directly to swelling. In the high-

burnup pin, to which I believe you are referring, the cold diametral gap was approximately 2 mils.

Mr. Kummerer: In one of your slides, sodium had an oxygen content of 12 ppm. Did you determine the oxygen content before or after the irradiation experiments?

Mr. Comprelli: The oxygen level to which you refer was associated with the microprobe trace across the 316 sample. That was not an irradiation experiment; it was a loop experiment. The oxygen level was obtained through determination of the plugging temperature.

Effect of Stoichiometry on the Properties of Mixed Oxide U-Pu Fuel

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I. Introduction

Conventional oxide fuels for present economic power reactors typically employ essentially stoichiometric fuel, UO_{2+x} , where x is positive and lies in a range up to 0.02. This has proven satisfactory for relatively low-burnup applications (10,000 to 25,000 MWD/T) in which stoichiometry changes with burnup are small.

Fast reactor fuels of the type $(\text{U}, \text{Pu})\text{O}_x$ are required to attain average burnup of 75,000 to 100,000 MWD/T to be of economic interest, and local peak burnups up to 130,000 MWD/T. The choice of preferred stoichiometry for fast spectrum power reactor application may prove to be appreciably different from the practice for present power reactors for several reasons:

(1) A wider range of stoichiometry and properties is conveniently attainable; for $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{2+x}$, the value of x can be readily adjusted to lie in the range $x = \pm 0.03$.

(2) The change in composition and properties with burnup is more significant (final composition is up to ~ 25 a/o fission products relative to uranium plus plutonium).

(3) The average operating temperature of the fuel is significantly higher (because of higher coolant temperature, higher heat fluxes, and higher specific power).

This paper is intended to cover the status of some fuel preparation and properties data, as well as observations of fuel performance, which lead to a tentative preference for a stoichiometry of 1.97 to 1.98 for mixed U-Pu oxide fuel.

II. Preparation and Homogeneity of Mixed Oxide Solid-solution Fuel

The preparation of a solid solution of PuO_2 and UO_2 can be accomplished by either coprecipitation or by efficient physical mixing of powders of PuO_2 and UO_2 followed by sintering or by pressing and sintering. Coprecipitation is at present the reference method since it provides a high degree of assurance of formation of a homogeneous, single-phase solid solution. Coprecipitation is accomplished by the simultaneous precipitation of ammonium diuranate and plutonium

hydroxide with ammonium hydroxide from nitrate solution. Subsequent steps include filtration, drying, and reduction. The flow diagram for this operation is shown in Fig. 1.

Physically mixed powders can also be used to produce a near-homogeneous solid solution (after sintering) by careful control of starting materials, particle size, and mixing procedures to insure against agglomeration of the powders during mixing. Departure from ideal process conditions results in localized high concentrations of PuO_2 and/or UO_2 which are difficult to guard against by conventional inspection methods. Electron-beam microprobe analysis affords the most sensitive measure of local homogeneity available at present.

Microprobe analyses (conducted with the cooperation of NUMEC and the Mound Laboratory) were performed with physically mixed and sintered powders and coprecipitated mixed oxides to establish the degree of homogeneity attainable.⁽¹⁾ These results are summarized in Table 1 and Fig. 2. The slight departure from unity in maximum to minimum ratio noted in Table 1 for coprecipitated fuel may be within instrumental error. The physically mixed powders generally have readily detectable inhomogeneities.

The feasibility of attaining nearly equivalent homogeneity by the mixed-powder route is shown by the sample represented in line 2 of Table 1. With sufficient experience in process control, and the development of convenient inspection methods to give statistical assurance of homogeneity, this may become another acceptable route for fast reactor fuel fabrication.

The samples in the lower three lines of Table 1 represent different degrees of inhomogeneity with mixed powders. For the samples which are not sintered, significant deterioration of the prompt Doppler would have to be accepted. This increases the calculated energy release for high-ramp-rate reactivity-insertion accidents, and consequently increases the difficulty and cost of reactor containment. Sintering and homogenization which may occur *in situ* in the hotter portions of operating fuel does not significantly alleviate the safety problem of small-scale inhomogeneity. The $1/T$ de-

FIG. 1. Coprecipitation Flow Diagram.

pendence of the Doppler coefficient implies that a large part of the useable Doppler Δk in a reactor transient must come from the colder regions of the fuel, which may not be rapidly homogenized by thermal diffusion. A much smaller loss of Doppler Δk is calculated for large-scale inhomogeneities (limited radial concentration gradients) as treated in another paper at this Symposium.⁽²⁾

III. Control and Measurement of Stoichiometry

Mixed oxide of uranium and plutonium, $(\text{U}, \text{Pu})\text{O}_2$, can be conveniently fabricated with direct control of stoichiometry over a range of oxygen-to-metal ratios from 1.97 to 2.00. These are obtainable during sintering of $(\text{Pu}, \text{U})\text{O}_2$ fuel by control of the moisture content of the sintering-furnace atmosphere. By addition of water vapor to the helium-6% hydrogen furnace gas to a level of about 1% at room temperature, stoichiometric mixed oxide is obtained. If helium-hydrogen gas is passed through magnesium perchlorate drying columns before entering the sintering furnace, fuel with oxygen-to-metal ratio of 1.97 is obtained. Analyses have shown that constant O/M ratio is obtained within a given

TABLE 1. SMALL-SCALE INHOMOGENEITY OF MIXED OXIDE FUEL

Type of Fuel	Agglomeration Regions, μ	Mean PuO_2 Conc, w/o	Max/Min Plutonium Content	Max/Avg Plutonium Content
Coprecipitated, Dry Sintering	$<1^*$	15.0	1.08	1.04
Physically Mixed, Dry Sintered	$<1^*$	15.0	1.26	1.08
Physically Mixed, Wet Sintered	$\sim 5^*$	15.0	1.92	1.30
Ball Milled for 4 Hr and Impacted	20-40**	20.0	2.69	1.74
Ball Milled for 64 Hr and Impacted	20-30**	20.0	1.93	1.44

* Analysis with $1\text{-}\mu$ -dia spot.

** Analysis with $20\text{-}\mu$ -dia spot.⁽³⁾

pellet and throughout a batch of pellets sintered at one time, provided that an even temperature is maintained across the charge.

A still wider range of stoichiometry is available by auxiliary process steps. Hyperstoichiometric mixed

FIG. 2. Plutonium Distribution in $\text{UO}_2\text{-15 w/o PuO}_2$ Fuels.

oxide is prepared by the controlled oxidation in air at 400°C of initially stoichiometric specimens. The batch of pellets or powder is heated until a prescribed weight increase has been obtained, and then soaked in pure argon at 700°C to obtain a uniform distribution of the excess oxygen. Pellets with tailored O/M levels of 2.010 to 2.060 have been made for property measurements and irradiation tests.

The analysis for O/M ratio uses the thermogravimetric procedure based upon oxidation followed by reduction in hydrogen at 700°C .⁽⁴⁾ A macro technique with precision of ± 0.001 is used for whole pellets or powder samples of 1 to 10 gm. A microthermogravimetric apparatus has been developed for samples as small as 2 mg. Local shielding makes possible the handling of highly irradiated fuel samples in the latter apparatus. Attempts to measure O/M of irradiated samples to date have been hampered by the extreme sensitivity of the measurement to traces of organic materials used in mounting and retrieving irradiated specimens.

The redistribution of oxygen in fuel operating with typical large thermal gradients can be roughly predicted by extrapolation to higher temperatures of the U-Pu-O thermodynamic data of Markin and Bones.*

For an effective oxygen partial pressure of 10^{-9} atm, fuel of the initial composition (0.2 Pu-0.8 U) $\text{O}_{2.00}$ is estimated to reach an O/M of about 1.91 at the center ($\sim 2500^\circ\text{C}$), an O/M of 2.00 at 70% radius ($\sim 1900^\circ\text{C}$), and an O/M of ~ 2.005 at the fuel surface ($\sim 1300^\circ\text{C}$).

IV. Nonstoichiometry in Mixed Oxides of Plutonium and Uranium

Nonstoichiometric mixed oxides are characterized as compounds which exist as a single phase over a range of chemical compositions. More specifically, they can be defined as a solid solution of binary oxides, $(\text{M}_a, \text{M}_{1-a})\text{O}_x$, where a and x are variable. The term "nonstoichiometric" is sometimes used loosely, since in many cases departures from stoichiometry are accompanied by the formation of a new phase or phases which are not readily detected by chemical analysis and which may be interpreted as a possible defect structure. An example of this is the UO_{2-x} system. At room temperature a stoichiometry of 1.97 actually refers to a two-phase mixture of very closely stoichiometric $\text{UO}_{2.00}$ and a second phase of uranium metal. Under equilibrium conditions the UO_{2-x} structure exists only at high temperatures. At higher temperatures, the UO_2 system does exhibit a true departure from stoichiometry, and at 1400°C the defect structure UO_{2+x} (where x may be positive or negative) exists between 1.98 and

* Unpublished work, Harwell, 1964.

approximately 2.20. A more detailed discussion of some of the effects of this transformation on physical properties is given in Sec. V. Departures from stoichiometry exist at oxygen sites which are vacant for the $2 - x$ condition. In the $2 + x$ condition additional oxygen atoms are located in interstitial sites which readily accommodate the extra oxygen in a stable structure.

In the mixed oxide system of PuO_2UO_2 there exists a unique combination of an oxide (PuO_2) whose highest oxide state is (IV) and of an (UO_2) oxide which has a maximum oxide state of (VI). In the other direction, PuO_2 exhibits a true nonstoichiometric defect structure down to an oxygen-to-metal ratio of approximately 1.98 and below that is a two-phase structure of $\alpha\text{-Pu}_2\text{O}_3$ and $\text{PuO}_{1.98}$.⁽⁶⁾ Excess oxygen added to UO_2 exists as either a defect structure or as a two-phase structure of UO_2 and U_4O_9 , depending on the temperature and rate of cooling.

Both UO_2 and PuO_2 exhibit fluorite-type face-centered cubic crystal structures which have no known allotropic modifications up to their melting point and have a U/Pu ionic-radius ratio of 1.04. These observations along with X-ray diffraction data on the mixed oxides have confirmed that a complete series of solid solutions exist across the phase diagram, which is

single phase for all stoichiometric compositions. Figure 3 shows room-temperature X-ray diffraction results which indicate an almost linear change in cell size of the solid solution of PuO_2 and UO_2 with composition.⁽⁶⁾ Per Vegard's law this confirms that complete solid solubility exists in this system. This has also been verified by several other investigators.^(7,8)

A series of solid solutions exists between various stoichiometries in specific compositions of $(\text{Pu}_a\text{U}_{1-a})\text{O}_2$. Compositions of $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{2+x}$ have been studied between stoichiometries of 1.975 and 2.01, and have been found to exhibit a single-phase structure with a linear decreasing cell size with increasing oxygen content.⁽⁹⁾ This is shown in Figure 4 and also in Table 2. A similar correlation has also been established by Dean for 30 and 50 w/o PuO_2 mixed oxides.⁽¹⁰⁾ Trace quantities of $\alpha\text{-Pu}_2\text{O}_3$ have been found in various compositions and stoichiometries of $\text{PuO}_2\text{-UO}_2$ solid solutions including hyperstoichiometric compositions. This effect is not well understood, but may reflect slowness in attainment of equilibrium distribution. The contraction of the unit-cell size with increasing oxygen content is similar to that noted in the single-phase UO_{2+x} structure in which the cell size varies almost linearly with increasing oxygen from 5.472 Å for $x = 2.00$ to 5.450 Å for $x =$

FIG. 3. Measured Change in Lattice Parameter with Composition in the $\text{UO}_2\text{-PuO}_2$ System.

FIG. 4. Relationship of Unit Cell Size with Stoichiometry in $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_2$ Specimens.

TABLE 2. CHANGE IN LATTICE PARAMETERS AND SECOND PHASES IN MIXED OXIDE WITH VARYING OXYGEN CONTENT

O/M, Chemical	PuO_2 , Chemical, w/o	$\text{Pu}(\text{Pu} + \text{U})$, Xray	Lattice Parameter of Solid-solution Phase,	Trace Phases	Relative Amount of Trace Phases
2.010	0.200	0.2037	5.455 ± 0.001	Alpha Pu_2O_3^*	Weak minor
2.000	0.200	0.2036	5.4567 ± 0.0002	Alpha Pu_2O_3^*	Weak minor
1.989	0.20 (nominal)	0.1873	5.4577 ± 0.0002	Alpha Pu_2O_3^*	Very weak
1.976	0.200	0.2016	5.460 ± 0.007	Alpha Pu_2O_3^*	Weak

* Pu_2O_3 - Pu_4O_7 —Identity of these phases is based on a measured lattice parameter of the second phase at 11.03 to 11.01 Å. The structure of alpha- Pu_2O_3 was indexed by Zachariasen and was shown to be a defect structure that can contain additional oxygen atoms in the range Pu_2O_3 to Pu_4O_7 .^(11, 12) Ellinger in a separate study found that the lattice parameter of alpha- Pu_2O_3 could vary between 11.00 ± 0.02 to 11.04 ± 0.02 Å for oxygen-rich and oxygen-deficient structures, respectively.⁽¹³⁾

2.18. This variation, however, is a function of the cooling rate since the equilibrium structure for UO_{2+x} is $\text{U}_4\text{O}_9 + \text{UO}_2$ (two phase). It should be noted that whenever U_4O_9 is observed the corresponding UO_2 cell size is that for $\text{UO}_{2.00}$.⁽¹⁴⁾ Such a correlation in the hyperstoichiometric mixed oxide system has not been found at this time.

Metallographic examination of mixed oxides as a function of stoichiometry have shown no apparent correlation of second phase with stoichiometry. Figure 5 shows the microstructure of UO_2 at 1.98 stoichiometry with its characteristic UO_2 plus free uranium. Precipitates of free uranium have been noted in specimens of 1.99 and lower stoichiometry. Metallographic examina-

tions of mixed oxides in the neighborhood of 20 w/o PuO_2 have indicated that the mixed oxide exhibits less distinctive etching behavior than UO_2 .

The structure of hyperstoichiometric mixed oxide has been found commonly to exhibit microcracks dispersed throughout the specimen. These cracks are apparently associated with the fabrication technique at the stage of controlled oxidation of either 1.98 or 2.00 mixed oxide to 2.05. The cracks presumably occur due to the density decrease on oxidation. Figure 6 shows an electron micrograph of mixed oxide fuel showing the striated microcontour substructure normally associated with coprecipitated mixed oxide.⁽¹⁵⁾ This substructure is apparently formed during the coprecipita-

465X

FIG. 5. Microstructure of Vacuum Reduced UO_{2-x} .

tion operation and probably represents a periodic series of either coherent changes in composition or orientation within each grain.

V. Effect of Stoichiometry on Some Properties of Mixed Oxides and UO_2

A. Thermal Conductivity of Hypostoichiometric UO_2

The adverse effect of excess oxygen on the conductivity and irradiation behavior of UO_2 was demonstrated earlier by Robertson in a series of in-pile experiments.⁽¹⁶⁾ These measurements showed that excess oxygen in UO_2 seriously decreases its average thermal conductivity (as defined by postirradiation microstructure). Out-of-pile measurements of thermal conductivity have been made of the effect of stoichiometry on the thermal conductivity of UO_{2-x} and mixed oxide samples under the FCR program.⁽¹⁷⁾ The purposes of these experiments were to establish information on the phase change of $\text{UO}_2 + \text{U}$ to UO_{2-x} in hypostoichiometric UO_2 and how this phase change might affect thermal conductivity, as well as to determine whether analogous effects were present in the mixed U-Pu oxide system.

Figure 7 shows the change in UO_2 thermal conductivity as a function temperature for three different stoichiometries. The results from this study showed

that the $\text{UO}_{1.97}$ had a slightly higher thermal conductivity than $\text{UO}_{1.98}$ in the temperature range from 800 to 1400°C, and both of these hypostoichiometric compositions exhibited 30% higher conductivity than $\text{UO}_{2.00}$ in this temperature range. All hypostoichiometric specimens tested exhibited a threshold temperature at which the conductivity rapidly decreased to a value close to that for $\text{UO}_{2.00}$. This threshold temperature was found to be characteristic for a given stoichiometry and was found to be present on repeated cycling through this range. The thermal conductivity of stoichiometric UO_2 was found to agree well with values obtained by some recent investigations.^(18, 19) The increase in thermal conductivity postulated or observed by some investigators at temperatures above 1600°C was not observed in these experiments. This may be due to the facts that the highest temperature observed was 1800°C and that ΔT used in those measurements was relatively large ($\sim 1000^\circ\text{C}$) to simulate fuel behavior.

It is hypothesized that the break in the conductivity curves of $\text{UO}_{1.98}$ and $\text{UO}_{1.97}$ is associated with the transformation temperature of $\text{UO}_2 + \text{U}$ to the defect structure UO_{2-x} . Figure 8 shows the partial UO_{2-x} phase

6100X

FIG. 6. Electron Micrograph of $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_2$ Showing Substructure within the Grain.

FIG. 7. Thermal Conductivity of Both Stoichiometric and Hypostoichiometric UO_2 as a Function of Temperature.

FIG. 8. Partial UO_2 -Oxygen Phase Diagram with Thermal Conductivity Transformation Temperatures Noted.

diagram with the observed threshold temperatures proposed by Bates.⁽²⁰⁾ By extrapolating the transformation lines, the change in slopes in the conductivity data can be associated with the transformation temperature of the process ($\text{UO}_{2.0} + \text{U}$) to UO_{2-x} .

The higher thermal conductivities of the $\text{UO}_{1.98}$ and $\text{UO}_{1.97}$ specimens below 1400°C is apparently associated with the presence of free uranium in the UO_2 matrix. Higher thermal conductivity of UO_{2-x} material has also been qualitatively observed by Notley at temperatures below 1400°C .⁽²¹⁾ Thermal conductivity changes caused by a noncontinuous dispersed phase based on a modified Maxwell-Eucken relationship⁽²²⁾ indicated that the high thermal conductivity of the $\text{UO}_{2.0} + \text{U}$ region is not caused by just the increased thermal conductivity of free uranium. One possible cause for this higher thermal conductivity is that the presence of free uranium in the UO_2 matrix provides an excess of free electrons, which would result in an additional contribution to the conductivity function.

B. Mixed Oxides

The effect of stoichiometry on the thermal conductivity of mixed oxide was found to be significantly different than that measured for UO_2 . Figure 9 shows

the measured thermal conductivities of $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_2$ specimens with stoichiometries of 2.04, 2.00, and 1.98.⁽²³⁾ These results showed that the $\text{UO}_2 + \text{U}$ to UO_{2-x} transformation temperature is displaced in the hypostoichiometric direction with the addition of PuO_2 . This has been verified by both metallographic and X-ray diffraction data (see Sec. II), which show that $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_2$ is single phase between 1.975 and 2.01 stoichiometry. Out-of-pile measurements on specimens with a stoichiometry of 2.05 are considered atypical since the hyperstoichiometric pellets were found to have microcracks dispersed throughout their volume. They exhibited lower conductivity than the 1.98 material to about $1200\text{--}1400^\circ\text{C}$. Rising conductivity at higher temperatures can be attributed to either heating of microcracks as plasticity develops, or to the dissolution of a second phase. In general, the hyperstoichiometric specimens exhibited a lower thermal conductivity than either the stoichiometric or hypostoichiometric fuel.

C. Melting Point

Melting points for the mixed oxide system have been measured by both V-filament technique with milligram samples and thermal-arrest measurements with massive

FIG. 9. Thermal Conductivity of Both Hypo- and Stoichiometric $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{2+x}$ as a Function of Temperature.

samples. Early measurements of the $\text{PuO}_2\text{-UO}_2$ system by the V-filament method indicated an anomalous maximum in the melting point at approximately 10 w/o PuO_2 .^(24, 25)

Recent V-filament melting-point measurements on the mixed oxide system under the FCR program also showed an anomalous maximum, but at 5 w/o PuO_2 rather than at the earlier noted value for 10 w/o PuO_2 .⁽²⁶⁾ Thermal arrest-melting measurements of the $\text{PuO}_2\text{-UO}_2$ system indicate that with adequate control of sample composition during the measurement this maximum is not observed.

Figure 10 shows the proposed liquidus for the $\text{PuO}_2\text{-UO}_2$ system as measured by thermal-arrest techniques.⁽²⁷⁾ These measurements indicated a simple binary phase diagram with complete miscibility for the $\text{PuO}_2\text{-UO}_2$ system. Thermal-arrest measurements at all compositions did not indicate any significant difference between the solidus and liquidus. On this basis it is reasonable to expect that the $\text{PuO}_2\text{-UO}_2$ system will exhibit a very narrow two-phase field which is consistent with other metal oxide systems, such as $\text{UO}_2\text{-ZrO}_2$, $\text{UO}_2\text{-ThO}_2$, $\text{Al}_2\text{O}_3\text{-Cr}_2\text{O}_3$, $\text{CeO}_2\text{-ZrO}_2$, and $\text{ThO}_2\text{-ZrO}_2$, all of which exhibit a narrow liquid-solid band, approximately 50 to 125°C wide.

The melting point of mixed oxides has been shown to be dependent on the stoichiometry. Figure 11 shows

both tungsten V-filament and thermal-arrest measurements for $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{2\pm x}$ specimens. Results from both techniques agree very well and show that the hypostoichiometric melting temperature is significantly higher than the stoichiometric composition. A similar relationship has also been observed for the UO_{2-x} system when a 150°C higher melting point was observed for $\text{UO}_{1.98}$.⁽²⁸⁾

The maximum noted in V-filament melting measurements at approximately 5 to 10 w/o PuO_2 is believed due to a greater rate of loss of oxygen at high temperatures, giving rise to a $(\text{Pu, U})\text{O}_{2-x}$ solid-solution compound that forms before melting. This new material then melts at the hypostoichiometric value and gives an apparent maximum in the melting data for stoichiometric specimens. The absence of a maximum in enclosed capsules used for the thermal arrest implies that with massive samples there is little or no oxygen loss.

D. Electrical Properties of Mixed Oxides at Low Temperatures

Electrical properties of both PuO_2 and mixtures of PuO_2 and UO_2 have been used by a number of investigators to establish phase boundaries and transformation temperatures. Figure 12 shows summary plots of the electrical-property studies.⁽⁹⁾ Thermoelectric measure-

FIG. 10. Liquidus of the $\text{PuO}_2\text{-UO}_2$ System.

FIG. 11. Effect of Stoichiometry on the Melting Point of $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_2$ Fuel.

ments of the PuO_2 - UO_2 system show an almost linear decrease in the Seebeck coefficient from UO_2 to PuO_2 . A gradual decrease from p- to n-type conduction was noted with increased PuO_2 content.

Electrical resistivity and thermoelectric measurements were found to give values quite sensitive to stoichiometry. Both properties increased with decreasing stoichiometry in a manner similar to UO_2 . The electrical resistivity was found to increase from a value of 4×10^3 ohm-cm for $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{1.975}$, suggesting that the electrically neutral compositions in mixed oxides is closer to 1.975 than 2.00 in mixed oxide. (A similar situation was reported by Duncan⁽²⁸⁾ for the UO_{2-x} system, which showed that a minimum charge carrier concentration exists at ~ 1.99 O/U ratio rather than at 2.00.)

VI. In-pile Behavior of Stoichiometric and Hypostoichiometric Mixed Oxides

A. Short-time Tests

Both hypo- and stoichiometric $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_2$ specimens have been irradiated under gross melting conditions to establish the difference in the $\int_{T_s}^{T_m} K d\theta$ of these fuels. This was accomplished by irradiating a hydraulic

rabbit capsule in the MTR containing both 1.98 and 2.00 stoichiometry fuel in separate compartments to establish the relative amounts of melting in each fuel section. The stoichiometric specimen exhibited significantly larger radius of melting than the hypostoichiometric fuel.⁽²⁹⁾ Figure 13 shows the irradiated microstructure of the two fuel specimens. (The central void in these specimens is a shrinkage void and is not necessarily related to the maximum temperature.)

Table 3 lists the capsule test parameters and results. Significantly more melting was noted in the stoichiometric sample and the integral to melting, from surface to center, was 64 W/cm and 46 W/cm for the samples of stoichiometry 1.98 and 2.00, respectively. Volume change on melting was measured by providing minimum free volume in the pin so that fuel expansion in melting would cause yielding of the cladding. The observed volume change was between 8 and 12%, based on clad expansion. The results from this experiment indicated that hypostoichiometric fuel can operate at significantly higher power level than stoichiometric fuel, based on center melting as a limit. Part of this difference is due to the higher melting point (about 100°C) of the hypostoichiometric fuel. An additional contribution to the higher integral to melting implies

FIG. 12. Electrical Properties of Mixtures of PuO_2 and UO_2 .

FIG. 13. Microstructure of Hypo- and Stoichiometric Fuel Irradiated at Identical Power Levels.

TABLE 3. SMEARED IN-PILE THERMAL CONDUCTIVITY RESULTS FROM HYPO- AND STOICHIOMETRIC $(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_2$

	$(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{1.98}$	$(\text{Pu}_{0.2}\text{U}_{0.8})\text{O}_{2.00}$
Fuel-operating Parameters		
1. Composition, w/o PuO_2	20	20
2. Stoichiometry	1.98	2.00
3. Density, % Theoretical	94.5	94.5
4. Backfill Gas	Argon	Argon
5. Pellet Diameter, in.	0.218	0.218
6. Diametrical Gap (Avg), in.	0.002	0.002
7. Φ_c/Φ_s , Self-shielding Factor	0.38	0.38
8. Time in Pile, hr	5	5
Test Results		
1. Fuel Pin Power, kW/ft	46.6 ± 5	46.6 ± 5
2. $\int_{T_s}^{T_c} K d\theta$, W/cm	94.4 ± 10	94.4 ± 10
3. $\int_{T_s}^{T_m} K d\theta$, W/cm	64 ± 7	46 ± 5
4. Molten diameter, in.	0.160	0.175

that the high-temperature conductivity of hypostoichiometric fuel is slightly greater than stoichiometric fuel.

B. High-burnup Stability

Both hyper- and hypostoichiometric mixed oxides have been irradiated under identical power and thermal-cycling conditions to an average peak burnup of 70,000 MWD/Tm.⁽³⁰⁾ Table 4 lists the in-pile test parameters of the experiment.

TABLE 4. IN-PILE TEST PARAMETERS

Pin Number	E2B-1	E2B-2
Stoichiometry	2.04	1.97
Fuel length, in.	8	10
Diametral gap, mil	1.8	1.8
Fuel diameter, in.	0.150	0.150
Peak power, kW/ft	18.0 ± 1.0	18.0 ± 1.0
Peak $\int_0^{T_c} K d\theta$, W/cm	87	87
Fuel surface temperature, °C	~1000	~1000
Peak fuel-center temperature, °C	~2600	~2600

The results from this experiment indicated no systematic difference between the performance of the fuels at a burnup of 70,000 MWD/T. The only microstructural difference noted was that the hyperstoichiometric fuel had a larger central void, suggesting that the irradiated hyperstoichiometric fuel might have a lower thermal conductivity (or higher volatility) than the hypostoichiometric fuel. This is shown in Fig. 14. Metallic fission products were noted dispersed throughout the high-temperature region (1800–2600°C) in both fuel specimens, and no correlation of the volume of the insoluble phases could be made with stoichiometry. Similar metallic fission products have been previously reported for high-burnup UO_2 and $\text{UO}_2\text{-ZrO}_2$ fuel.⁽³¹⁾

VII. Summary and Conclusions

The reference fabrication method for solid-solution mixed oxide fuel is coprecipitation, pressing, and sintering in hydrogen. Homogeneity with respect to the

FIG. 14. Microstructure of Hyper- and Hypostoichiometric Mixed Oxide at 70,000 MWD/T.

micron scale of measurement is readily attained by this process.

Mixing of UO_2 and PuO_2 powders also shows promise of attaining near-equivalent homogeneity (and 1.97 stoichiometry) after sintering, provided that agglomeration is avoided in the mixing process. Mixtures with agglomerates present imply significant loss of prompt Doppler coefficient.

There is a preliminary basis for preference of stoichiometry of 1.97 to 1.98 for mixed oxide fuel. The most significant properties are thermal conductivity, clad and sodium compatibility, melting point, gas retention, volume expansion (with temperature, with fission products, and on melting), and creep rates under stress. Most of these properties have some dependence on both stoichiometry and on burnup.

The usual stoichiometry for UO_2 fuels is 2.00. Sufficient information has been developed to rule out the hyperstoichiometric UO_2 compositions based on their poorer thermal conductivity. Hypostoichiometric UO_2 on the other hand, shows very definite potential based on a slightly higher thermal conductivity and melting point. One reason for the relative lack of information on hyperstoichiometric UO_2 is the difficulty of fabrication.

With mixed oxides of PuO_2 and UO_2 , hyperstoichiometric compositions are conveniently prepared. Out-of-pile thermal conductivity data show differences at low temperatures, but closely similar conductivity at

1200 to 1600°C for varying stoichiometry. In-pile smeared thermal conductivity data covering a range up to central melting at 2750°C indicate approximately 20% higher conductivity for 1.97 material when compared with 2.00. The higher melting point of the hypostoichiometric composition is also an advantage. Preliminary results suggest that the overall advantage of 1.97 material in conductivity and/or melting point persists to high burnup. Hypostoichiometric mixed oxide fuel has also better compatibility with sodium at high temperature than either the 2.00 or 2.05 compositions.

Unequivocal selection of stoichiometry is not yet possible since this will involve evaluation and weighting of the relative importance of properties such as those listed above, some of which are not yet accurately known except as approximate limits.

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Discussion

Mr. Wirtz (Karlsruhe): We have observed that stoichiometric UO_2 after a fairly short time in a temperature gradient at high temperature changes its stoichiometry by a few percent. We have also observed that thermal conductivity is a function of stoichiometry, as you have already said. So after a short while, every fuel will have adapted to that gradient of stoichiometry. Does this mean something in your consideration?

Mr. Zebroski: No, I think this was fully anticipated. This was the thing that I was giving the calculation on. There is no question that the phenomenon occurs. The question is to what extent it occurs. This also explains why a simple attempt to construct in-pile peak operating temperature curves from out-of-pile temperature data, particularly low temperature with low thermal gradient, is doomed to failure. I think it is more complicated, but the complexities are the kind I think we can handle now.

Mr. Kummerer (Karlsruhe): You have proposed stoichiometry of 1.97 to 1.98 as a reference phase. As I understand it, that means stoichiometry during the production of the material, because I think that the stoichiometry does change markedly with a different temperature of the fuel. Is this assumption right?

Mr. Zebroski: Yes. The stoichiometry of an initial fabricated 1.98 material will vary from as low as 1.91 at the center to 2.006 at the edge for an effective oxygen partial pressure of 10^{-11} atm.

Mr. Kummerer: You mentioned that the wet sintering process leads to a rather high inhomogeneity over a distance of 20 to 40 microns. What were the conditions of this wet sintering, and what is your definition of inhomogeneity?

Mr. Zebroski: We have simply characterized inhomogeneity by peak to minimum ratio concentration and by effective wavelength of the inhomogeneity as determined by the microprobe. This tends to understate homogeneity, because if the inhomogeneity has a dimension of 1 micron and the analytical method has a resolution of 3 microns, you will not see the extremes

in concentration and you have to make allowance for this.

Mr. Bradbury (Harwell): In the case of uranium oxide we have observed microsegregation of the fission products to form grain-boundary inclusions. Have you observed similar effects from high-burnup mixed oxide?

Mr. Zebroski: As far as I know, every fuel element we've ever looked at always shows this effect above perhaps 30,000 to 40,000 MWD/T; you always see the second phase.

Mr. Bradbury: Have you examined the constituents of these inclusions at all?

Mr. Zebroski: I think Bates' work is still the most definitive on this.

Mr. Skavdahl (Battelle-Northwest): I am interested

as to how you analyzed our results to get a 20- to 40-micron inhomogeneity. You reported a 20-micron spot on the probe. We went down to a $\frac{1}{2}$ -micron spot. Our particle size analysis and statistical analysis of the data gave an average particle size of 2 microns and a maximum particle size of about 10 or 12 microns.

Mr. Zebroski: I think this comes back to the definition of inhomogeneity, which is an area of controversy at least three years old now, and I would guess three or six years more will be needed before it is resolved. I would like to observe again that in the resolution between safety and economics, the economic gain to permitting gross inhomogeneity is very trivial. There are good procedures available for getting homogeneity; why defend inhomogeneity so desperately?

Comment

R. E. SKAVDAHL

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I would like to present some recent phase-equilibria data on the plutonium-oxygen and uranium-oxygen binary systems obtained at Battelle-Northwest.

Several substoichiometric plutonium oxide samples, varying in O/Pu atomic ratio from 1.92 to about 1.6, were examined by differential thermal analysis and were found to exhibit (during cooling) a broad exothermic peak at about 250°C. This corresponds quite closely to the eutectoid reaction at 300°C shown in the plutonium-oxygen phase diagram which we published in 1963. The lower limit of O/Pu ratio is given as "approximately" 1.6, since oxidation of the samples at such low stoichiometry made it difficult to determine the precise stoichiometry existing at the time of the observed reaction.

A small, extremely sharp exothermic reaction on heating was observed at about 875°C for a sample which, before heating, had an O/Pu ratio of 1.55, and after cooling, an O/Pu ratio of 1.62. Once again, sample oxidation proved to be a problem. However, the observed reaction agrees very well with the allotropic transformation of α -Pu₂O₃ to α' -Pu₂O₃ shown in our phase diagram to occur at 800°C between O/Pu 1.50 and 1.62. The differential thermal analyses were performed by C. E. McNeilly.

High-temperature reflection electron micrographs were taken by Daniel, Chikalla, and Bauer during the course of heating a sample of PuO_{1.8}. The smooth polished surface of the specimen began to coarsen at about 700°C, which is consistent with the transformation to the single-phase PuO_{2-x} predicted by the phase diagram. No further changes were noted until a temperature of 1000°C was reached. After cooling to room temperature, the sample surface was marked by a series of heavy ridges similar to the lamellae typical of substoichiometric plutonium oxide and which we normally take to result from the eutectoid reaction at 300°C.

Increased confidence in the location of the substoichiometric liquidus line in the uranium-oxygen system has resulted from additional melting-point data by Bates and Horn. Seven data points have been added, as shown in Fig. 1. Each point represents

approximately 15 separate melting-point determinations.

A capsule containing uranium oxide with O/U between 1.93 and 1.98 was irradiated for one cycle in the MTR. At the heat-generation conditions (23 kW/ft) of the irradiation, melting to ~40% of the fuel radius and columnar grain growth to ~72% of the fuel radius would be expected in stoichiometric UO₂. However, the substoichiometric material exhibited melting to only about 20% of the radius and columnar grain growth to about 70%. This is further evidence of the improved thermal conductivity of substoichiometric oxide fuel.

Discussion

Mr. Okrent (ANL): This really relates to a previous question raised by Dr. Wirtz. Since the stoichiometry in fact varies with radial fission during an actual irradiation, what does it mean when you say had the stoichiometry been 2.00 you would have gotten 40% melting during irradiation and in fact you got 20% melting? It is not clear to me how one makes the specification that he wishes 1.97; this must mean that he expects some different gradients under actual operation with 1.97 than were it 1.92 or 2.02. I would appreciate having both of these points elaborated.

Mr. Skavdahl: The observation was an empirical one, based on what we had previously observed in stoichiometric UO₂ experiments at these power-generation conditions. With UO₂ we would have expected to have observed about 40% melting along the radius. However, we started with a lower stoichiometry, again in a gross sense, and we got less melting for the same power generation. I would not care to draw a plot of melting radius or thermal conductivity versus stoichiometry at various positions. I am not able to do that myself.

Mr. Kelman (ANL): I'm confused about the reference to liquidus temperatures and melting point with no reference to solidus temperatures; what is it that you are referring to?

FIG. 1. Tentative Oxygen-Uranium Phase Diagram.

Mr. Skavdahl: The melting points were those observed.

Mr. Kelman: But is this supposed to be congruent melting? Or do you have a solidus and liquidus temperature?

Mr. Skavdahl: It is congruent melting for a single phase.

Mr. Zebroski (GE): I think the question is that you can't just have a single line. With a massive sample you get a thermal arrest, but it is a tiny one. Thus the solidus and the liquidus lines are separated by no more than 50 to 100°, and this is in accord with many other binaries like urania-ceria, urania-thoria,

urania-zirconia, and so on, which also have very narrow differences of the order of from as little as 25 to 100 degrees. I think it just becomes experimental technique when it is that small.

Mr. Skavdahl: I think the error band is actually almost bigger than the difference in the temperature between the solidus and liquidus.

Mr. Okrent: I wonder if Dr. Zebroski would give more of the reasons for the 1.97 in view of the fact that he does have 1.97 at only one radius in the actual operations.

Mr. Zebroski: Well, I think Dr. Skavdahl really gave a very good answer to that empirically. You

have an average value and then you swing about the tie line. It works out from the volume relationships that the mid-volume point, which is about 70% of the radius thermodynamically, stays about the original stoichiometry. The bulk of the fuel at that point rises very slightly to just about stoichiometric, and a very small volume fraction of the fuel, I think of the order of 5%, reaches the extreme low limit of stoichiometry that I mentioned.

Mr. Okrent: I assume that there may be other properties that deteriorate which we don't yet under-

stand, such as swelling. This may turn out to be worse in the cooler portions.

Mr. Zebroski: This has already been observed. Nobody has done experiments in which he says stoichiometry shall stay level. This has been with us always.

Mr. Okrent: I agree. What I am wondering is, will everything be optimum at the 1.97 or only some properties?

Mr. Zebroski: No, this is only a present choice. As of right now, the choice looks like a reasonable compromise with the many properties you have.

Plutonium and Fission Product Redistribution in Mixed Oxide Fuels

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(PRESENTED BY P. E. NOVAK)

I. Introduction

The distribution of plutonium and fission products in mixed oxide fuels is important to the safety and life of fast reactor systems. The interest in plutonium distribution stems from the importance of the negative Doppler coefficient to reactor safety and its dependence on fuel homogeneity. Separation of the plutonium from the uranium reduces the average temperature rise in U-238 relative to the case of complete homogeneity and thus reduces the negative Doppler coefficient. Fission product concentration gradients do not materially influence the Doppler physics of a fast reactor system because of the relatively small amount of heat liberated by neutron capture in fission products. However, the concentrations of chemically reactive species, such as cesium and its precursor, iodine, near the fuel-clad interface may adversely affect the performance of the cladding material.

Several physical and chemical processes are potentially available to produce plutonium and fission product concentration gradients in operating mixed oxide fuels. The most likely mechanisms are:

- (1) zone refining by solidus-liquidus separation (change of state);
- (2) zone refining by solid-state pore sweeping in a thermal gradient;
- (3) differential mass transport of plutonium-rich phases by vaporization and deposition;
- (4) solid-state diffusion-induced separation in a thermal gradient (Soret effect);
- (5) electrorefining by an emf produced in a thermal gradient;
- (6) selective chemical reactions in fuel operating in contact with sodium.

In addition, fission product daughters with gaseous precursors typically show extensive redistribution within the fuel by diffusion and flow of the gases.

It is the purpose of this paper to explore some of these separating or de-mixing phenomena which might

occur and to assess both their magnitude and consequences to the operation of fast reactor systems.

II. Previous Observations of Plutonium and Fission Product Concentration Gradients in Oxide Fuel

Many investigators have observed migration of volatile and gaseous fission products in UO_2 , thoria, and to a limited extent in mixed oxides. However, there is still a wide variation in the patterns reported, presumably because of the difficulty of determining or controlling all of the parameters which can affect the distribution of fission products in the fuel. One fairly consistent pattern is the concentration of Cs-137 at the cooler periphery of the fuel.⁽¹⁻⁴⁾ Another is the tendency for slight enrichment (10 to 20% of average value) of refractory fission products, such as (Ce,Pr)-144, at the fuel center.

There is little agreement on plutonium relocation. Both the 1963 work of Ruiz⁽⁵⁾ on the FCR program and the later work on UO_2 by Bates⁽¹⁾ indicated slight increases in plutonium concentrations near the center of mixed oxide and UO_2 fuel rods, whereas Bailey and Chikalla⁽²⁾ reported insignificant migration of plutonium.

Lewis⁽⁴⁾ reported that the measured plutonium concentration in irradiated UO_2 varied less than $\pm 5\%$, with the exception of two unexplained low-plutonium data points at the midradius of one pellet rod. Figures 1 and 2 show some of these observations.

In much of this work it is not clear whether the trends reported are significantly larger than the possible sampling and analytical uncertainties. However, there does appear to be agreement that the plutonium concentration generally follows the refractory (Cr,Pr)-144 fission product concentration. Interpretation of some of these experimental results is complicated by the large self-shielding corrections that must be made to correct for the preferential surface burnup of plutonium, and for both thermal and resonance capture by U-238 in a thermal reactor spectrum.

FIG. 1. Previously Observed Fission Product Distribution in Oxide Fuel.

FIG. 2. Previously Observed Plutonium Distribution in Oxide Fuel.

III. Recent Measurements of Plutonium and Fission Product Concentration Gradients in Mixed Oxide Fuel

Recent measurements were made both in-pile and out-of-pile to determine plutonium migration rates quantitatively. Davies and Novak⁽⁶⁾ extended the work of Schmitz and Lindner⁽⁷⁾ from 1500 to 2400°C, and measured the self-diffusion coefficients of Pu-242 and U-233 in UO₂ to be 3.4×10^{-10} and 2.6×10^{-10} cm²/sec, respectively, at 2400°C, indicating plutonium to be slightly more mobile than uranium in the solid state.

An in-pile experiment was then performed to evaluate the effect of stoichiometry on plutonium and fission product distribution after irradiation and to evaluate possible separation effects. Figure 3 is a montage which correlates the distribution of (1) alpha activity, (2) beta-gamma activity, and (3) specimen microstructure for 0.25-in.-diameter, 20% plutonium-80% uranium mixed oxide specimens of three different initial stoichiometries irradiated to 5000 MWD/T.

Striking variations in structures and distribution of both fission products and plutonium are evident. However, the tracer-spiked regions intended to measure plutonium diffusion coefficients at several different temperatures were lost due to accidental gas blanketing of portions of the NaK bond external to the fuel-pin cladding. This caused clad failure. Thus these specimens do not represent normal full-power operation; rather, they are indicative of possible effects under extreme operating conditions. This experiment has been repeated under more normal conditions; the results are presently being analyzed.

The microstructures of the hyperstoichiometric (O/M = 2.05) and stoichiometric (O/M = 2.00) fuels are shown in Fig. 4. They consist of three discrete rings about a central shrinkage void. The ring

immediately adjacent to the void has vestiges of dendritic grains, probably formed either by vapor transport or by solidification of a molten two-component system. The former hypothesis agrees with observations reported at Hanford;⁽⁸⁾ the latter agrees with work done at Chalk River.⁽⁹⁾ The middle ring in both specimens consists of short, thick columnar grains, the innermost portions of which are essentially free of voids. The remainder of the fuel contains long, thin columnar grains extending out to the periphery.

The hypostoichiometric (O/M = 1.98) pin contains only one structural zone, consisting of long, thin columnar grains extending from the void to the periphery. No discrete pore-sweeping boundary is evident, and substantial clad deformation was observed in the overheated zone, suggesting that this specimen experienced a somewhat higher local temperature than the other two. The entire cross section may have undergone melting.

The alpha autoradiographs for this specimen (see Fig. 3) show a higher alpha activity (plutonium concentration) in the fuel bordering the central shrinkage void, and along the pellet periphery, with a relative depletion in the midradial region. The width of the inner high-alpha-activity zone (~20 mils) corresponds to the width of the dendritic layer in the hyperstoichiometric and stoichiometric pins. The zone width in the hypostoichiometric pin is of similar magnitude, although its interfaces do not correlate with a discrete fuel structure. Progressing outward, the gradual reduction in alpha activity in the region of the midradius is followed by a steep activity rise as the pellet periphery is approached. This is particularly evident in the hypostoichiometric pin, which shows a well-defined peripheral band of high alpha activity from which radial needles of similar high activity

FIG. 3. Plutonium and Fission Product Distribution in Mixed Oxide Fuel.

FIG. 4. Mixed Oxide Fuel Structure.

extend toward the thermal center. The spaces between the needles exhibit low alpha activity suggestive of plutonium depletion. In addition, the fuel-clad gap, which contains an apparent NaK-fuel reaction product, also appears depleted in alpha activity. The gradual activity changes in the midradial region and at the periphery bear no direct correlation with the columnar grain structure observed in these regions, nor is there any observable activity change in the region where pore sweeping ceases.

Beta-gamma autoradiographs, representative of the same three fuel surfaces, show a closer correlation with grain structure. Again the hyperstoichiometric and stoichiometric pins show similar distribution. The midradial band is essentially void of beta-gamma activity, while the periphery of the fuel contains the highest beta-gamma activity. The dendritic region exhibits somewhat lower activity. Beta-gamma activity distribution in the hypostoichiometric pin is concentrated primarily in a thick zone surrounding the central void and along the pellet periphery. Thin fingers of high activity were observed radially throughout the entire fuel cross section.

Figure 5 shows the plutonium concentration as a function of position along the radii of the stoichiometric and hypostoichiometric specimens. It also

shows plutonium distribution in a very-low-burnup mixed oxide specimen that sustained gross central melting during a 10-min irradiation. Note that melting in the unfailed fuel pin did not cause the degree of segregation observed in the higher-burnup, failed fuel specimens that operated in contact with sodium. There appears to have been no axial plutonium redistribution.

The solid lines shown in Fig. 5 represent ultrasonically drilled 0.015- and 0.030-in.-diameter micro-core samples from the high-burnup specimens and X-ray fluorescence analysis on the low-burnup specimen. In the X-ray fluorescence analysis a 0.040×0.080 -in. elliptical probe was used. The dashed line in the hypostoichiometric specimen represents the analysis of two loosely held fuel particles removed from the highly alpha-active, needle-like zone and periphery of the specimen with adhesive backed tape.

Mass-spectrometric analyses of sample solutions showed that the concentration of plutonium across the fuel sections correlated closely with the relative alpha-activity profile shown in the autoradiographs.

The concentration of both plutonium and fission products into two general areas of the fuel indicates that separation occurred by at least two discrete mechanisms. Enrichment around the central shrink-

FIG. 5. Observed Plutonium Distribution in Mixed Oxide.

age void with evidence of gross fuel melting suggests the preferential separation of plutonium upon the solidification of molten mixed oxide. Zone refining may contribute to the central enrichment by preferentially depleting the midradial region. Sodium reaction with the failed fuel probably accounts for the narrow concentration and depletion bands at the periphery.

In the case of fission products, the same mechanisms that separated plutonium can separate refractory fission products. However, the additional process of "distillation" is available to concentrate volatile fission products and their precursors at the periphery of the fuel.

IV. Consequences of Plutonium Redistribution on the Calculated Doppler

The consequences of various patterns of plutonium redistribution on Doppler reactivity were calculated

for a large, sodium-cooled fast reactor fueled with mixed oxide. Assumptions used in these calculations include: no heat conduction during a transient, no Doppler saturation, no effect on neutron spectrum by plutonium redistribution, negligible spatial neutron-flux depression, no radial fuel densification, no axial migration, and the final fuel temperature is a function of initial temperature and fissionable isotope concentration. The reactor analyzed had a 2-ft-high by 11.7-ft-diameter pancake core with a ratio of 0.092:0.005, respectively. The plutonium content of the mixed oxide fuel was 24%, with 13.65% Pu-239 and 7.67% Pu-240, the balance being higher isotopes of plutonium. The calculational transients terminated at an average fuel temperature of 2000°K.

The two basic calculational models for plutonium segregation are shown in Fig. 6. The parabolic distribution corresponds to the core-sampled hypostoichiometric specimen; the parabolic distribution without

a) PARABOLIC Pu DISTRIBUTION

b) PARABOLIC Pu DISTRIBUTION (MODIFIED)

FIG. 6. Calculational Plutonium Distribution Models.

a) PARABOLIC Pu DISTRIBUTION

b) PARABOLIC Pu DISTRIBUTION WITHOUT EDGE DEPLETION

— 750 - 2000°K
 - - - 1450 - 2000°K

FIG 7. Effect of Plutonium Migration on the Doppler Coefficient of Reactivity.

TABLE 1. CALCULATED DOPPLER REACTIVITY CHANGE DUE TO OBSERVED PLUTONIUM REDISTRIBUTION

Observer	$(E_0/\bar{E}) - 1$	Δk -Doppler (Migration)/ Δk -Doppler (no Migration)			
		Parabolic Plutonium Distribution		Parabolic Plutonium Distribution without Edge Depletion	
		Zero power	Full power	Zero power	Full power
Ruiz <i>et al.</i>	0.20	1.00	0.99	1.00	1.00
Bailey and Chikalla	0.37	0.99	0.97	0.99	0.99
Bates	0.41	0.98	0.97	0.99	0.99
Lewis	0.05	1.00	1.00	1.00	1.00
This work:					
5000 MWD/T; O/M = 1.98	0.14	1.00	0.99	1.00	1.00
5000 MWD/T; O/M = 2.00	0.45	0.98	0.96	0.99	0.99
3 MWD/T; O/M = 1.98	<0.05	1.00	1.00	1.00	1.00

edge depletion corresponds to the other specimens discussed here. The dashed line represents the edge enrichment noted in the loosely held particulate samples from the hypostoichiometric specimen.

Figure 7 exhibits the effect of plutonium redistribution on Doppler reactivity Δk -Doppler (migration)/ Δk -Doppler (no migration) as a function of central (E_0) and average ($\bar{E}$) fuel enrichment for each of the above models.

Table 1 shows the calculated Doppler reactivity change for zero power and full power for each of the observed instances of plutonium segregation, both for the parabolic plutonium distribution and for the parabolic distribution without edge depletion. In the full-power calculation an initial parabolic temperature profile in the fuel rod was assumed. The assumption of the parabolic plutonium distribution with edge enrichment increases the Doppler degradation by approximately 1% compared to the parabolic distribution without edge depletion. Note that in the cases of unfailed mixed oxide fuel (Ruiz *et al.*, and Bailey and Chikalla) the Doppler is reduced a maximum of 3%. The use of Bates' observations for these calculations is open to serious question because his plutonium distribution data is a measure of plutonium buildup in UO_2 fuel in a thermal-neutron spectrum rather than separation of an initial two-component system. However, applying such data to a mixed oxide fuel would correspond to a 3% decrease in Doppler. The differences in calculated Doppler change for the two high-burnup specimens of this work correspond primarily to the differences in detailed thermal history during gas blanketing and NaK attack on the fuel after failure. These vary from 0 to 4% and represent extreme conditions.

V. Conclusions

The following conclusions are drawn from the above analyses:

- (1) Plutonium redistribution has been observed in high-performance mixed oxide fuel. The central region tends to become enriched in plutonium with a corresponding depletion in the midradius.
- (2) The maximum effect of the segregation observed in this report is to reduce the calculated Doppler shutdown reactivity by 4%.
- (3) Refractory fission products tend to follow the observed distribution of plutonium while volatile species concentrate in the cooler peripheral regions of the fuel.

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Discussion

Mr. Wirtz (Karlsruhe): I missed in your presentation any indication that temperature may play a part in the segregation of plutonium. It is well possible that the segregation is very strongly dependent on

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Discussion

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temperature and may even sometimes change sign with change of temperature. Have you observed such effects?

Mr. Novak: Our data apply to high-performance fuels, for which $\int kd\theta$ is over 80–85. The original FCR data are for a high-performance pin running at maybe 17 kW/ft linear power; Bailey and Chikalla's experiments also ran about there. I would suspect that in a low-temperature pin you would not see these gross magnitudes; in fact, for a pin that sustained gross center melting on short burnup in a 10-minute irradiation there was very little segregation.

Mr. Persiani (ANL): Would you care to comment on the migration properties as a function of the fabrication technique when you talk about solid-solution fuels or mechanical mixed fuels, and also as a function of temperature?

Mr. Novak: I believe that all the mixed oxide pins that were discussed were certainly the FCR ones which were coprecipitated mixed oxides. The UO₂ work by Bates was a high-burnup situation.

Mr. Persiani: Would you expect a different behavior if you had a physical homogeneous mixture?

Mr. Novak: I don't know. We have not studied this particular case. There would probably be different effects on the Doppler.

Mr. Okrent: Your study of the change in the Doppler effect indicated that this wasn't very large. I

wonder if you had the same sort of buildup of plutonium in the center of the fuel and you went on to the unlikely accident where you were supercritical and needed a certain amount of Doppler effect prior to fuel failure. In this case, would the amount of energy the reactor could absorb prior to fuel failure be markedly influenced by the fact that you now had an appreciable concentration of plutonium near the center of the pin and could reach vaporization, for example, earlier in the transient?

Mr. Novak: Yes, with the plutonium near the center in a fast transient it would heat up higher. The plutonium would melt. This particular effect was not taken into account in this calculation.

Mr. Campise (AI): Were these irradiations done in a thermal reactor?

Mr. Novak: Yes.

Mr. Campise: What kind of surface-to-center flux depression did you have, and what effect would the increased capture rate in U²³⁸ have on the plutonium distribution in the pins?

Mr. Novak: The center-to-surface flux depression, as I recall, was of the order of about a half. In these lower burnup cases, namely, 5000 MWD/T, for which we did the microsampling, the burnout of plutonium or burnup of plutonium at the edge would be virtually compensated within limits of any of our measurements by the thermal and resonance capture of U²³⁸.

Microanalytical Studies of Micro- and Macro-segregation in Oxide Fuels*

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(PRESENTED BY K. WIRTZ)

1. Introduction

For the evaluation and development of fast reactor oxide fuels, techniques applicable to the study of segregation in the fuel are required. From the viewpoint of nuclear safety, metallic-ion segregation and quantitative data on the ion mass transfer in a thermal gradient are of particular importance. Evaluation of preparation and fabrication procedures, as they affect fuel homogeneity, is desired. Because thermal, mechanical, and chemical properties vary remarkably with stoichiometry, techniques capable of quantitatively detecting oxygen segregation, which can seriously affect fuel performance, also are required.

Electron-microprobe techniques, if adequately adapted for high-sensitivity analysis in the presence of radioactive material, are uniquely suited to the study of short-range segregation. Quantitative elementary microanalysis is effective for the elements boron to plutonium (atomic number Z from 5 to 94), and highly localised point analysis of microvolumes ranging from $1\ \mu^3$ to integrated surface scannings of $(300)(300)(1)\ \mu^3$ with a detection limit of about 0.01 %.

A segregation of solid fission products as a consequence of temperature distribution in oxide fuels during irradiation has been previously observed.^(1, 2)

The present study was conducted to investigate the segregation of CeO_2 in UO_2 annealed in a temperature gradient. The CeO_2 was intended to serve as a substitute for PuO_2 , as well as being a representative of solid fission products. The results obtained are preliminary in nature, but they demonstrate the value of the technique in studying the segregation problem.

2. Outline of Experimental Procedure and Necessary Adaptations for Microanalysis of Irradiated Specimens

For unirradiated plutonium-containing targets, only minor changes in conventional electron probe microprobe analysis have to be introduced. A vacuum evaporated thin layer of about 80 Å nickel, plated simultaneously on the surface of the metallographically mounted and polished surface of the specimen and the calibration standards, meets all the following requirements. Satisfactory retention of fugitive activity, increase of thermal and electrical conductivity of ceramic materials, perfect adherence to the specimen surface, sufficient optical transmission for microscopical observation of structure and localisation of the analysis and, above all, insignificant effect on the X-ray excitation and emission efficiency.

For microanalysis of irradiated materials, the presence of an intense source of gamma radiation necessitates radiobiological protection for the operator. The electron-optical bench, including the specimen stage and the associated electronic units of a commercial CAMECA D 46⁽³⁾ instrument, are shown in Fig. 1. A 10-cm-thick protection wall of lead, surrounding the specimen chamber, the remote specimen-introduction system, and the drives for the specimen stage and spectrometer scan are shown in Fig. 2.

For microanalysis of unirradiated material or general servicing, the lead shield can be hydropneumatically lifted upwards. The upper part of the electron-optical bench is screened by a 7-cm-thick Denal ($D = 18$) plate placed above the objective lens and pierced to leave only passage to the electron beam. Below the specimen stage a combined Denal-lead plate protects the lower part of the bench with the vacuum pumping units. The use of a new type of X-ray spectrometer,⁽³⁾ with a constant take-off angle of the emergent X rays, independent of their wavelengths, facilitates enormously the correct channeling of the characteristic X rays and the elimination of direct or scattered gamma radiation.

* This paper has been prepared within the framework of the association EURATOM Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

[†] U. S. Atomic Energy Commission, delegated to the Karlsruhe Fast Breeder Project.

[‡] Institute for Neutron Physics and Reactor Technology, Karlsruhe.

[§] EURATOM, Brussels, delegated to the Karlsruhe Fast Breeder Project.

FIG. 1. Commercial CAMECA Microanalyzer and Associated Electronic Units.

A pierced cylindrical collimator (of DENAL) is placed immediately after the X-ray entry window of the spectrometer, leaving passage only for the X ray in the direction of the monochromator. A second collimator, consisting of a DENAL tube, is placed before the window of the detector and is automatically oriented in the direction of the analysed radiation.

The correlation of the sensitivity of analysis, $\Delta C/C$, with the total counting interval Δt , in seconds, required for one analytic determination, as influenced by the mean counting rate r_c (in I/sec) and the peak to background ratio P/B (both to be determined experimentally on the specimen) is shown in the nomogram of Fig. 3 and corresponds to the statistically obtained sensitivity formula⁽⁴⁾ for microprobe analysis:

$$\frac{\Delta C}{C} = \frac{2 \sqrt{r_c}}{\sqrt{\Delta t} (r_o - r_B)}; \quad P/B = r_c/r_B;$$

$$y = r_c \left(\frac{\Delta c}{c} \right)^2.$$

As a practical example the dashed lines in Fig. 3 show how it is possible to determine the necessary counting interval from the required sensitivity. The gamma activity of irradiated material noticeably decreases the peak-to-background ratio, even for correctly shielded instruments employing pulse-height discrimination analysis. The diagram in the upper right corner of Fig. 3 shows the experimentally determined variation of the P/B ratio with sample activity; it is strictly applicable only for the experimental conditions

and the specific shielding of the instrument used. The reduction of the P/B ratio for microanalysis of irradiated materials consequently requires an increased counting interval. However, the formation rate of the well-known carbon contamination spot at the point of impact of the electron beam limits the counting interval in conventional microanalysis to about 50 to 100 sec.

Consequently the use of a decontamination device (such as an argon stream directed through a mass-spectrometer vacuum leak and a nonmagnetic capillary onto the point of impact of the electrons,⁽⁴⁾ which permits arbitrary long counting time intervals) is required. For the determinations of uranium, both the $UL_{\alpha 1}$ and the U_{β} lines and the $Ce L_{\alpha 1}$ line were used. For the determination of local variation of oxygen an empirically calibrated fixed-charge procedure, described by Heinrich,⁽⁵⁾ was used.

3. Materials and Thermal Diffusion Treatment

$UO_2 \cdot CeO_2$ pellets, 8 mm in diameter and 10 mm long, were produced by NUKEM, Germany, either by mechanical mixing of powders or by coprecipitation followed by sintering in a hydrogen atmosphere at temperatures ranging from 1500°C to 1650°C for 2 hr. The samples were submitted to thermal diffusion for up to 600 hr and temperature gradients ranging up to 750°C/cm, developed by placing a specimen on a water-cooled pedestal, the other end being heated in a refractory metal furnace.

For some special studies of microsegregation in the high-temperature range, pellets with a central hole

Fig. 2. Electron Microanalyzer for Irradiated Materials.

were radially heated by a tungsten rod and thermal gradients up to $5000^{\circ}\text{C}/\text{cm}$ were reached.

After the thermal treatment, the pellets were sectioned longitudinally by a diamond wheel, and one surface metallographically prepared and submitted for microanalysis. Both macrosegregation and microsegregation were studied by quantitative electron-microprobe analysis.⁽⁴⁾ For the macrosegregation studies, mean cerium concentrations were obtained by quantitative scanning of surface regions of $50 \times 300 \mu$, accomplished either by mechanical scanning, in which case the specimen is translated under a static electron probe, or by use of the CAMECA semi-electronic scanning

device. This technique was necessary to decrease the scatter of statistical point analysis, caused by local inhomogeneities. Conventional point-by-point analysis was used to study microsegregation, a consequence of the interaction of small CeO_2 inclusions within the matrix.

4. Experimental Results

Metallographic inspection and quantitative microanalysis demonstrated that the as-sintered material consisted of an apparently homogeneous $(\text{U,Ce})\text{O}_2$ matrix containing inclusions of pure CeO_2 . These inclusions, with varying diameters up to 100μ , were

FIG. 3. Nomogram for Correlation of Required Sensitivity, Counting Interval, Counting Rate, Peak to Background Ratio, and Sample Activity.

randomly distributed and frequent in pellets sintered from mechanically mixed powders, smaller and only rarely observed in coprecipitated material.

4.1 Macrosegregation after Thermal Diffusion

The typical effect of a high thermal gradient on cerium mass transfer in sintered pellets of coprecipitated $\text{UO}_2\text{-CeO}_2$ is shown in Figures 4 and 5. Identical

behaviour was observed in mechanically mixed fuels, as is seen by the cerium concentration profile along the temperature axis, shown in Fig. 6.

The essential effects of annealing in a temperature gradient are the migration of cerium to regions of higher temperatures and preferential loss by evaporation at the highest temperature.

Three distinct zones can be identified:

In a first zone, up to temperatures of 1450°C, no variation of the original concentration was observable, because of low diffusion rate. In the temperature range above 1450°C, thermal diffusion results in preferential cerium migration towards regions of higher temperatures.

At temperatures at which equiaxed grain growth occurs (about 1900°C), the cerium concentration decreases, because of preferential loss by evaporation of cerium at the high-temperature end of the specimen. At these temperatures an inversion of the Soret superposition may occur. However it is difficult at this stage to separate thermal diffusivity effects from those due to preferential evaporation. The position and the amplitude of the minimum and the maximum are time dependent, moving to lower temperatures with increased time, while simultaneously the depth of the minimum increases. When a crack occurs across the specimen, it acts as a diffusion barrier, as illustrated in Fig. 6.

4.2. Microsegregation

The mechanism responsible for the kinetics of macrosegregation can be qualitatively studied by the micro-

analytical determination of the CeO_2 inclusion, the $(\text{U,Ce})\text{O}_2$ matrix, and their interaction.

In the region up to 1450°C no interaction between the CeO_2 particles and the matrix occurred, as can be seen in Fig. 7, which shows a surface scanning image in the $\text{Ce } L_{\alpha 1}$ line and a linear scanning line across the interface of the inclusion and matrix, where the CeO_2 concentration drops immediately to the matrix concentration. The hollow spherical aspect of the CeO_2 inclusions is probably due to a lack of plasticity of the pure CeO_2 powders during the pelletising step.

In the temperature range from 1450°C up to 1900°C the diffusion mechanisms have been evaluated from the depth X of the interdiffusion layer between the matrix. By linear Ce-scannings along a line of equi-thermal flux, the chemical diffusion coefficients can be determined.⁽⁶⁾ The effect of a positive thermal gradient $+(dT/dx)$ is measured by scanning the Ce-concentration profile against the direction of the thermal flux (towards the higher-temperature region); the influence of a negative thermal gradient, $-(dT/dx)$, is measured in the opposite direction.

Typical linear scanning curves in the three directions are compared in Fig. 8, and the results are sum-

FIG. 4. Effect of Thermal Gradient $[(dT/dx) = 860^\circ\text{C}/\text{cm}]$ on Cerium Segregation in Coprecipitated $\text{UO}_2\text{-CeO}_2$ (20 m/o). Diffusion Annealing at 2100°C – 1240°C for 185 Hr.

FIG. 5. Effect of Thermal Gradient [$(dT/dx) = 562.5^{\circ}\text{C}/\text{cm}$] on Cerium Segregation in Coprecipitated $\text{UO}_2\text{-CeO}_2$ (6 w/o). Diffusion Annealing at 1870°C - 1420°C for 600 Hr.

FIG. 6. Effect of Thermal Gradient [$(dT/dx) = 420^{\circ}\text{C}/\text{cm}$] on Cerium Segregation in Wet, Mechanically Mixed $\text{UO}_2\text{-CeO}_2$ (3 w/o). Annealing at 1800°C - 1380°C for 40 Hr.

FIG. 7 Effect of a Thermal Gradient on CeO_2 Inclusion in $\text{UO}_2\text{-CeO}_2$ at 1400°C .

marized in Fig. 9. The direction of enhanced diffusion noted around the inclusions is in exact agreement with the direction of diffusion in a thermal gradient observed on a macroscale, described in Sect. 4.1. In the temperature range above 1900°C , the CeO_2 concentration decreases with increasing temperature, as evaporation appears to become a predominant mechanism. This is clearly seen in the absorbed electron image of Fig. 10 (the brightness of the image is proportional to the mean atomic number of the specimen), where around the small equiaxed grains a cerium depleted zone is seen. High-sensitivity statistical analysis gives evidence of a decrease of cerium and oxygen from the center to the grain boundary of the crystals. The steepest cerium gradient is observed in the direction of the higher temperature [$+(dT/dx)$]. The grain-boundary zone of the columnar grains is clearly hypostoichiometric. The formation and growth of the columnar grains by vapor

transport can be inferred by observation of the cerium and oxygen gradients within the grains.

For the initial long annealing periods, the CeO_2 inclusions dissolved completely in the solid solution at higher temperature. Recently, short-time diffusion treatments were performed on axially heated pellets involving a very high thermal gradient, and inclusions were still partially observed at temperatures up to 2100°C . Porosity and a brittle interdiffusion layer prohibited a quantitative analysis, however, in comparison with the diffusion studies in the second temperature zone; an inversion of the relative interpenetration depth with the thermal gradient, dT/dx , was observed.

In all specimens examined to date, no metallic uranium inclusions have been observed, the metallic inclusions seen in the electron picture of Fig. 11 being precipitates of the refractory metal used in the heating plates. Analysis of the heating plates showed an absorption of evaporated CeO_2 and, to a minor extent, of UO_2 .

5. Discussion

Two significant mechanisms for the migration of CeO_2 in UO_2 have been identified in these studies: thermal diffusion and vapor transport. Thermal diffusion of CeO_2 in the direction of increasing temperature predominates at lower temperatures; above about 1900°C preferential evaporation of CeO_2 from the high-temperature end of the specimen and vapor transport, leading to a segregation on a microscale, predominates. The maximum in CeO_2 concentration observed near the lower temperature limit of the columnar-grain region is consistent with the increased activity of solid fission products noted at the same location in irradiated oxide fuels.⁽²⁾ In view of the chemical similarities between the lanthanide and actinide metals, a similar segregation pattern for PuO_2 in UO_2 might be expected.

On the basis of the relative sintering behaviour of CeO_2 and PuO_2 containing UO_2 fuel, more rapid plutonium diffusion is indicated.

From the concentration curves of the type shown in Figures 4–6, the temperature dependence of the chemical and thermal diffusion coefficients, and of the Soret effect, can be calculated by analog and digital computing methods. Such calculations are now in progress and will be published when completed.⁽⁷⁾

In order to provide an estimate of the Soret effect, it is assumed that after a diffusion gradient treatment of 600 hr an almost steady state has been reached. At this point the chemical and thermal diffusion driving forces are balanced and the atomic flux J becomes zero. Thus,

$$J = -D\rho \frac{dc}{dx} + D'\rho c(1 - c) \frac{dT}{dx} = 0,$$

where

D = chemical diffusion coefficient;

D' = thermal diffusion coefficient;

c = weight fraction;

T = temperature, in $^{\circ}\text{K}$;

x = distance.

The Soret coefficient is

$$S_T = \frac{D'_T}{DT} = \frac{dc/dx}{c(1-c) \frac{dT}{dx}}$$

From the CeO_2 concentration profile in Fig. 5 a coefficient

$$S_T = 2.3 \times 10^{-3} \text{ (per } ^{\circ}\text{K)}$$

is estimated at 1750°C .

FIG. 8. Influence of Thermal Gradient on Mass Transport in $\text{UO}_2\text{-CeO}_2$ Pellets (No. 43) at 1600°C . Mechanically Mixed $\text{UO}_2\text{-CeO}_2$ (4 w/o).

Specimen No.	Fabrication Method	Temperature Gradient, $\Delta T/\text{cm}$	Temperature of Inclusion, $^{\circ}\text{C}$	Annealing Period, sec	Penetration, cm^{-4}
27	Mechanically mixed	+550	1750	5.04×10^4	26
		-550			15
		0			18
43	Mechanically mixed	+420	1600	1.44×10^5	32
		-420			20
		0			23
57	Coprecipitated	+750	1570	4.18×10^5	55
		-750			32
		0			38
100	Coarse; mechanically mixed	+5000 -5000 0	2000	2.16×10^4	To be analysed

FIG. 9. Influence of Temperature Gradient on Microsegregation

FIG. 10. Absorbed Electron Image Showing Cerium Depletion at Grain Boundary of Equiaxed Grains, $200\ \mu \times 200\ \mu$.

FIG. 11 Absorbed Electron Image Showing Refractory Metal Inclusions at Temperatures of 2050°C in $(\text{U,Ce})\text{O}_2$ (Sample No. 57), $200\ \mu \times 200\ \mu$

Furthermore, in view of the importance of the problem of plutonium relocation, and its effect on power distribution and reactivity in Fast Reactors, it is planned to extend these studies to both unirradiated and irradiated plutonium containing UO_2 fuel. It is anticipated that the analysis of specimens with up to 100-Ci activity will be possible with the shielded micro-probe unit described in this paper.

Acknowledgment

The authors wish to express their sincere thanks to Prof. Dr. K. Wirtz for encouraging the work on thermal

diffusion and to Prof. Häfele for stimulating the micro-probe work.

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Discussion

Mr. Zebroski (GE): We published some calculations of the Soret constant some years ago. The results were very disappointing; the constant was not a constant. The calculations involved a good many thermodynamic quantities which have not been measured and which probably will not be measured in the near future because of the difficulty in making these measurements in high temperature. I think this is a convenient way of correlating the data, but I believe it will not be useful for making predictions.

Mr. Wirtz: I do not agree entirely. Indeed you must find out what thermodynamic values are behind this picture and you know that for the Soret coefficient

$$J = D \frac{\partial c}{\partial x} + D' c(1 - c) \frac{dT}{dx} = 0;$$

$$-\frac{D'}{D} = \frac{1}{c} \frac{dc}{dT} = S = -\frac{Q}{kT^2} = 2.3 \times 10^3/^{\circ}\text{C at } 1750^{\circ}\text{C},$$

where Q is measured at one of the temperatures. It is also temperature dependent, but I am quite sure that for a certain mixture of uranium and plutonium oxide this value Q is unique. It should be measured. It will vary with temperature and composition of the specimens but it is a physical constant. I think it would be of much more value to find out those physical constants instead of presenting to us measurements of different probes and different reactors and the different irradiation conditions and at different temperatures, and never find out what these physical values are.

Mr. Paul (NUMEC): If this sort of analysis does eventually prove to be of some value, the same sort of attack or approach might be of some value in examining the thermal conductivity of the various types of fuels, since the same principles of irreversible thermodynamics should apply.

Mr. Wirtz: No, no, definitely not.

Mr. Paul: Part of the conductivity would be due to the transport of the various materials through the fuel.

Mr. Wirtz: Only a very small amount of (fuel) heat conductivity would arise from this effect.

Mr. Zebroski: I think that we should mention that we have seen no segregation at all. What we observed were just concentration gradients, which you might

call *de facto* segregation. But with the five or six different mechanisms operative and particularly when you get a wavy concentration gradient in the actual thermogradient situation, I think it is very bold indeed to ascribe this entirely to the Soret effect. In fact, I think that Dr. Theisen did suggest two other mechanisms operated in the same sample.

Mr. Wirtz: I agree entirely.

Session V-B
October 13, 1965

Mostly Reactivity Coefficients

Chairman: R. AVERY
Argonne National Laboratory

Secretary: B. J. Toppel
Argonne National Laboratory

Recent Investigations of Fast Reactor Reactivity Coefficients

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(PRESENTED BY H. H. HUMMEL)

I. Equivalence between Homogeneous and Heterogeneous Resonance Integrals in Cylindrical Geometry

Although the equivalence relation between homogeneous and heterogeneous resonance integrals⁽¹⁾ has proven very useful, it seemed desirable to determine whether it can be made sufficiently accurate for use in multigroup cross-section-generation codes.

A key assumption of the theory is that the flux in each region can be assumed spatially flat. Another important assumption is that the probability $P_0(E)$ that a uniformly distributed source of neutrons of energy E will escape from an isolated rod without suffering a collision is given by the rational approximation

$$P_0(E) = \frac{1}{1 + (\Sigma_0(E)/\Sigma_{e0})}, \quad (1)$$

where $\Sigma_0(E)$ is the macroscopic total cross section of the rod, and

$$\Sigma_{e0} = S_0/4V_0 = 1/2R_0. \quad (2)$$

Here S , V , and R are the surface, volume, and radius, respectively, of the rod; subscripts 0 and 1 refer to fuel and moderator regions, respectively. The rational approximation is not extremely accurate, and an improvement suggested by Levine⁽²⁾ is to replace Eq. (1) by

$$P_0(E) = \frac{1}{1 + (\Sigma_0(E)/\Sigma_{e0}a)}, \quad (3)$$

where a is a constant, assumed to be the same for every resonance.

If one regards a as a function of E , as will be done for both a and Σ_0 in the following, clearly the rational approximation can be made exact, but it is then no longer useful for determining an equivalence relationship. It was suggested by Otter⁽³⁾ that a mean value $\bar{a}$ be defined for each resonance by, in effect, integrating over the resonance by breaking it up into intervals, in each of which Eq. (2) is applied with appropriate constant values of parameters. However, it was found by

Otter that Levine's assumption with a constant value of $\bar{a}$ of 1.35 for all resonances gave quite good results.

For a lattice in which Dancoff interaction must be taken into account, if an equivalence relation is to be obtained, the escape probability for the rod in the lattice, P_0^* , must again be cast in the rational form

$$P_0^* = \frac{1}{1 + (\Sigma_0/\Sigma_{e0}^*)}. \quad (4)$$

An approximate relationship between P_0 and P_0^* ,

$$\frac{1}{P_0^*} = \frac{1}{P_0} + \frac{\Sigma_0 C}{\Sigma_{e0}(1 - C)}, \quad (5)$$

has been developed^(4,6) by use of the assumption that paths in successive fuel pin cells are uncorrelated. When, as suggested by Levine,⁽²⁾ the correction factor a is introduced for the lattice case also, one obtains from Eqs. (3), (4), and (5),

$$\Sigma_{e0}^* = \Sigma_{e0}(1 - C) \frac{a}{1 + (a - 1)C}. \quad (6)$$

The factor a is still that for P_0 and appears here because of the relation Eq. (5) between P_0 and P_0^* .

The equivalent σ_p , the effective reactor scattering cross section in barns per absorber atom to be used in a homogeneous resonance integral calculation, is then

$$\sigma_p = \sigma_{p0} + \frac{\Sigma_{e0}^*}{N_0}, \quad (7)$$

where σ_{p0} is the fuel-rod scattering cross section per absorber atom in barns and N_0 is the concentration of absorber atoms in the fuel in atoms/cc $\times 10^{-24}$.

The accuracy of Eq. (6) was checked by calculations kindly performed by E. Pennington, who used his collision probability code.⁽⁷⁾ This code uses the flat-flux approximation and an isotropic cell-boundary condition. The calculations were carried out for monoenergetic neutrons for a 0.25-in. pin with steel clad and sodium coolant typical of a fast power breeder. Various values of the relevant cross sections were assumed to cover the range of escape probabilities of interest. Equation (6) was found to be accurate to about 1% for a defined by the correct isolated-rod escape proba-

bility and with C obtained from the calculation with Pennington's code. It was found also that clad and coolant regions could be homogenized in the calculation of C with good accuracy, a result previously obtained by Pennington⁽⁸⁾ for water lattices. The discrepancy in $\gamma = 1 - C$ noted in this case between the two- and three-region cells did not exceed 1%.

An approximate method of calculating the Dancoff factor, proposed by Bell,⁽⁶⁾ is based on the equation

$$\gamma_B = 1 - C_B = \frac{1}{1 + (\Sigma_{el}/\Sigma_1)}, \quad (8)$$

where Σ_{el} in this case is

$$\Sigma_{el} = \frac{S_0}{4V_1} = \frac{R_0}{2(R_1^2 - R_0^2)} \quad (9)$$

and R_1 is the outer radius of the moderator (or in this case, homogenized clad and coolant). It has been found by Leslie, Hill, and Jonsson⁽⁹⁾ that γ does indeed depend to a good approximation only on Σ_1/Σ_{el} , although Eq. (8) gives values about 12% too low at large values of Σ_1/Σ_{el} , in the range from 2 to 3. Calculations of γ were carried out in Ref. 8 with a collision-probability cell calculation with isotropic return quite similar to Pennington's for a square cell with $V_1/V_0 = 1$. Calculations were also carried out in Ref. 9 for this same cell with a Monte Carlo calculation, with the result that the collision-probability results were about 3% too high at large Σ_1/Σ_{el} . Values of γ calculated by Pennington⁽⁸⁾ for a series of different water-moderated lattices, as well as those discussed in the present paper, all fall very close, within 1 or 2% in most cases, to the curve of γ versus Σ_1/Σ_{el} for the escape-probability calculation of Ref. 9, which is reproduced in Fig. 1. The points

obtained by Pennington's code are also plotted. If γ really does depend only on Σ_1/Σ_{el} , then it seems one would do just as well by making an accurate determination of this universal curve and using an empirical fit. Whether this is really so perhaps requires further exploration. In any event, it is suggested that for the time being a modified Bell approximation be obtained by using a fit to the Monte Carlo curve of Ref. 9 for γ versus Σ_1/Σ_{el} . A simple fit, which is 2% low at $\Sigma_1/\Sigma_{el} = 3.0$ and closer at smaller values, is

$$\gamma = \gamma_B + \gamma_B^4 (1 - \gamma_B). \quad (10)$$

In using Eq. (6), one is still faced with the problem of what mean value $\bar{a}$ to use. It would be possible to make $\bar{a}$ a function of C . This may not be worthwhile, however, since for C nearly unity the factor on the far right side of Eq. (6) is nearly unity regardless of $\bar{a}$; for small C , the case of an isolated rod is approached.

An investigation of the appropriate value of $\bar{a}$ was made by use of the code of Kier.⁽¹⁰⁾ By means of this code the Boltzmann equation is solved in the resonance region for a two-region cylindrical cell with up to three resonant isotopes allowed in the fuel region. Isotropic return is assumed at the cell boundary. Since a comparison was made with resonance integral calculations made in the narrow-resonance approximation and without interference scattering, Kier's code was modified to be consistent with these assumptions. The effect of spatial flux variation was still present, however.

The calculations were performed for an oxide fuel pin with stainless steel clad and sodium coolant, the latter two being assumed homogenized. Cell dimensions and compositions were chosen⁽¹¹⁾ according to the specifications for the comparison calculations proposed for this conference by Okrent. Fuel and cell outer radii were 0.318 cm and 0.505 cm, respectively.

Calculations were done for a series of U-238 resonances between 1.0 and 1.3 keV, using the Firk, Lynn, and Moxon parameters. Resonances with neutron widths less than 0.01 eV were excluded because of the weakness of the self-shielding of such resonances. Values of $\bar{a}$ for $C = 0$, the isolated-rod case, are given in Table 1, and are seen to lie between 1.19 and 1.53, except for one value at 1.59. The former two values of $\bar{a}$ seem to be reasonable lower and upper limits according to the curve of $\bar{a}$ versus optical chord length in the fuel presented by Otter.⁽³⁾ The precision of the determination of $\bar{a}$ at small neutron widths is not very high.

Calculations were also made with Dancoff interaction present and varied by varying the assumed sodium scattering cross section. Calculations were made for σ_s of steel assumed constant at 10 b and σ_s of sodium assumed to be 10, 20, 30, 60, and 100 b, corresponding to values of C of 0.758, 0.663, 0.584, 0.494, and 0.276,

FIG. 1. Values of the Dancoff Factor for Various Approximations.

TABLE 1. ISOLATED-ROD VALUES OF $\bar{a}$

Resonance Energy, keV	Γ_n , eV	$\bar{a}$
1.328	0.010	1.50
1.281	0.027	1.53
1.276	0.025	1.46
1.255	0.236	1.21
1.221	0.014	1.53
1.204	0.057	1.42
1.186	0.030	1.44
1.178	0.043	1.41
1.149	0.274	1.19
1.130	0.010	1.59
1.061	0.051	1.39
1.029	0.019	1.53

respectively. It was found that for σ_s of 10 and 20 b, the equivalent σ_p obtained by the relations given here were accurate to 1 or 2 b out of totals of 48 and 56 b, respectively, using $\bar{a} = 1.35$. At smaller values of C , however, greater disparities appeared. Results at $\sigma_s = 30$ and 100 b are shown in Figs. 2 and 3, with inferred σ_p values for individual resonances plotted against neutron width. It is found in these cases that σ_p is slightly larger than expected and indeed at small neutron widths is larger than would correspond to a possible value of $\bar{a}$. Not shown are results for $\sigma_s = 60$ b. In this case apparent σ_p values, which were expected to be about 70 to 76 b, were about 13 b, a slightly larger discrepancy than in the cases for 30 and 100 b. It is not obvious why this should be, and it may be due to an undetected error. In any event, the errors in the equivalence relations indicated here are of small consequence for fast reactor calculations.

Comparisons were made at 300°K and at 750°K. At the higher temperature the inferred σ_p was usually slightly larger than at the lower one, the difference being 1 to 2 b. This indicates that the use of the equivalence relations leads to an underestimation of the Doppler effect by several percent, which is not a significant error.

While the results here indicate that equivalence theory is satisfactory for U-238 resonances in the energy range considered, it is still possible that significant errors could occur at lower energy, where resonances are stronger and the flat-flux approximation is less realistic.

II. Effect of Parameter Variation on Sodium Void Effect

We consider the effect of variations in the fission cross section of Pu²³⁹ and the capture cross section of U²³⁸ from the reference values used in the comparison calculations done for this Conference,⁽¹¹⁾ referred to as

the Set 232 values and given in Table 2. These cross sections were identical with those of Set 224⁽¹²⁾ above 25 keV.

σ_f of Pu²³⁹

We have found that, particularly for a ceramic-fueled system, the value of σ_f of Pu²³⁹ at quite low energies, in the range 1 to 3 keV, can be significant for the sodium void effect. Although there is not much flux at such low energies, the energy variation of the adjoint function can be significantly affected at higher energies by events occurring in the range from 1 to 3 keV because of the elastic-transfer coupling term in the adjoint function. This is a significant source of uncertainty because of the large uncertainty in σ_f of Pu²³⁹ in this energy range. We have considered an alternative set of values to those of Set 232, based on data presented by Hemmendinger⁽¹³⁾ and based on Los Alamos bomb test measurements (see Table 2). Since these data are

FIG. 2. Values of σ_p as a Function of Γ_n of the Resonance (for σ_s of Sodium Equal to 30 b).

FIG. 3. Values of σ_p as a Function of Γ_n of the Resonance (for σ_s of Sodium Equal to 100 b).

TABLE 2. CROSS-SECTION VARIATIONS STUDIED

J	E_L	$U^{238} \sigma_f^{(a)} \text{ at } 1500^\circ\text{K}; \sigma_p = 40 \text{ b}$				$Pu^{239} \sigma_f^{(b)} \text{ at } 1500^\circ\text{K}; \sigma_p = 300 \text{ b}$					
		Reference Set 232	Set 215 Gps. 5-11	Set 215 Gps. 15-21 BNL-325	Set 215 Gps. 15-21 FLM	Reference Value Set 232	White	Russian	Los Alamos Bomb Test	High Low Energy Value	
										1	2
1	3.68 MeV	0.0091				1.90					
2	2.23	0.0231				1.95					
3	1.35	0.0588				1.97					
4	0.821	0.128				1.82		1.7			
5	0.498	0.130	0.139			1.64		1.55			
6	0.302	0.116	0.127			1.57		1.5			
7	0.183	0.140	0.160			1.56		1.6			
8	0.111	0.191	0.240			1.60	1.56	1.65			
9	0.0673	0.259	0.310			1.73	1.50	1.65			
10	0.0409	0.335	0.420			1.89	1.45	1.7			
11	0.0248	0.407	0.450			1.89	1.70	1.7			
12	0.015	0.514				1.90		1.8			
13	0.12 keV	0.640				1.88		2.1			
14	4.31	0.784				2.34		2.5	2.5		
15	2.61	0.677		0.907	0.907	3.03		3.1	3.5		
16	2.03	0.735		1.115	1.115	3.73		3.4	3.0		
17	1.23	0.792		1.112	0.982	4.47		4.0	5.0		
18	0.96	1.062		1.242	1.162	5.34			6.5		
19	0.58	1.288		1.268	1.138	6.27				6.9	6.9
20	0.29	1.238		1.308	0.898	8.23				9.2	11
21	0.101	1.729		1.599	1.589	11.67				12.7	15
22	0.029	2.211				11.83				23	29

(a) Calculated values include flux correction factor.

(b) α was kept constant in σ_f variations.

preliminary and since resonance parameters are not available, the values inferred here are not to be taken too seriously. The question of self-shielding has been ignored here, but should be small for Pu^{239} in the energy range above 1 keV. The Los Alamos values are generally higher than those for Set 232 except in the case of Group 16 by from 2.0 to 2.6 keV.

At still lower energies, below 1 keV, the measured cross section as shown in BNL-325⁽¹⁴⁾ suggests that the Set 232 values of σ_f for Pu^{239} , which were based on a statistical distribution of parameters above Group 22, may be too low. Calculations with parameters recently obtained from Harwell⁽¹⁵⁾ give a value of around 14-15 b at 1500°K and of $\sigma_p = 300 \text{ b}$ for Group 21. The σ_f cross sections for Pu^{239} in the Bondarenko book⁽¹⁶⁾ are also higher. We have therefore tried two alternative sets of higher low-energy values. The Bondarenko values appear to fall in between these, but a careful interpolation with respect to energy, temperature, and σ_p has not been done.

Another variation made in σ_f of Pu^{239} corresponds to recent data of P. White,⁽¹⁷⁾ which indicate considerably lower values in the energy range from 25 to 200 keV than were used in Set 232.

Still another variation is the use of Russian data⁽¹⁸⁾ for the σ_f of Pu^{239} . These values are not greatly different from those of Set 232, but the statistical uncertainty is rather large.

σ_f of U^{238}

The values assumed for Set 232 (and for Set 224) are lower than most of the data in BNL-325 in the energy range 25-300 keV because of a desire to give some weight to the data of Moxon.⁽¹⁹⁾ The effect of use of values from Set 215, used in calculations presented at the October, 1963 conference at Argonne,⁽²⁰⁾ was studied. Cross sections in this set are higher than those of Set 232 in the energy region 25-800 keV (Groups 5-11) and are close to the curve in BNL-325. The Set 215 cross sections are also higher below 4 keV, as a consequence of using the BNL-325 parameters and unresolved region parameters corresponding to them, whereas the Set 232 values use the new Columbia resonance parameters.⁽²¹⁾ This was also studied as a variation for Groups 15-21. Another variation was to use the Set 215 cross sections for Groups 15-21, but with the Firk, Lynn, and Moxon parameters⁽²²⁾ for Groups 17-21, below 2.0 keV.

TABLE 3. EFFECT OF PARAMETER VARIATIONS ON SODIUM VOID EFFECT (CASE 9)^(a); FULL SODIUM FLUX

		Components for 50% Voiding (Full Na Flux)			
		Elastic	Inelastic	Capture	Spectral plus Capture
Reference		1.88	1.02	0.17	3.07
Pu ²³⁹ σ_f Variations	Los Alamos	1.83	1.01	0.17	3.01
	White	1.97	1.11	0.18	3.26
	Dubrovina	2.00	1.03	0.17	3.20
	High values (1)	1.72	1.01	0.17	2.90
	Groups 19-22 (2)	1.62	1.00	0.17	2.79
U ²³⁸ σ_γ Variations	Set 215 Groups 5-11	1.87	1.08	0.17	3.12
	Set 215 Groups 15-21 BNL-325	2.08	1.04	0.16	3.28
	Set 215 Groups 15-21 FLM	1.93	1.03	0.16	3.13

^(a) Atoms/cc $\times 10^{-21}$: Na: 0.0123; Pu²³⁹: 0.0009; U²³⁸: 0.0063; Pu²⁴⁰: 0.00045; Stainless Steel: 0.011; Oxygen: 0.0144; Fission Product Pairs: 0.00036.

Results of Parameter Variation (see Table 3)

σ_f for Pu²³⁹. The use of the low-energy cross sections inferred from the Los Alamos bomb test experiment reduced the positive spectral component slightly. This was caused by the fact that the varied cross section was mainly above the original one. This resulted in a reduction in the energy variation of the adjoint function. The higher values at still lower energy have also resulted in less positive void effects for the same reason.

The use of the new White data, corresponding to a lower fission cross section in the range from 25 to 200 keV, has resulted in a more positive effect as expected. The data of Dubrovina and Shigin, which also lie generally below the Set 232 values, also give more positive results because of a more rapid variation of the adjoint function.

σ_γ for U²³⁸. Although none of the variations had a very important effect, the use of the higher cross sections at lower energy causes the void effect to be more positive, because the energy variation of the adjoint function becomes more rapid. This effect is less pronounced with the Firk, Lynn, and Moxon cross sections, as they give lower cross sections than do the BNL-325 parameters.

III. Effect of Presence of Pu²⁴¹ on Sodium Void Effect

It is expected that the substitution of Pu²⁴¹ for Pu²³⁹ will result in a more negative sodium void effect for

TABLE 4. SPECTRAL PLUS CAPTURE δk FOR 50% SODIUM VOIDING
10% of Pu²³⁹ Replaced with Pu²⁴¹; Full Sodium Flux

Fertile to Fissile Ratio	Case	% δk	
		Original	Pu ²⁴¹ Present
9.5	8	4.02	3.20
7.5	9	3.07	2.41
5.5	10	2.07	1.51

the same ratio of fertile to fissile atoms because the Pu²⁴¹ fission cross section varies more rapidly with energy than that of Pu²³⁹. This has been studied for Cases 8, 9, and 10 of the comparison calculations for this Meeting.⁽¹¹⁾ These are oxide-fueled compositions with ratios of fertile to fissile atoms as indicated in Table 4. The composition is otherwise as indicated in Table 3, the total fuel atoms/cc remaining the same. Ten percent of the Pu²³⁹ was replaced with Pu²⁴¹, the Pu²⁴¹ cross sections being given in Ref. 11. Fuel-cycle studies indicate that equilibrium Pu²⁴¹ contents are generally in the range from 7 to 10 % of the Pu²³⁹ content.

The results, given in Table 4, show that, as expected, the void effect is considerably less positive with Pu²⁴¹ present. Note that this comparison is for a fixed fertile to fissile ratio and takes no account of a possible change in critical enrichment for a given reactor size as a result of the substitution.

IV. Nonlinearity in Spectral Component as a Function of Sodium Content

It has been found that nonlinearity is due primarily to variations in the energy dependence of the adjoint function, and hardly at all to variation of effective sodium cross section or flux, except for events occurring in the vicinity of the 2.85-keV sodium resonance. Nonlinearities of the adjoint function tend to be associated with local variations in energy of η , the fission neutrons produced per neutron captured. When strong moderation is present, these variations are smoothed out and do not cause corresponding variations in the adjoint function. In the limit of no moderation, the adjoint function becomes equal to η , aside from the effect of leakage. As sodium is removed and moderation is

reduced, therefore, any local variations in η tend to appear in the adjoint function. This is not very marked with oxide systems, as strong moderation is still present. With a metal system rather strong effects can occur.

The example which is given in the following is admittedly a rather extreme one in that the ratio of sodium to fuel is rather high. It is taken from a report to be published by Singh and Hummel⁽²³⁾ on reactivity coefficients in U^{233} - Th^{232} -fueled systems. Results were obtained from the use cross-section Sets 215 and 224. The essential difference between the two for the purpose of the present discussion is in the assumed energy variation of the Th^{232} capture cross section (see Fig. 4). The basis for the curve for Set 224 is reported in Ref. 12. The lower curve, for Set 215, favors data of Moxon.⁽¹⁹⁾ As a result of the dip in the cross section below 0.1 MeV, the adjoint function for the metal-fueled case treated by Set 215 shows a rise just below 0.1 MeV, which is barely perceptible for the full sodium case (see Fig. 5) and quite strong for the no-sodium case (see Fig. 6). This effect is suppressed for the Set 215 oxide case, and not present at all for the Set 224 metal case. The consequences for the elastic removal component of the sodium void effect may be seen in Table 5. Although the variations in the adjoint function which occur in the region of greatest flux amount to only a few percent, they correspond to large changes in the sodium void effect. The results in Table 5 were obtained by perturbation theory and correspond to 50 % sodium voiding using the indicated flux. The nonlinearity for the Set 215 case is very strong; that in the Set 224 metal case is much less. Peculiarity in the behavior of the void effect in the oxide cases is due mostly to events occur-

FIG. 4. Assumed Energy Variation of σ_γ of Th^{232} for Cross-section Sets 215 and 224.

FIG. 5. Adjoint Function as a Function of Energy in a U^{233} - Th System; Fuel Sodium.

FIG. 6. Adjoint Function as a Function of Energy in a U^{233} -Th System; No Sodium.TABLE 5. ELASTIC REMOVAL COMPONENT FOR 50% VOIDING, $\% \Delta k$

$$\frac{Th^{232} + U^{234}}{U^{233}} = 8.0; 70 \text{ v/o Na}; 1500^\circ K$$

Flux Used, % Sodium	100	50	10	0
Set 215 Metal	1.05	0.85	0.03 (0.01) ^(a)	-0.26 (0.23) ^(a)
Oxide	0.47	1.15 (0.04)	2.18 (0.47) ^(a)	3.71 (2.62) ^(a)
Set 224 Metal	1.09	1.08		0.99 (0.41) ^(a)
Oxide	1.13	1.74		5.14 (3.52) ^(a)

^(a) Contribution from Group 15 (2.8–4.0 keV).

ring in the vicinity of the 2.85-keV sodium resonance. The contribution to δk in this region is indicated in parentheses in Table 5. The large effect at zero sodium flux is due to a failure in perturbation theory, since the unperturbed flux is being multiplied by a large sodium transfer cross section. As sodium is introduced, a flux depression appears in the vicinity of the resonance and the effect is quickly reduced.

The results given here suggest that interesting effects may be observed in some cases by looking for nonlinearities in sodium void effect in different systems for various degrees of moderation. Useful information about the energy variation of both the Th^{232} and the

U^{235} capture cross sections could conceivably be obtained.

Acknowledgment

The authors wish to thank E. Pennington for performing some of the calculations with his collision probability code, and P. Kier for assistance in the use and modification of his code for integration of the Boltzmann equation.

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A Comment on the Comparison of Some Sodium Void Coefficients with Experimental Data

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The difficulties which are encountered in the evaluation of the sodium void coefficient in fast reactors may be considered to belong, in general, to one of two different categories. The first includes all the problems involved in the determination of the correct spatial behavior of the real and adjoint fluxes in the reactor, which often requires complex calculations to take into account transport effects, inhomogeneities, anisotropy, irregular geometry, and two- or three-dimensional effects. The second category includes the determination of a suitable set of cross sections to be used in the calculations. The combination of all these sources of uncertainty may cause a considerable inaccuracy in the calculations, and the disagreement which may be found among the values calculated by different authors for the sodium void coefficient of similar reactors has been put well in evidence by Okrent.⁽¹⁾

One logical procedure intended to improve the accuracy of the calculations is to face separately the different causes of inaccuracy, and it is with this consideration in mind that a set of calculations has been made on the variation of the sodium void coefficient when different cross-section sets are used. The purpose of these calculations was to compare the results with experimental data in such a way that a criterion could be at hand, however modest, to assign a figure of merit to each cross-section set.

The reactor considered in the calculations was chosen to be similar in shape and composition to Assembly 45A of ZPR-3.⁽²⁾ It is sufficient here to say that it is a zoned reactor, cylindrical in shape, and that it consists of a reflector, of a driver, of a filter, and of a central zone. The composition of the central zone is such that for every atom of plutonium-239 there are, in the central zone, approximately six atoms of uranium-238, eight atoms of sodium, four atoms of graphite, and twelve atoms of stainless steel. Three different sets of cross sections were used in the calculations. The first and the second set were, respectively, the Argonne Set 224 and the Argonne Set 232.⁽³⁾ The third set was the Argonne Set 224 with a modification introduced by D. K. Butler to include the White cross sections for

plutonium-239 and for uranium-235; we shall refer to this set as to the Set 224 + W.

The parameter which was calculated with each set of cross sections, and compared with the experimental data, was the value of the central sodium worth. This value was chosen for the comparison, rather than the value of a distributed sodium worth, because at the center of the reactor the leakage component of the sodium worth vanishes, and this decreases the effect of the uncertainty involved in the determination of the spatial behavior of the fluxes.

The value of the central sodium worth in a critical reactor is given, in perturbation theory and in a concise matrix notation, by the following expression:

$$\text{Sodium Worth} = \frac{\langle \phi^+ | \sigma_s^{\text{Na}} - \sigma_{so}^{\text{Na}} - \sigma_c^{\text{Na}} | \phi \rangle_0}{\langle \phi^+ | \chi \rangle_0 \langle \nu \Sigma_f | \phi \rangle_0} \cdot \frac{\langle \phi^+ | \chi \rangle_0 \langle \nu \Sigma_f | \phi \rangle_0}{\int_V \langle \phi^+ | \chi \rangle \langle \nu \Sigma_f | \phi \rangle d\mathbf{r}} C, \quad (1)$$

where $\langle \phi^+ |$ and $| \phi \rangle$ are, respectively, the adjoint and the real fluxes; $[\sigma_s^{\text{Na}}]$, $[\sigma_{so}^{\text{Na}}]$, and $[\sigma_c^{\text{Na}}]$ are the total microscopic scattering cross sections, the microscopic scattering kernel, and the microscopic capture cross section, of sodium; $\langle \nu |$, $| \chi \rangle$, $[\Sigma_f]$, and V are the average number of neutrons produced in a fission, the fission spectrum, the macroscopic fission cross section of the core, and the volume of the core. The subscript 0 refers to quantities evaluated at the center of the reactor, and C is equal to the product of the number of atoms which are present in a kilogram of sodium times the number of inhours per unit excess reactivity.

If the central zone is large enough for the fundamental mode to be established at the center of the reactor, the first factor on the right hand side of Eq. (1) may be considered very close to the result of a fundamental mode calculation, and it is almost independent on the accuracy with which the spatial dependence of $\langle \phi^+ |$ and $| \phi \rangle$ is evaluated. The second factor, on the other hand, does not depend of the material whose worth is being calculated. Thus, the calculated ratio of the central worths of two different materials should be

TABLE 1. MATERIAL WORTHS IN ASSEMBLY 45A

	Set 224	Set 232	Set 224 + W	Experimental
Central Worths (lb/kg)				
Na	-4.76	-5.96	-5.89	-4.7 ± 0.4
Pu Fuel	188.1	199.2	174.1	176 ± 1.0
Depleted U	-8.47	-9.35	-8.66	-7.6 ± 0.1
Central Worth Ratios				
Na/Pu Fuel	0.0253	0.0299	0.0338	0.0267 ± 0.0024
Na/Depleted U	0.562	0.637	0.681	0.618 ± 0.061
Na Void Coefficient*	0.601	0.717	0.805	(0.662)

* Fundamental Mode Calculations ($\% \Delta k/k$ for 50% sodium removal).

almost independent of the spatial variation of the fluxes, and its comparison with the experimental data should provide a more significant indication of the reliability of the cross-section set which is being employed.

The calculations were performed by perturbation theory in MACH-1, a one-dimensional diffusion code developed by D. A. Meneley. In view of the previous considerations, it was believed that the limitations of diffusion theory would not affect the results for the worth ratios to any appreciable extent. Table 1 shows the calculated results for the worths of sodium, of plutonium fuel (95.4% Pu^{239} and 4.6% Pu^{240}), and of depleted uranium (99.794% U^{238} and 0.206% U^{235}), with their ratios, and the corresponding experimental values.⁽⁴⁾

The agreement between calculated and experimental values is remarkably good with all the cross-section sets, both for the absolute values of the worths and for their ratios. From the comparison of the worth ratios we may conclude that the experimental results fall in between the results that have been calculated with the Argonne cross-section Sets 224 and 232, which rate almost equally well, with Set 224 + W coming up last. It should be noted, however, that all three cross-section sets come through the test quite satisfactorily, and, in effect, all almost within the experimental error. Thus, the result of the calculations should be considered an indication of the relative worth of the different cross-section sets, rather than a final conclusion.

The last line of Table 1 gives the results of a fundamental-mode calculation of the sodium void coefficient

for a 50% sodium removal throughout the core, as obtained from the evaluation of the first factor on the right-hand side of Eq. (1), and, by interpolation, what may be considered as an experimental result for a fundamental-mode sodium void coefficient. This result would be very close to the spectral-plus-capture components for uniform voiding of a reflected reactor with a core composition equal to the composition of the central zone, since the core size would be large and the effect of the reflector on the core spectrum would not be important. These values are given for orientation purposes, and show that the reactivity differences between theory and experiment are not as serious as one might have feared, considering the uncertainties in relevant parameters.

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Discussion

Mr. Küsters (Karlsruhe): I would like to make a comment. I agree that the uncertainties in the microscopic cross sections for the sodium void effect may be not so severe as the uncertainty in the calculational methods. I remind you of the comment Dr. Häfele gave yesterday in the Panel Discussion. He said that we have found that in using or in calculating the sodium void for a two-dimensional reactor with one-dimensional calculations with suitable buckling, we got a positive effect; when we made two-dimensional calculations, we got a negative effect. The difference was about \$3. I would say that the sodium void is much more sensitive to the calculational methods than to the cross sections.

Mr. Hummel (ANL): I think that if you look at the spectral or leakage components separately, the spectral component is probably not overly sensitive to the details of the geometry. This is what really does the damage. Admittedly the leakage component gives you trouble, but this is something I think we can certainly work up to. I think it would be helpful to separate these two parts of the void effect when you discuss the problem.

The Influence of Some Important Group Constants on Integral Fast Reactor Quantities*

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I. Introduction

It is well known that for large fast reactors the calculation of critical mass and safety coefficients is possible only to an unsatisfactory degree of accuracy. One of the main reasons comes from the fact that the group constants used in the multigroup calculations are uncertain. The objects of this paper are to find the importance of some uncertainties and the basis for further improvements in the calculational methods for dilute fast reactors. For this purpose it is useful to distinguish between two sources of uncertainties in group constants: those arising from the weighting spectrum and those from uncertainties in the microscopic data.

In the first part of this paper the effect of spectral fine structure with respect to resonance self-shielding and the influence of the macroscopic weighting spectrum is discussed. It is investigated how reactivity coefficients like the Doppler coefficient (DC), sodium density coefficients, or breeding ratios, important for economical considerations, are influenced by different weighting spectra, which are used in various sets of group constants.

In the second part the effect of some uncertain microscopic data with respect to the calculation of dilute fast reactors is investigated. The aim is to have a better knowledge of the importance of these group constants in fast reactor calculations. Some of these effects are separated during the preparation of the first Karlsruhe group-constant set KFK 26-10, which is briefly described here.

The method of calculating the DC used here is based on perturbation theory.⁽¹⁾ Sodium void or loss is calculated in two criticality calculations. The resonance self-shielding is treated in narrow resonance approximation and is determined in advance of each reactor calculation. This is the procedure described in the Russian group-constant set.⁽³⁾

II. Methodical Questions

1. The Effect of Resonance Self-shielding

The theoretical investigation of fast reactors was first made for relatively small assemblies with hard spectra. A large number of these facilities have been calculated with good results in critical masses with the unshielded group-constant set "YOM" given by S. Yiftah, D. Okrent, and P. A. Moldauer and by S. Yiftah, M. Sieger ("YOM-64").⁽²⁾

For dilute fast reactors there is an appreciable amount of neutrons below 10 keV and the effect of resonance self-shielding should be remarkable. Nevertheless, the YOM set gives good results in criticality for these large reactors (see Table 6 below, for instance).

To have a better understanding of the shielding effect on neutron multiplication, we made a series of fundamental-mode calculations for reactors of different sizes and compositions with the shielded and unshielded Russian "ABN" set of I. I. Bondarenko *et al.*⁽³⁾ The result is shown in Fig. 1. The ordinate is the difference of the shielded ($k_{\text{eff},s}$) and unshielded calculations. All reactors are made critical with buckling iteration so that $k_{\text{eff},s} = 1$ always. The abscissa is the escape probability of neutrons for the system, defined by $P = 1 - (k_{\text{eff},s}/k_{\infty,s})$.

For large fast reactors of about 7000-liter core volume there is a loss in reactivity of about 6% if the calculation is made with unshielded cross sections. This reactivity loss is mainly due to a decrease in k_{∞} . For small cores with high escape probability, the neutron balance is made up mainly of leaking neutrons and fissioning neutrons. In these cores with hard spectra only the resonances of structural materials like iron and nickel contribute to the shielding effect. However, these resonances are almost scattering resonances, and therefore the use of unshielded scattering cross sections results in increasing transport cross sections (higher by a factor of 2 near the iron resonance at 40 keV) and in lowering of leakage. The fundamental-mode calculation yields a gain of about 8% in reactivity for the core composition

* This paper has been prepared within the framework of the association EURATOM Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

FIG. 1. The Effect of Resonance Self-shielding for Some Reactors.

$k_{eff,s}$ is the multiplication factor for the shielded calculations ($k_{eff,s} = 1$), $k_{eff,us}$ for the unshielded calculations. P is the escape probability of neutrons for the system.

of the ZPR III/33 assembly. Naturally, the effect is overestimated by this calculation, but the general trend should be correct. A one-dimensional check for ZPR III/33 gives +2.7 % reactivity gain. For the large cores the fundamental-mode calculations give very good results. The dashed line in Fig. 1 gives the probable tendency.

In a similar way as does the multiplication constant, the resonance self-shielding affects the neutron lifetimes: an increase for small and a decrease of about 30 % for large fast reactors, if no shielding corrections are made. The contribution to the average lifetime from below 10 keV is about 40 %. Spectra for large reactors calculated with shielding corrections are increased in the low-energy range, and any DC calculation made with "unshielded spectra" is meaningless (see ref. 1).

It is assumed that the complex problem of resonance self-shielding may be treated with an accuracy of about 20 %. Therefore, the uncertainty caused by the effect of resonance shielding is about 6 % for the determination

of neutron lifetimes and about 1 % in the multiplication constant. This situation makes it very clear that continued attention should be paid to this problem. Especially the resonance data should be more accurate. Also, the proper shielding of the transport cross section has to be studied. This is important for the scattering resonances of the structural materials, where the narrow-resonance approximation or even a fine-group representation as used in ELMOE are not the suitable methods.

2. The Influence of the Weighting Spectrum

Many of the group-constant sets in use for the calculation of large fast reactors contain about 30 groups up to 10 MeV. The use of such a group number with most computers gives a reasonable machine time.

As a basis for our investigation, we chose the boundaries of the Russian ABN set. The question of whether such a group width is sufficiently small with respect to the weighting spectrum was first investigated by calculating group constants for the weighting spectra, $1/E$, YOM-weighting flux (see ref. 2) and a spectrum (KFK) of a sodium-cooled, 1,000-MWe reactor. The microscopic data were taken from the Karlsruhe Nuclear Data File KEDAK.⁽⁴⁾

As a typical example the group constants for fission of Pu^{239} are shown in Fig. 2. There are no essential differences in the most interesting energy region for fast reactors. Only in the region of resolved resonances below 100 eV are there discrepancies up to 50 %. Here only few resonances are located in one group, and therefore the position of group boundaries and the shape of the weighting spectra are important. From this it follows directly that the high and relatively broad resonances of sodium and the structural materials acquire special interest. These scattering resonances enter into the diffusion calculation besides the determination of resonance shielding only in the elastic-removal and transport cross sections.

In this paper we will discuss only the effect of the weighting spectrum on the elastic-removal group constants. A detailed investigation about the transport cross section, especially in connection with the average scattering cosine, is under way.

Neutrons are removed from a group by elastic scattering with an isotope of mass A only out of the degradation interval to the extent E_L/α , where E_L is the lower group boundary and $\alpha = \left(\frac{A-1}{A+1}\right)^2$. Since α is near unity for isotopes in a sodium-cooled fast reactor ($\alpha = 0.78$ for O^{16}), the degradation interval is mostly small compared with the width of the group. As a first step we took the coarse group structure of the ABN set and investigated only the degradation interval with the appropriate shielding in the narrow-resonance approxima-

Fig. 2. σ_f for Pu^{239} in ABN Group Boundaries for Different Weighting Spectra.

tion. The anisotropic scattering in the center-of-mass system for high energies is considered as an isotropic scattering with a pseudo-material by averaging the scattering cosine of each material over the degradation interval to which an energy loss, characterizing the pseudo-material, is connected. For constant scattering cross section and fixed average spectrum in a group, a decreasing weighting spectrum with increasing energy will give higher elastic-removal cross sections than increasing spectra. In reactors with oxide or carbide fuel the effect may be severe.

In order to study the effect of different weighting spectra, we replaced in the ABN set only the elastic-removal group constants, because other group constants are not sensitive to the weighting and therefore remain unchanged. With KEDAK data we calculated the removal constants for the 9 materials C, O, Na, Cr, Fe, Ni, U^{235} , U^{238} , and Pu^{239} . In Fig. 3 are shown the YOM and KFK weighting spectra in lethargy representation, in Fig. 4 the corresponding elastic-removal group constants for O¹⁶.

The differences are remarkable and should have a great influence on integral reactor quantities. For a dilute fast test reactor we calculated Doppler coefficients $[(1/k)(dk/dT)]$ related to 900°K, the loss of 40 % sodium from the reactor core, neutron lifetimes, and breeding ratios (see Table 1). The core composition is as follows: Cr, 2.56 v/o; Fe, 9.52 v/o; Na, 55.9 v/o; Ni, 0.9 v/o; O, 15 v/o; Pu^{239} , 1.5 v/o; U^{238} , 13.5 v/o.

The group sets K1 and K2 were based on the same

microscopic data and are different only in the weighting spectra (different elastic-removal constants). The sets K1 and ABN have the same weighting spectrum ($1/E + \text{fission spectrum}$) in the first three groups, but K1 elastic-removal group constants were calculated with KEDAK data. As compared with ABN or YOM, the different cross sections and procedure in calculating the elastic moderation gave 2 % difference in the multiplication constant, and 0.3 % for the loss of 40 % sodium (using the same group set for the original and voided reactor, but with new determination of the shielding factors for removal of neutrons). The Doppler coefficient decreased by about 16 %. The difference in neutron multiplication arose from differences in the removal cross sections above 30 keV only. The breeding ratios are practically not influenced. It should be noted here that the breeding ratios and DC belong to the reactors which are made critical by variation of particle number densities of Pu^{239} and U^{238} ($N_9 + N_8 = \text{const.}$). The YOM weighting in K2 gave an increase in k_{eff} by about 0.6 % compared with K1, but the DC was lowered by a factor of about two. This results from the spectral shift to higher energies because below 100 keV the YOM weighting gives lower elastic removal than $1/E$ -weighting. Therefore we have less neutrons in the resonance region. The lifetime data reproduced the spectral shifts and showed the same tendency as the DC variation.

The KFK set is a completely new group set in which all microscopic data were taken from the KEDAK file. This set is briefly described later on; we compare the

FIG. 3. Weighting Spectra Used for Elastic-removal Compilation.

FIG. 4. Elastic-removal Group Constants for the Weighting Spectra Shown in Fig. 3.

KFK and other sets in Table 6 below. We see that despite great differences in microscopic data the breeding ratios are nearly unchanged. The sodium coefficients for the various Karlsruhe sets are not very different. The increase of 0.6% compared with ABN results partly (0.3%) from the cross sections used (ABN-K1).

The other 0.3% is due to the weighting spectrum. Figures 3 and 4 show that in the range from 0 to 100 keV the elastic-removal constants are smaller in KFK than with $1/E$ -weighting; above 100 keV these constants are larger so as to result in a spectral shift to lower energies. However, one has to be careful in speaking about a

TABLE 1. THE RESULTS FOR A 7850-LITER CORE OF A PLUTONIUM OXIDE BREEDER WITH DIFFERENT GROUP SETS

Group Set*	Critical Mass, kg Pu ²³⁹	Δk (%) for M = 2242 kg	Δk (%) for 40% Sodium Loss	Doppler Coeffi- cient ($\times 10^5$)	Lifetime ($\times 10^7$ sec)	Internal Breeding Ratio	Breeding Ratio
ABN	2242	0	0.52	-1.88	6.37	1.04	1.43
KFK	2420	-4.5	1.1	-1.45	5.80	1.06	1.45
K1	2340	-2.15	0.83	-1.56	5.95	1.06	1.44
K2	2296	-1.48	1.17	-0.81	4.82	1.10	1.52

* K1: σ_{er} with $1/E$ -weighting (KFK-data). K2: σ_{er} with YOM-weighting flux. KFK 26-10: 1,000-MWe spectrum; all group constants are KFK data.

FIG. 5. Comparison of Four Group-constant Sets for the Capture Constants of Fe.

“shift” of the spectrum, because below 10 keV there are less neutrons than with $1/E$ -weighting, more neutrons naturally than with YOM-flux. The DC is therefore smaller than for $1/E$ -weighting and for ABN (20 %).

If one calculates the spectrum of a reactor without sodium with a group set that includes the dip at the 3-keV sodium resonance in its weighting spectrum, one finds also a clear dip in the calculated spectrum at the sodium resonance. This dip mainly results from the elastic-removal group constants of the light elements, all of which have a peak below and a minimum above the resonance. We removed the “psuedo-dip” and used this spectrum as a weighting for new elastic-removal cross sections to build a new group set, KFK 26-11, which was used for calculating total coolant loss. We calculated the total loss, Δk , and obtained

$$\Delta k_{\text{eff}} (\text{KFK 26-10}) = 0.029 \quad (\text{same group set});$$

$$k_{\text{eff}} (\text{KFK 26-10}) - k_{\text{eff}} (\text{KFK 26-11}) = 0.025.$$

The loss Δk is decreased by about 0.5 % if one takes the corresponding system group sets. It should be noted that the spectra should have been converged if such small changes are expected.

We see that the weighting spectrum has an important influence on integral fast reactor quantities by the process of elastic moderation.

In a second step we are investigating whether the method of calculating the elastic moderation, briefly described above, is sufficient. An additional comparison with ELMOE calculations on an IBM 7090 computer was prepared. This code cannot be used on the Karlsruhe IBM 7074 because of its small storage capacity.

FIG. 6. Comparison of Four Group-constant Sets for the Capture Constants of Ni.

We can draw a further conclusion from the results discussed above. Multizoned cores and blankets should not be calculated by using one group set prepared with a core or an average reactor spectrum as a weighting only. To be more accurate, one needs at least two group sets, one for core materials and one for blanket materials.

III. The Karlsruhe Group Set KFK 26-10

Most of the discussion on changes in neutron multiplication arising from differences in microscopic data is done in chapter IV by comparing the results of the Karlsruhe group set KFK 26-10 with those of the ABN set. Therefore it is necessary to give a short description of the characteristics of the KFK set.

1. Comparison of Microscopic Data

This part is mainly based on a comparison of group constants.⁽⁶⁾ In Figures 5, 6, and 7 the capture data of several authors for Fe, Ni, and U^{238} are compared. The boundaries of the 9 groups are shown as intersections on the abscissa. They are chosen in such a way as to coincide optimally with the boundaries of the different sets. The group constants HR of Hansen and Roach,⁽⁶⁾

YOM, and ABN are condensed into these groups by means of the corresponding weighting fluxes. The Karlsruhe data KFK are taken from the KEDAK file and are revised data.⁽⁷⁾ $1/E$ -weighting was used in order to provide a direct comparison with ABN and HR.

For Fe^{56} large discrepancies exist for the capture group constants in the region above 500 eV. The KFK values, which are based on recent Harwell and Oak Ridge measurements,⁽⁷⁾ are higher by a factor of about 2 than the ABN data up to 100 keV. YOM and HR deviate from KFK by a factor of about 10 between 0.2 and 1.6 keV. For higher energies there is better agreement.

The situation is only a little better for Ni. The KFK resonance capture cross sections are based on capture widths estimated from the thermal isotopic capture cross sections.⁽⁷⁾ These data are higher by a factor of about 2 than the other data between 0.5 and 1.6 keV.

For U^{238} in Fig. 7 the agreement in the capture constants is good. The KFK data above 130 keV are based on recent Harwell measurements.

In Fig. 8 the inelastic cross section of Pu^{239} is shown. The abscissa now is the lethargy u . Below 2 MeV the data of the ANL⁽⁸⁾ and ABN sets are different by a factor of about 10; KFK values coincide with YOM

FIG. 7. Comparison of Four Group-constant Sets for the Capture Constants of U^{238} .

data, which are lower by a factor of about 2 compared with ABN.

For Pu^{239} the KFK data for fission are lower by about 20 % (recent Russian and British measurements) between 10 and 50 keV than ABN. The α -value remained unchanged in this region.

2. Preparation of the KFK Set

The group set was prepared with the Karlsruhe MIGROS-system, which combines several codes. For a prescribed weighting spectrum, the unshielded group constants were calculated from data in the KEDAK file. Elastic-removal constants were determined as described in chapter II.

The KEDAK file now contains data only for the materials H, He, C, O^{16} , Na, Cr, Fe, Ni, Mo, U^{235} , U^{238} , and Pu^{239} . In order to have a very complete set, we took the ABN set as a basis and replaced the unshielded group constants of the nine materials by KFK data. Calculations for the test reactor in chapter II were therefore made with KFK constants only. The spatially averaged spectrum of the Karlsruhe Na-1 sodium-cooled reference design reactor was used as a weighting. Since the different codes in the block for calculating resonance shielding in MIGROS had not yet been fully combined, and because calculations mostly showed good

agreement with the Russian shielding factors, we chose as a first step the Russian ones with only slight modifications for the removal values. We have 26 groups; the group boundaries are given in the appendix. Using the same microscopic data and calculational methods, exchanging only the spectrum, we then obtained the set KFK 26-11, which was used for the calculation of total coolant loss.

IV. Microscopic Uncertainties

Naturally, it is always possible to check the used data by comparing integral data with those measured in critical facilities. Yet, all these results have to be interpreted very carefully, and it is not always possible to come to a conclusion about microscopic data.

Therefore, we thought it might be helpful to see how the uncertainties work in large fast reactors. At first we made a calculation concerning the sodium resonance. Up to the last conference at Antwerp in July 1965, the peak value of the sodium resonance at 3 keV had been uncertain by about 30 %. We tried to find the action of this resonance by removing the peak in the K-1 set and calculating coolant void and DC with the new set K1R. Table 2 gives the results.

As expected, there was almost no effect on the void effect for sodium, but the DC decreased by about 20 %.

FIG. 8. The Group Constants for Inelastic Scattering in Pu^{239} .

TABLE 2. COMPARISON OF SODIUM VOID AND DC, CALCULATED WITH AND WITHOUT SODIUM RESONANCE

Set	Δk (%) for 40% Sodium Loss	Doppler Co-efficient ($\times 10^5$)	Lifetime ($\times 10^7$ sec)
K1	0.83	-1.69	6.21
K1R	0.89	-1.34	5.84

An uncertainty of about 30 % in height therefore may have less than 7 % influence on DC. It should be noted that these results are valid for weighting spectra without any resonance dip. The problems previously discussed are equally significant, because the weighting flux changes the elastic moderation of all light materials.

The realistic influence on criticality of the discrepancies shown in Figures 5 through 9 were estimated from calculations done with ABN and KFK set.

Table 3 gives the average group constants for fission

and capture, condensed from the 26-group ABN and KFK sets, using the same 1,000-MWe spectrum for condensation. The mentioned differences for Fe, Ni, and Pu are reproduced; the average capture value of U^{238} is smaller in KFK by about 5 %. It should be mentioned that the O^{16} capture is overestimated by about 40 %, so that the difference with respect to ABN is even larger. The microscopic cross sections for the higher isotopes of plutonium were taken to be the same in the KFK and ABN sets.

Table 4 shows the results of calculations for a spherical model of the following core composition (Na 1): Cr, 4.6 v/o; Na, 49.8 v/o; O, 14 v/o; Pu^{240} , 0.7 v/o; Pu^{242} , 0.05 v/o; Fe, 8.8 v/o; Ni, 5.6 v/o; Pu^{239} , 1.4 v/o; Pu^{241} , 0.1 v/o; U^{238} , 11.7 v/o. The reaction rates in Table 4 are normalized to make the total production of neutrons in the core unity. Both reactors are made critical. The KFK column shows that the capture in iron contributes

about 2% to the balance, that in nickel about 2.5%. In other words, without any capture in iron or nickel about 2% change in criticality can be expected. If one now compares ABN with KFK values, the increase in the iron capture cross section by a factor of 2 results in an additional contribution in absorption rate per neutron born in the core of less than 1%, for nickel of less than $\frac{1}{2}\%$. The increased capture probability in iron from 1 to 2% is partially due to the softening of the spectra by KFK data.

It is remarkable that the lowering of the Pu^{239} fission cross section between 10 and 50 keV is completely compensated in this reactor by the higher spectrum in this region, so that the fission rates are nearly the same. This is clearly an effect of criticality. U^{238} has about 2.5% increase in capture rates, which is also due to spectral effects.

Because the reactor is made critical with both sets by radial iteration, the leakage compensates the higher absorption rates in KFK. The total breeding ratio with KFK is nearly unchanged compared with ABN, but the internal breeding is increased by about 6.5%. More-

TABLE 3. COMPARISON OF AVERAGE MICROSCOPIC CROSS SECTIONS IN A 1,000-MWe SPECTRUM (CONDENSED FROM 26 GROUPS) FOR ABN AND KFK DATA

Element	$\bar{\sigma}_f$		$\bar{\nu}\sigma_f$		$\bar{\sigma}_c$	
	ABN	KFK	ABN	KFK	ABN	KFK
Cr	—	—	—	—	$9.86 \cdot 10^{-3}$	$9.12 \cdot 10^{-3}$
Fe	—	—	—	—	$9.66 \cdot 10^{-3}$	$1.97 \cdot 10^{-2}$
Na	—	—	—	—	$2.46 \cdot 10^{-3}$	$2.77 \cdot 10^{-3}$
Ni	—	—	—	—	$2.33 \cdot 10^{-2}$	$2.85 \cdot 10^{-2}$
O ¹⁶	—	—	—	—	$1.03 \cdot 10^{-3}$	$9.0 \cdot 10^{-4}$
Pu^{239}	2.110	1.986	6.150	5.790	$5.91 \cdot 10^{-1}$	$5.06 \cdot 10^{-1}$
U^{238}	0.040	0.039	0.112	0.113	$4.31 \cdot 10^{-1}$	$4.10 \cdot 10^{-1}$

TABLE 4. COMPARISON OF FISSION AND ABSORPTION RATES FOR ABN AND KFK SETS

Element	R_f (%)		R_a (%)	
	ABN	KFK	ABN	KFK
Cr	—	—	0.57	0.60
Fe	—	—	0.95	2.07
Na	—	—	0.30	0.35
Ni	—	—	1.98	2.41
O	—	—	0.25	0.22
Pu^{239}	24.84	24.78	31.55	31.02
Pu^{240}	1.94	1.79	6.61	6.87
Pu^{241}	2.96	3.30	3.45	3.84
Pu^{242}	0.09	0.09	0.29	0.30
U^{238}	4.31	4.05	35.79	38.08
Sum	34.14	34.01	81.78	85.76
Leakage	—	—	18.22	14.24
Total	—	—	100.00	100.00

TABLE 5. SEPARATION OF DISCREPANCIES BETWEEN THE ABN AND KFK SETS DURING PREPARATION OF KFK 26-10

	ABN	R1	R2	KFK
k_{eff}	1.0038	0.9953	0.9823	0.9701
$\frac{k_{eff} - k_{eff}^{ABN}}{k_{eff}^{ABN}}$ (%)	0	-0.85	-2.15	-3.37
Lifetime ($\times 10^7$ sec)	3.76	3.79	3.48	3.70
k_{∞}	1.266	1.246	1.234	1.221

over, one can plainly see that a change in the average ν -values for Pu^{240} of 15% (as is true between YOM and ABN) gives a change in the balance by about 1%, which is also the difference in the multiplication. The differences in the higher plutonium rates are due to spectral effects.

FIG. 9. Successive Fundamental-mode Spectra during KFK 26-10 Preparation.

TABLE 6. COMPARISON OF RESULTS OBTAINED FROM DIFFERENT GROUP SETS FOR A 5,000-LITER PLUTONIUM OXIDE BREEDER

Set*	Critical Mass (kg Pu ²³⁹)	Breeding Ratio	Internal Breeding Ratio	$\Delta k(\%)$ for 40% Sodium Loss	Fraction of Fissions below 9.1 keV
ANL (26)	1514	1.34	0.89	+0.25	0.17
YOM (16)	1536	1.35	0.92	+1.22	0.11
HR SH (16)	1567	1.43	0.91	-0.23	-
HR UNSH	1721	1.51	1.02	+1.22	-
ABN (26)	1523	1.37	0.93	+0.13	0.178
KFK (26)	1643	1.375	0.938	+0.69	0.185

* Value in parentheses gives number of groups.

TABLE 7. COMPARISON OF RESULTS FROM DIFFERENT GROUP SETS FOR ZPR III/39

ZPR III/39	ABN	YOM	KFK	Expt.
k_{eff}	1.0000	0.9839	0.9928	1.0
Critical Mass (kg U ²³⁵)	388	422	403	426.5
Deviation from $M_{exp}(\%)$	-9.0	-1.0	-5.5	-
$\sigma_f^{U^{238}}/\sigma_f^{U^{235}}$	0.0488	0.052	0.0483	0.0475
$\sigma_f^{Pu^{239}}/\sigma_f^{U^{235}}$	1.1560	1.230	1.183	1.180

During preparation we separated the single effects of the KFK set with fundamental-mode calculations. The results are shown in Table 5 and in Fig. 9. We started with the ABN set and successively replaced values for the nine materials mentioned above. The shielding fac-

tors remained the same except for the elastic-removal cross sections. In a first step we replaced O¹⁶ and C (spectrum R1 in Fig. 9). For a typical oxide breeder the replacement of oxygen results in a loss in multiplication of 1%. This is almost exclusively due to the different description of elastic moderation (above 0.4 MeV). The peak of the spectrum is shifted remarkably. In the next step we exchanged Na, Cr, Fe, and Ni. The resulting spectrum is R2, and the decrease in multiplication is about 1% also, caused by the increased capture in iron and nickel. Finally, values for U²³⁵, U²³⁸, and Pu²³⁹ were replaced, resulting in a further lowering in multiplication (fission of Pu²³⁹ and increased absorption in U²³⁸ by spectral shift).

In Table 6 the KFK-set is compared with group sets already mentioned. Integral data were calculated for a 5,000-liter fast oxide reactor, which is described in the Geneva paper P/267 (1964) by D. Okrent *et al.*

Moreover, a comparison was made with the measured data of the spherical assembly ZPR III/39. In Table 7 we find better agreement for the critical mass than given by use of ABN; the KFK spectral indices are in a better agreement with experiment than are the YOM and ABN results. These two comparisons very clearly show the difficult situation in obtaining final conclusions about used sets.

Acknowledgment

The authors appreciate many helpful discussions with R. Froelich, K. Ott, and J. J. Schmidt. We are obliged for the assistance of H. Bachmann, B. Ortman, and D. Woll in preparing the different group sets.

Appendix

GROUP BOUNDARIES FOR THE ABN AND KFK SETS

Group	E_n	Δu	Group	E_n	Δu
1	6.5-10.5 MeV	0.48	14	1.0-2.15 keV	0.77
2	4.0-6.5 MeV	0.48	15	465-1000 eV	0.77
3	2.5-4.0 MeV	0.48	16	215-465 eV	0.77
4	1.4-2.5 MeV	0.57	17	100-215 eV	0.77
5	0.8-1.4 MeV	0.57	18	46.5-100 eV	0.77
6	0.4-0.8 MeV	0.69	19	21.5-46.5 eV	0.77
7	0.2-0.4 MeV	0.69	20	10.0-21.5 eV	0.77
8	0.1-0.2 MeV	0.69	21	4.65-10.0 eV	0.77
9	46.5-100 keV	0.77	22	2.15-4.65 eV	0.77
10	21.5-46.5 keV	0.77	23	1.0-2.15 eV	0.77
11	10.0-21.5 keV	0.77	24	0.465-1.0 eV	0.77
12	4.65-10.0 keV	0.77	25	0.215-0.465 eV	0.77
13	2.15-4.65 keV	0.77	T'	0.0252 eV	

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Discussion

Mr. Yiftah (Israel AEC): In Fig. 2 are shown four different weighting spectra. Were the same macroscopic data used with different weighting spectra? Was the same number of groups used in all the sets? Was a typical 1000-MW(e) flux used as a weighting flux for the calculation?

Mr. Küsters: The answer to the three questions is yes. Except for the ABN set all the same microscopic cross sections were used. The groups were the same, as was the group structure. We placed the nine materials in the Russian group-constants set to have a very complete one. Therefore, we left the group structure totally the same.

Mr. Chernick (BNL): Since you have indicated that self-shielding effects are important, could you give more details? Was this a homogeneous calculation?

Mr. Küsters: Yes.

Mr. Chernick: This is also the case for sodium loss; you didn't go to complete sodium loss, but still even that would be homogeneous.

Mr. Küsters: Yes, still homogeneous.

Mr. Chernick: Has anybody checked the Russian self-shielding factor for homogeneous systems just to see how adequate they are?

Mr. Küsters: Yes, we did and found that for plutonium and uranium there was a discrepancy with our own calculations of less than 5% for the fissile and fertile materials. Additional discrepancies were found for the structural materials.

Mr. Chernick: How about the streaming effect? There seems to be a great deal of uncertainty in the spectrum

which causes this. In addition, it seems to me that, in view of the importance of the voiding problem as indicated yesterday by the Panel, here again heterogeneity effects become important, especially for the pancake cores. For realism one would have to take this into account.

Mr. Küsters: Yes. We are now taking into account the heterogeneity in the self-shielding effect by the rational approximation of Wigner as the first step.

Mr. Ott (Karlsruhe): I would like to reply to your question about the streaming effect. We made a study about the influence of the streaming effect on the sodium void effect, and we found, at least for the usual fast reactor shapes, it is really small, because there are about 100,000 pins. In other words, the reactor is in itself approximately homogeneous and large streaming effects cannot occur.

Mr. Perry (ORNL): Do I understand correctly that you conclude the KFK set is the one best suited for your purposes?

Mr. Küsters: Yes.

Mr. Perry: Was this the set on which the comparison calculations were based?

Mr. Küsters: Yes.

Mr. Hummel (ANL): Our experience with ELMOE has been that it seems to be possible to define effective cross sections that are applicable to a fairly wide variety of reactors. So I would expect the procedure that the speaker indicated to be fairly successful. On the slides the speaker indicated some results with an ANL-26 group set, results which were rather negative. I wish to warn people that there is one material in this cross section set that has a big error in the plutonium fission cross section. I think that on some listings it was designated material 2 at 300° and sigma p of 300. The apparent fission cross section about 2 or 3 kilovolts was way too big, and this would lead to much too negative a sodium void effect. This might have been the one you used in that comparison. That material should be thrown out.

Mr. Küsters: I took this figure from the Geneva paper.

Mr. Hummel: I think that those figures came from the erroneous cross section.

Some Calculations of Fast Reactor Doppler and Sodium Loss Coefficients with Particular Reference to the Effects of Core Lattice Structure and Radial Fuel Temperature Distribution

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1. Introduction

This paper gives the results of calculations concerning two physics problems of dilute fast power reactors: (1) the effects of core heterogeneity in the main Doppler region of the spectrum; and (2) the treatment of resonance capture in a fuel rod with a radial temperature distribution.

For current reactor designs the neutron mean free path is usually several times greater than the cell dimensions over most of the spectrum, and heterogeneity effects are unlikely to be very important. At low energies, however, the mean free path will be very small near resonance peaks, and heterogeneity effects might become more significant. In particular, the stronger self-shielding of resonances in lumped fuel, as compared with the homogenized cell, will influence the multigroup cross sections and their Doppler change with temperature.

The heterogeneity calculations described in Section 2 refer to a particular unreflected reactor lattice. The reproduction constant k_{eff} as well as the Doppler and sodium loss coefficients are compared with the results for a homogeneous system of the same size and material composition. The treatment of heterogeneity is limited to the energy range below 15 keV. The calculations are a particular application of a numerical method for treating the resonance-energy region which has been developed at Winfrith.⁽¹⁾

A different problem requiring consideration of the lattice cell structure is the calculation of resonance capture in a fuel rod having a radial temperature distribution. A common approximation is to use the mean temperature. However, some Monte Carlo calculations for a fast reactor lattice⁽²⁾ cast doubt on this procedure, and further study of the problem was considered necessary. In Section 3 are given the results of calculations, also by the Monte Carlo method, for a hypothetical cell containing a uranium-238 dioxide rod, which is subdivided into three radial regions at different temperatures. The resonance

capture within a limited energy range is compared with results for uniform rod temperatures.

2. Comparison of Doppler and Sodium Loss Coefficients for Homogeneous and Heterogeneous Systems

(a) Method of Calculation

The aim of the investigation was to study the effects of core heterogeneity in the spectral region below 15 keV, which gives rise to most of the Doppler coefficient in typical dilute fast reactors.

The calculations refer to an unreflected reactor containing 39.3v/o Pu²³⁹-U²³⁸ dioxide fuel (U/Pu = 5), 50.7 v/o sodium and 10 v/o steel. A square lattice cell was assumed of side 0.354 in., containing a central fuel rod 0.28 in. in diameter. The steel and sodium were assumed to be uniformly mixed in the outer cell region. The specifications are similar to Case No. 4 of reference 3, but a two-region square cell was chosen for convenience of calculation. The geometrical buckling was fixed at 15.4 m⁻², giving a system close to critical size.

The reproduction constant k_{eff} was calculated for different fuel temperatures and sodium densities by assumption of the standard fundamental-mode solution in multigroup diffusion theory. A 33-group set of cross sections was used, covering the energy range from 10 MeV to 0.4 eV in half lethargy units, apart from the top group, which was of unit width. Above 15 keV and below 37 eV the group cross sections were taken from the current Winfrith data set for dilute fast reactor calculations (FD2), and were uncorrected for heterogeneity or Doppler broadening. Between 15 keV and 37 eV the group cross sections were calculated by a numerical method for treating the resonance region,⁽¹⁾ using two STRETCH codes, GENEX and SDR.

The purpose of GENEX is to compute a library of Doppler-broadened cross sections for the fissile and

fertile isotopes below about 20 keV. Resolved resonance widths and spacings are used where available, while in the unresolved region they are generated by sampling from assumed statistical distributions. The cross sections are computed over a suitably chosen energy mesh which is fine enough to represent the variation through individual resonances, namely, about 10^5 points between thermal energies and 20 keV.

The SDR code uses GENEX cross sections in a numerical solution of the Boltzmann transport equation for a lattice cell, starting with a neutron source at 20 keV. The 2.85-keV sodium resonance cross section was represented analytically by a series of straight-line segments. The code treats the spatial variation of flux across the cell by the method of collision probabilities. These functions are computed at each energy mesh point by a separate routine, using for the present cell geometry the method of reference 4. An editing code, SDRED, finally processes the computed flux spectrum and cross sections to give reaction rates and group cross sections averaged over prescribed energy ranges. For the present application, macroscopic group cross sections were computed for the cell, which included averaging over the two-region spatial flux distribution as well as over energy.

Sets of 12-group cross sections between 15 keV and 37 eV were obtained in this way for fuel temperatures 300 and 900°K, and for zero, 50, and 100% sodium density. Similar sets were also derived for the comparative case of the homogenized lattice cell.

(b) Results

The calculated values of k_{eff} , the average Doppler coefficient between 300 and 900°K, and the reactivity change on complete loss of sodium at 300°K are shown in Table 1. The contributions to the sodium loss coefficient from terms other than the leakage are also included. As explained above, heterogeneity is allowed for only in the resonance region below 15 keV, and the lattice is treated as homogeneous above this energy.

It will be seen that the predicted effects of heterogeneity are small. Thus, k_{eff} is increased by only 0.07%. A large change in this quantity was not to be expected, however, since only about one-sixth of the total reactivity comes from below 15 keV. The calculations of Storrer *et al.*⁽⁵⁾ for a somewhat similar system suggested that k_{eff} might be increased by a further 0.1% from heterogeneity effects above 15 keV, when allowance is made for the larger diameter of the fuel rod considered by those authors.

The magnitude of the negative Doppler coefficient

TABLE 1. THE EFFECT OF HETEROGENEITY BELOW 15 keV ON k_{eff} , THE DOPPLER COEFFICIENT, AND THE 100% SODIUM LOSS COEFFICIENT

Parameter	System	
	Homogeneous Lattice	Heterogeneous Lattice
k_{eff} (300°K)	1.0000	1.0007
Doppler Coefficient (10^{-5} per °C)	-1.07	-1.10
Sodium Loss Coefficient (%) dk/k	-9.45	-9.51
Sodium Loss Coefficient Excluding Leakage Term (%) dk/k	+2.23	+2.17

is about 3% greater in the heterogeneous case. A perturbation-theory analysis of the results showed that this figure is the balance of several small competing terms. The main contribution arises from the increased Doppler change in the absorption cross section for the energy groups near the 2.85-keV sodium resonance. The small change in the Doppler coefficient is consistent with the conclusion of Greebler and Goldman⁽⁶⁾ that heterogeneity in large fast ceramic reactors would have a small effect on Pu²³⁹ and U²³⁸ multigroup cross sections.

The results in Table 1 show that the effect of heterogeneity on the sodium loss coefficient is also small. The overall coefficient is slightly more negative, mainly because of a 3% decrease in the positive non-leakage component. This component includes the effects of spectral hardening and the stronger self-shielding of fission and capture resonances which occur when sodium is removed. The reduction of 3% due to heterogeneity comes in roughly equal parts from the spectral hardening and resonance self-shielding terms.

The calculations in Table 1 were based upon Hibdon's data for the 2.85-keV sodium resonance. A similar set of calculations was also performed using alternative data due to Lynn, Firk, and Moxon (Harwell). The peak total cross sections are, respectively, about 630 and 370 b, but the cross section below the resonance is higher in the latter data. The change from Hibdon to Lynn *et al.* data gave rise to only small differences in the results. For both the homogeneous and heterogeneous cases, k_{eff} was reduced by 0.1%, and the magnitude of the Doppler coefficient by 1%. The main effect was to increase the positive nonleakage component of the sodium loss coefficient by about 5%, e.g., the figure of 2.23% dk/k shown in Table 1 for the homogeneous case was increased to 2.35% dk/k , yielding a slightly less negative overall sodium

TABLE 2. U^{238} RESONANCE CAPTURE AND ITS DOPPLER CHANGE FROM 300°K, FOR UNIFORM TEMPERATURES OF 300, 600, AND 900°K, AND A RADIAL TEMPERATURE DISTRIBUTION WITH MEAN VALUE 600°K

Energy Range (keV)	Temperature (°K)	U^{238} Capture	Doppler Change
1.6-2.1	300	0.085 ± 0.001	—
	600	0.096 ± 0.001	0.011 ± 0.0007
	900	0.104 ± 0.001	0.019 ± 0.0007
	Distribution	0.096 ± 0.001	0.011 ± 0.0007
0.8-1.6	300	0.248 ± 0.002	—
	600	0.278 ± 0.003	0.030 ± 0.0025
	900	0.302 ± 0.003	0.054 ± 0.0025
	Distribution	0.277 ± 0.003	0.029 ± 0.0025

loss coefficient, -9.33% as against the quoted -9.45% dk/k with Hibdon data.

It should be emphasized that the small effects of heterogeneity obtained above refer to a typical lattice in a power reactor. Considerably more important heterogeneity corrections have been indicated by experimental and theoretical work on fast zero-energy assemblies such as ZPR-III and ZEBRA. There the fissile, fertile, coolant, and structural materials are often present in the form of separate plates, giving a larger cell size than in a power reactor. In the latter system a considerable degree of homogeneity is already achieved through the use of mixed oxide or carbide fuel pins.

3. Uranium-238 Resonance Capture for a Nonuniform Radial Temperature Distribution

(a) Method of Calculation

The calculations refer to a square lattice composed of a U^{238} dioxide rod surrounded by sodium. The rod diameter was chosen to be 0.20 in. and the cell width 0.30 in. The assumed isotopic concentrations in units of 10^{24} atoms per cc were U^{238} : 0.020; oxygen: 0.040 in the rod; sodium: 0.022 in the outer cell region. A hypothetical temperature distribution was defined by subdividing the rod into three concentric radial regions of equal cross-sectional area. The central region was at 900°K, the intermediate region at 600°K, and the surface region at 300°K.

The U^{238} resonance capture of neutrons slowing down below a prescribed source energy was calculated by a Monte Carlo code MOCUP for the IBM 7090 computer.⁽⁷⁾ Reflective boundary conditions were imposed at the outer cell surface, so that effectively the results refer to a lattice array rather than to

an isolated cell. The library of U^{238} resonance cross sections obtained by the GENEX code (see Section 2) was used with MOCUP.

Calculations were made separately for two energy ranges, 2.1 to 1.6 keV, and 1.6 to 0.8 keV, which lie in the main Doppler region. The numbers of neutron histories followed were 16,384 and 6,044, respectively, yielding about the same percentage standard error for the calculated U^{238} capture in each range.

Comparative calculations were also made for uniform rod temperatures of 300, 600, and 900°K. Runs at different temperatures were correlated by choosing the same source neutrons and the same sequence of random numbers used for tracking, in order to reduce the standard error on the Doppler change in capture.

(b) Results

The calculated U^{238} capture, and its Doppler change relative to a uniform rod temperature of 300°K, are shown in Table 2. The capture is normalized to one source neutron entering the top of each energy range. Results are included for uniform rod temperatures of 300, 600, and 900°K, as well as for the assumed 3-region radial temperature distribution.

The standard error of the Doppler changes is less than might be expected from the quoted error for the capture, by about 30% for the lower energy range and about 50% for the higher. These reductions are a consequence of the correlation of MOCUP runs explained above.

The volume-weighted mean of the assumed temperature distribution is 600°K. It will be seen from Table 2 that the results for the case of the temperature distribution are in good agreement with those for a uniform temperature equal to this value. One may conclude that the present work gives support to the common approximation of replacing a radial distribution of fuel-rod temperature by the mean value when calculating resonance absorption in fast reactors. The statistical uncertainty in the results does not, however, exclude the possibility of errors of order $\pm 10\%$ in Doppler coefficients calculated by this rule. Such errors may not be considered excessive compared to those due to current imperfections in the methods and data used for calculations, or to uncertainties in predicted temperature conditions in power reactors.

Blässer and Matthes⁽²⁾ performed Monte Carlo calculations for a lattice of 0.125-in.-diameter rods of 10% enriched uranium, cooled with sodium. A parabolic radial temperature distribution was assumed, and the energy range 1 to 10 keV was investigated.

The quantities calculated were the partial Doppler coefficients with respect to increments in the rod surface and centre temperatures. The ratio of these coefficients over a range of temperature conditions was shown to vary between about 2.5 and 4, in contrast to the value of unity expected if the mean-temperature rule were correct.

A direct comparison with these results could not be made in the present investigation since cross-section data were not available at a sufficient number of temperature values. However, the problem considered corresponds roughly to the determination of the centre-temperature Doppler coefficient as defined by Blässer and Matthes.

The agreement with the mean-temperature rule in the present work suggested that it may thus be the behaviour of the surface rather than of the centre temperature coefficient which is responsible for the results obtained by these authors. Collins⁽⁸⁾ has investigated the problem of capture in a bare slab of U^{238} with a parabolic temperature distribution, assuming normally incident neutrons. For a range of slab widths and temperature conditions he found that the ratio of the surface to centre temperature coefficients of capture departed only slightly from the value given by the mean-temperature rule. The present work, together with Collins' results, suggest that the use of this rule should be an adequate approximation in Doppler calculations for the type of fast reactor considered here.

To conclude, we show in Table 3 the radial distribution of U^{238} capture in the rod, for uniform temperatures and for the temperature distribution. The energy range is 0.8 to 1.6 keV, but similar trends were also found for the higher range. Standard errors are not quoted individually, but lay within the limits ± 0.0005 to ± 0.0025 .

For uniform temperatures the capture distribution is nearly flat, although with a tendency to rise slightly towards the rod surface. For the temperature distribution, however, the capture decreases radially outwards.

Comparison of the results suggests that the capture at any point in the rod is determined primarily by the local temperature and is relatively insensitive to the temperature elsewhere. For instance, in the case of the temperature distribution, the capture (0.083) in the surface region at 300°K is close to the value (0.086) when the rod is at a uniform temperature of 300°K, and likewise for the other two regions. This suggests that a suitable analytical approach to the problem would be to evaluate the resonance capture as a function of uniform rod temperature, and then

TABLE 3. RADIAL DISTRIBUTION OF U^{238} RESONANCE CAPTURE BETWEEN 0.8 AND 1.6 keV, FOR UNIFORM ROD TEMPERATURES AND A RADIAL TEMPERATURE DISTRIBUTION

Temperature (°K)	Rod Region		
	Central	Intermediate	Surface
300	0.080	0.081	0.086
600	0.092	0.091	0.095
900	0.099	0.099	0.103
Distribution	0.103 (900°K)	0.090 (600°K)	0.083 (300°K)

average over the actual radial temperature distribution.

4. Conclusions

1. The reactivity of a typical dilute fast power reactor core, and the Doppler and sodium loss coefficients, are found to be only slightly influenced by heterogeneity effects in the resonance energy region below 15 keV. The use of Lynn, Firk, and Moxon's data for the 2.85-keV sodium resonance in place of Hibdon's data also gives rise to only small changes in the results.

2. The U^{238} resonance capture in a fuel rod having a particular radial temperature distribution agrees closely with the results for the uniform mean temperature over the energy range from 0.8 to 2.1 keV which was investigated. The statistical error in the results, due to use of the Monte Carlo method, does not, however, preclude the possibility of $\pm 10\%$ errors in Doppler coefficients calculated assuming the mean fuel temperature.

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Discussion

Mr. Ott (Karlsruhe): In your heterogeneity study, you showed remarkably small effects, and the heterogeneity value for k_{eff} was larger than in the homogeneous case. How shall one understand this?

Mr. Codd: I think that arises from the change in the resonance group cross sections with self-shielding. Self-shielding is stronger in the heterogeneous case.

Mr. Ott: In calculating the Doppler coefficients, in which you consider the temperature distribution over the reactor, have you also considered the temperature distribution of the additional energy release?

Mr. Codd: No, we have not extended the investigation outside this simple temperature variation across the fuel pin.

Mr. Ott: I meant that the cross temperature distribution over the reactor influences your calculations of the Doppler coefficient. A second influence will come from the space distribution of the additional energy release. The two effects are different.

Mr. Codd: This is limited to the case of uniform temperature conditions across the core.

Mr. Ott: Do you know of the early investigation of Paul Greebler about the influence of the microscopic temperature distribution over the rod?

Mr. Codd: Yes.

Mr. Ott: Greebler derived that the deviation of, say, an effective temperature from the average temperature should be expected to be of the order of some percent.

Mr. Codd: Yes, we were aware of that work and it is discussed in this paper. I think we wanted to do a little more because Greebler's considerations particularly refer to conditions at the center and in the extreme wings of the resonances; in our calculations we integrate over the whole resonance. In general, our results support Dr. Greebler's conclusions; however, the results indicate an even smaller effect due to the temperature distribution, because we have taken into account conditions in the middle parts of the resonance as well.

Mr. Engelder (Babcock & Wilcox): In support of your calculations which indicate the temperature distribution within the fuel pin is relatively unimportant, I would like to comment that Dr. Pettis at our laboratory has measured the resonance integral of thorium

oxide under both conditions of a temperature distribution such as you have shown and under an average temperature through the fuel pin. He found essentially no difference in the resonance absorption between those two cases. This, of course, is in a $1/E$ spectrum. The measurement is for thorium rather than uranium, but I think from considerations of the Debye temperature you would expect this effect to be more serious in thorium than in uranium. We were not able to create exactly a parabolic temperature distribution across the pin. It did have a temperature profile much like the one you have drawn on the blackboard, but it didn't quite have the same temperature gradient at the surface of the pin where you would expect the effect to be most serious. Nonetheless, based on these measurements, there should be no very strong effect of the temperature distribution through the pin.

Mr. Codd: Thank you very much; is this work to be published?

Mr. Engelder: It has been published as a B&W report within the last three months.

Mr. Yiftah (Israel AEC): I just wanted to remark that Mr. Shaviv and I looked into the same problem of core heterogeneity using the Monte Carlo technique and our results corroborate yours. The core heterogeneity had only a very slight influence on the results.

Mr. Wall (GE): I have three questions and one comment regarding the heterogeneity effect. Would you describe the spatial cell which you are weighting and the size of the fuel pin? Secondly, what kind of spectrum were you weighting this over? You were looking below 15 keV, so what was the magnitude of the contribution with different uranium resonances? Thirdly, what is the relative effect of the sodium resonance and the uranium resonances on the heterogeneity effects you showed? The comment I would like to make is that in the experiments we have done at the MSTA at Vallecitos we have looked at the heterogeneity effect and have seen a very small effect. There was no sodium present in the system.

Mr. Codd: We used a square of 0.35-in. OD and a fuel rod of 0.280-in. diameter. The volume proportion of fuel was 40% and I would say it was Case No. 4 specified by Dr. Okrent. We chose a square cell because we happened to have a collision-probability routine for this particular geometry. As regards your second question, we calculated our spectrum. This is part of the function of the Code SDR, which solves the Boltzmann transport equation in the resonance region. So we had to assume no spectrum. This was a slowing-down spectrum with depths at all the resonances.

Mr. Wall: The point of my question was the relative weight as regards U^{238} . We have done some meas-

urements, to be reported on this afternoon, which indicate that there was more weighting at the 1-keV U^{238} resonance than we expected from our calculations. Therefore, the relative weighting of the different resonances was somewhat important.

Mr. Codd: We have applied no extraneous weighting to resonances. The calculation integrates right down through energy taking each resonance as the neutrons come to it. In this energy region there are the resolved resonance parameters, and we don't have to make any separate assumptions about weighting.

Mr. Dunenfeld (Allison Division, GMC): We are very much interested in the Doppler effect for transient

conditions. Under those conditions the temperature profile within the fuel can be inverted from the sort you showed. What would the effect of that be on the heterogeneity effect?

Mr. Codd: We have not actually done that case although we have thought of doing it. Judging from the results we have, we think that the capture in any particular region depends mainly on the local temperature conditions rather than upon the particular variation of the temperature over the whole rod. I would think that in the inverted temperature conditions that one would gain a good agreement or approximation using the mean temperature.

Results of Doppler Coefficient Calculations for Fast Reactors and Comparison of Different Methods*

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PART I

I. Introduction

The results of Doppler coefficient calculations of three different origins are compared and analysed. The aim of this analysis is to find out the special assumptions which are responsible for obvious discrepancies. This paper gives preliminary results. The three groups of results are calculated from

- (α) the Karlsruhe nuclear data file⁽¹⁾ using the Karlsruhe code DCP⁽²⁾ for calculation of Doppler coefficients from temperature derivatives of self-shielded cross sections;
- (β) a Russian self-shielded group-constants set ABN;⁽³⁾
- (γ) a YOM⁽⁴⁾ group-constants set with corrections for the temperature dependence and for low-energy neutrons. These last results are presented in Part II of this paper, by Shaviv and Yiftah.

As a result of such an analysis one should expect a separation of all the discrepancies in three classes:

- a first class (C1) coming from the use of different resonance parameters;
- a second class (C2) coming from the spectrum, mainly caused by the use of different sets of group constants;
- a third class (C3) coming from the use of different

methods for the calculation of the Doppler reactivity effect.

For such an analysis the full knowledge of the nuclear data used by the two other groups [(β) and (γ)] in form of a data file as required for the Karlsruhe code DCP is necessary. Unfortunately, this has not been possible to manage up to now. Therefore, this paper contains only part of the comparison which we are carrying out. Beside the comparison of the final results as they were originally calculated, the temperature dependence of the Doppler coefficients is carefully investigated. Furthermore, the three different ways which are in common use for the calculation of Doppler coefficients are discussed and compared. These are the perturbation calculations of dk/dT and Δk , and successive k calculation.

These comparisons and discussions will show some discrepancies and uncertainties. A summary of these will be given in section VI.

II. Survey of the Methods of Calculation Used

The results compared in this paper were calculated by two (and sometimes three) different methods which are in common use:

1. the usual perturbation calculation of the Doppler coefficient dk/dT based on the temperature derivatives of the effective cross sections, $d\Sigma/dT$. One part of the Karlsruhe results, subsequently marked " α ," was calculated in this way (DCP).
2. The calculation of a Doppler Δk by means of

* This paper has been prepared within the framework of the association EURATOM-Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

† This work is partly supported by the Kernforschungszentrum Karlsruhe—EURATOM Association on Fast Reactors.

two multigroup calculations using effective cross sections which differ by an amount $\Delta\Sigma$ corresponding to a finite temperature interval ΔT . The second part of the Karlsruhe results and all the Israeli results marked "β" and "γ," respectively, were obtained by this method.

For the comparison of the results evidently a temperature law is necessary. This may cause some uncertainties to enter into the comparison.

3. Sometimes a third method was used for the calculation of Doppler Δk -values, namely, the determination of Δk from $\Delta\Sigma$ by means of first-order perturbation theory. However, in this method it is unclear at which temperature the neutron spectrum shall be called "unperturbed," because the spectrum may change remarkably by shifting the temperature from one end to the other of the interval under consideration.

These difficulties do not arise in the first and second methods, where it is possible to choose the proper temperature or spatial temperature distribution in the effective cross sections for the multigroup calculation of the neutron flux.

- (α) In the Karlsruhe Doppler calculation code DCP, the handling of the resonance cross sections is based essentially on the paper of Nicholson⁽⁵⁾ with some improvements concerning the approximation of the ψ -function and the mutual shielding of resonances of the same resonance series.⁽²⁾ The resonance data are listed in Ref. (1). The neutron flux and adjoint-flux spectrum in most cases were calculated from the Russian set of group constants, ABN,⁽³⁾ and in a few cases with the Karlsruhe set of group constants, KFK 26-10.⁽⁶⁾
- (β) The Russian set of group constants⁽³⁾ (26 groups) is available for three temperatures. It may be interpolated for other temperature values. By means of successive multigroup calculations the Doppler Δk were determined.
- (γ) The Doppler Δk calculations of the Israeli group are described in Part II of this paper.

III. Comparison of Doppler Δk Values and Doppler Coefficients

For comparison we have chosen in the first part of this paper two sequences of reactors with decreasing core volume. The first sequence investigated in more detail is identical with the first four cases proposed for intercomparison at this conference. They differ only* in the ratios of fertile to fissile material: $y = U^{238}/Pu^{239} = 11, 9, 7$, and 5. They are treated in the

* Concentrations in 10^{24} atoms/cm³; Na: 0.0123; stainless steel: 0.0011; oxygen: 0.0144; $Pu^{239} + U^{238}$: 0.0072.

TABLE 1. Δk BETWEEN 300°K AND 900°K

$y = \frac{U^{238}}{Pu^{239}}$	$-\Delta k_{\alpha}$ (Karlsruhe) from dk/dT - Calculation and $1/T^x$ Law	$-\Delta k_{\beta}$ (ABN) Successive k Calculation	$\frac{\Delta k_{\beta} - \Delta k_{\alpha}}{\Delta k_{\alpha}}$ (%)
11	0.0258	0.0237	-8.2
9	0.0195	0.0178	-9
7	0.0132	0.0120	-9.1
5	0.00746	0.00659	-11.6

TABLE 2. Δk BETWEEN 900°K AND 2,100°K

$y = \frac{U^{238}}{Pu^{239}}$	$-\Delta k_{\alpha}$ (Karlsruhe) from dk/dT - Calculation and $1/T^x$ Law	$-\Delta k_{\beta}$ (ABN) Successive k Calculation	$\frac{\Delta k_{\beta} - \Delta k_{\alpha}}{\Delta k_{\alpha}}$ (%)
11	0.0190	0.0190	0
9	0.0141	0.0145	+2.9
7	0.00939	0.00983	+4.7
5	0.00513	0.00559	+9

zero-dimensional approximation using B^2 to go critical. The second sequence consists of the Pu^{239} -oxide-fueled, blanketed spheres (case Pu "A") of Part II of this paper. The core volumes in this sequence are 800, 1,500, and 2,500 liters, respectively.

First comparison: The same neutron spectra, different resonance data, different methods of calculation.

In Table 1 the Doppler reactivity changes between 300 and 900°K are compared for the different ratios (y) of fertile to fissile material. The Δk values of column 2 were determined from the Doppler coefficients calculated by means of the Karlsruhe Doppler perturbation code DCP using the temperature law a/T^x fitted at 300 and 900°K. The spectra were calculated by means of the ABN group-constants set. The values in column 3 result from two successive multigroup calculations at 300 and 900°K, respectively.

In Table 2 the same values corresponding to the temperature interval from 900 to 2,100°K are listed.

It should be emphasized that all the pairs of Δk compared in Tables 1 and 2 contain the same neutron spectrum, so that the discrepancies actually refer to the resonance data (C1) and the method of handling these data (C3). It is evident from Table 1 that between 300 and 900°K the Karlsruhe DCP values are about 9% higher than the values calculated from ABN and that this difference is nearly independent of reactor size. Table 2 shows discrepancies of the opposite sign in the interval from 900 to 2,100°K, strongly increasing with decreasing reactor size.

This follows from the first comparison: Using the same neutron spectra, we obtained from different

data and different calculational methods Doppler Δk values which are in agreement up to $\pm 10\%$. The remaining discrepancies have a trend from which one may suggest that the method of handling the resonance data may be more responsible for the differences than the resonance data themselves are.

Second comparison: Different neutron spectra, identical resonance data, identical method of calculation. Results are given in Table 3.

From the second comparison we see that: The Doppler coefficients calculated with the ABN spectrum are about 15–20% larger than those obtained with the KFK-26-10 spectrum, showing the large influence of the neutron spectrum on the Doppler coefficient. This may also explain the still larger discrepancies shown in Table 3.

These differences in the spectra are discussed by H. Küsters and M. Metzenroth.⁽⁶⁾

Third comparison: Different spectra, different resonance data, different methods of calculation.

The comparison of the values in Table 4 shows remarkably large differences in the Doppler coefficients in cases where spectra, resonance data, and methods of calculation are all different (cf. Part II of this paper). In the brackets of the last column are given the values calculated from the ABN set of group constants, showing relatively small differences from A_α because

both results are calculated with the same neutron spectra.

IV. Comparison of the Temperature Law for dk/dT

The previous comparisons suggest a detailed investigation be made of the temperature dependence of the Doppler coefficient, dk/dT . First, we split the largest part of the temperature dependence, namely, a factor $1/T$,⁽⁷⁾ and consider the quantity

$$(1) \quad A(T) = T(dk/dT),$$

which will be called “reduced Doppler coefficient” in the following. As a generalization of the $1/T$ law we use

$$(2) \quad dk/dT \simeq a/T^x,$$

which was used in Section III to link dk/dT with Δk .

For the determination of a and x of equation (2) one needs two values of Δk or dk/dT . If only one value is available, one has to make an assumption about a or x . Usually one puts $x = 1$. From our Doppler coefficient calculations we computed three values for dk/dT , so that one can determine special approximations for the intervals from 300 to 900°K and from 900 to 2,100°K. In Figures 1 and 2 these approximations are compared for the four “zero-dimensional” reference reactors which were discussed above. It can be seen that the use of eq. (2) instead of just $1/T$ in these cases improves the Doppler coefficient by less than 5%. The main result from Figures 1 and 2 is that the temperature dependence of the reduced Doppler coefficient is remarkably different for the calculational procedures α and β .

Another investigation of the temperature dependence is given in Fig. 3, in which the exponent x is plotted for increasing core volume characterized by y . Also in this plot the results of procedures (α) and (β) show an opposite behaviour. With decreasing y the neutron spectrum is shifted to higher energies, and one should expect from this an increasing value of x , representing the trend from the $T^{-1/2}$ law⁽⁸⁾ of thermal reactors to a $T^{-3/2}$ law⁽⁵⁾ for very hard neutron spectra. The results of procedure (α) indicate this trend, whereas these of procedure (β) show the opposite trend. Unfortunately, we could not use the results of Part II of this paper for a further comparison of this kind, because in these sequences there is no unique trend in x as a function of core volume.

In the preceding analysis we used x as a quantity independent of temperature. If this assumption were accurate, one could use the $1/T^x$ law also for extrapolation to higher temperatures which may be useful for excursion calculations. Figure 4 shows the two values of x determined from the three Doppler coefficients (α)

TABLE 3. COMPARISON OF dk_α/dT (900°K) DETERMINED BY PERTURBATION METHOD DCP WITH THE SAME $d\Sigma/dT$ 'S AND DIFFERENT SPECTRA CALCULATED WITH DIFFERENT GROUP-CONSTANT SETS

$y = \frac{U^{238}}{Pu^{239}}$	$-\left(\frac{dk}{dT}\right)_\alpha \times 10^6$ with ABN Spectrum	$-\left(\frac{dk}{dT}\right)_\alpha \times 10^6$ with KFK 26-10 Spectrum	$\left \frac{dk}{dT} \right _{ABN} - \left \frac{dk}{dT} \right _{KFK}$
9	18.9	16.6	14%
7	12.6	10.9	16%
5	7	5.8	21%

TABLE 4. COMPARISON OF REDUCED DOPPLER COEFFICIENTS $[A(T) = T(dk/dT)]$ AT 900°K FOR THE Pu^{239} O₂-FUELED BLANKETED SPHERES OF ANL-6212 (See Part II of this paper)

Core Volume, liters	$-A_\gamma$ (Israel) from Successive k Calculation and T^{-x} Law	$-A_\alpha$ (Karlsruhe) from Direct $T \frac{dk}{dT}$ Calculation (DCP)
800	0.00126	0.0034 (0.00334)
1,500	0.00364	0.0061 (0.00580)
2,500	0.00536	0.0087 (0.00815)

mentioned above. We plotted only the results for the reactor with $y = 9$. For all the other reactors we got quite similar values. We conclude from this that for the purpose of extrapolation the use of the $1/T^x$ law is not an essential improvement compared with the use of the $1/T$ law. However, since x sensitively depends on the trends of the Doppler coefficients, the analysis of x is a useful tool to check results for their internal consistency.

FIG. 1. Comparison of Reduced Doppler Coefficients Calculated by Different Methods for $y = 11$ and 9.

FIG. 2. Comparison of Reduced Doppler Coefficients Calculated by Different Methods for $y = 7$ and 5.

FIG. 3. Exponent x of the Law $dk/dT \sim 1/T^x$ as Function of y .

V. Short Investigation of Δk Calculation by Means of First-order Perturbation Theory from $\Delta\Sigma$

The third method,^(7, 9) mentioned in section II, namely, the use of first-order perturbation theory for the calculation of Δk from $\Delta\Sigma$, involves an uncertainty in the choice of the "unperturbed" spectrum. To investigate the sensitivity with respect to the choice of the unperturbed spectrum we calculated Δk from $\Delta\Sigma$ values corresponding to temperature intervals from 300 to 900°K and from 900 to 2,100°K. The results are listed in Tables 5 and 6, and are compared with the exact values of Δk calculated from successive k calculations. These tables show that for the temperature interval considered here the Δk_{pert} values are up to 10% too high if the spectrum corresponding to the lower end of the temperature interval is used or up to 10% too low for the spectrum of the higher end of the interval. This suggests a comparison of the average value of both with the correct Δk . These average values, which are listed in the next-to-last columns of Tables 5 and 6, show deviations from the correct values of only 0 to 3%.

Clearly, all these deviations have to decrease if the temperature interval is decreased.

FIG. 4. Exponent x of the Law $dk/dT \sim 1/T^x$ as Function of T .

VI. Final Discussion

The aim of this investigation was to find out the real roots of discrepancies in calculated Doppler reactivity results which usually appear if one compares Doppler coefficients of different origins. In the introduction these roots were discussed and subdivided into three classes: C1 to C3. In this paper three groups of results $[(\alpha), (\beta), \text{ and } (\gamma)]$ are compared. The investigation is not completed up to now. The first results are qualitatively the following:

C1: The influence of the use of different resonance data on calculated Doppler reactivity results seems fairly small. From former investigations^(1, 9) it follows that one has to expect not essentially more than 10% variation. A draft comparison with the results of Shaviv and Yiftah shows still lower differences.

C2: The influence of the neutron spectrum used for the calculation of the Doppler reactivity effect is very strong. Tables 3 and 4 show discrepancies of more than 20% coming from the use of different spectra calculated from different sets of group constants. A further effect which may lead to different spectra is the use of an incorrect reference temperature, which may happen if a group-constant set is

available only for a few temperatures. The errors coming from this are small if the temperature used is not too far from the real one (compare Tables 5 and 6). Also, the use of dynamic spectra instead of static ones influences the Doppler reactivity effect only by small amounts. (In a typical example, for an excursion caused by \$1.5 step reactivity the Doppler coefficient was decreased by less than 7%.)

C3: The reactivity calculated from $\Delta\Sigma$ by successive k calculation and the determination of dk/dT from $d\Sigma/dT$ by means of perturbation theory are straight forward, and Δk (by successive k calculations) agrees with $\int_{\Delta T} (dk/dT)dT$ from perturbation theory, if $\Delta\Sigma$ corresponds strictly to $d\Sigma/dT$. Therefore methodical differences can only come from the use of different methods for the calculation of $\Delta\Sigma$ or $d\Sigma/dT$ out of the resonance data.

Tables 1 and 2 show that the values of different Doppler Δk calculations agree fairly well if the same neutron spectrum is used. However, the temperature dependence disagrees in two respects (see Fig. 1 to 3). The reduced Doppler coefficient $T(dk/dT)$ decreases with temperature in the Karlsruhe (α) results and increases in the ABN results. Furthermore, the exponent x in the temperature law $1/T^x$ decreases with decreasing core volume in the ABN results, whereas the Karlsruhe results show the opposite trend. We do not think that this comes from

TABLE 5. Δk BETWEEN 300°K AND 900°K

$y = \frac{U^{238}}{Pu^{239}}$	$-\Delta k_{300-900}^{300}$	$-\Delta k_{300-900}^{900}$	$-\Delta k_{300-900}^{\overline{300-900}}$	$-\Delta k$ (Successive k Calculation)
11	0.0247	0.0216	0.0232	0.0237
9	0.0187	0.0163	0.0176	0.0178
7	0.0128	0.0112	0.0120	0.0120
5	0.00715	0.00629	0.00672	0.00659

$\Delta k_{300-900}^{300}$ calculated from $\Delta\Sigma_{300-900}$ and spectra at 300°K.

$\Delta k_{300-900}^{900}$ calculated from $\Delta\Sigma_{300-900}$ and spectra at 900°K.

$\Delta k_{300-900}^{\overline{300-900}} = \frac{1}{2}(\Delta k_{300-900}^{300} + \Delta k_{300-900}^{900})$.

TABLE 6. Δk BETWEEN 900°K AND 2,100°K

$y = \frac{U^{238}}{Pu^{239}}$	$-\Delta k_{900-2,100}^{900}$	$-\Delta k_{900-2,100}^{2,100}$	$-\Delta k_{900-2,100}^{\overline{900-2,100}}$	$-\Delta k$ (Successive k Calculation)
11	0.0207	0.0182	0.0194	0.0190
9	0.0157	0.0138	0.0147	0.0144
7	0.0107	0.00948	0.0101	0.00983
5	0.00607	0.00545	0.00576	0.00559

$\Delta k_{900-2,100}^{900}$ calculated from $\Delta\Sigma_{900-2,100}$ and spectra at 900°K.

$\Delta k_{900-2,100}^{2,100}$ calculated from $\Delta\Sigma_{900-2,100}$ and spectra at 2,100°K.

$\Delta k_{900-2,100}^{\overline{900-2,100}} = \frac{1}{2}(\Delta k_{900-2,100}^{900} + \Delta k_{900-2,100}^{2,100})$.

different resonance data. We guess that it is caused by different handling of the resonance data. Further investigations will hopefully clear this discrepancy.

Besides these, further improvements in the calculated Doppler coefficients depend upon improvement of the neutron spectrum.

PART II

Introduction

Recently six design studies of a 1000-MWe fast plutonium breeder were published in the USA, France, and Germany.⁽¹⁰⁻¹⁵⁾ The cores of the designed 1000-MWe reactors, intended to be in operation in the seventies, differ in geometry and type of fuel, and have volumes in the range from 3500 to 7500 liters. Smaller reactors, however, in the range of 150-300 MWe and core volumes of 1000-1500 liters will be built and operated in various countries before undertaking the 1000-MWe unit.

As a matter of fact, the USSR has already started construction of the biggest fast reactor to date, BN-350, a 1000-MWt reactor to be used for power production and desalination of the Caspian Sea water near Schevchenko. This reactor, which has a core volume of 1870 liters, is to be completed and begin operation in 1968-69.

It was thought to be of interest, therefore, to study in detail one of the main safety mechanisms in fast reactors, the Doppler effect, for systems with core volumes in the range of 1000-2500 liters. The results of calculations of the change in neutron multiplication factor due to Doppler effect which were obtained in different ways are compared. In all cases the isothermal Doppler effect was calculated for a uniform rise in temperature over the whole core.

The comparison made here relates only to the possible different ways to calculate Δk caused by $\Delta \Sigma$ or dk/dT caused by $d\Sigma/dT$, and the relation between these quantities.

Description of the Calculated Systems

The calculated systems were first described by Yiftah and Okrent.⁽¹⁶⁾ Details are given in Table 7. All systems are spherically symmetric. Three plutonium compositions were assumed and are denoted by Pu "A," Pu "B," and Pu "C." Pu "A" is pure Pu²³⁹. Pu "B" corresponds to the isotopic composition of plutonium after one cycle of irradiation in a thermal reactor fueled with enriched uranium (in this case the Yankee reactor fueled by oxide fuel and cooled by pressurized water). Pu "C" corresponds to the plutonium obtained after several fuel cycles in thermal reactors with high burnup. This is the plutonium isotopic composition obtained at equilibrium. The reason

for choosing these special isotopic compositions for plutonium is that they represent and embrace the possible isotopic composition which will be obtained from various origins.

The core volume of the calculated systems ranges from 800 to 2500 liters. Three chemical compositions of the fuel were assumed: metal, oxide, and carbide fuel. This assumption seems to cover the possible fuel composition of the fast breeder reactors.

Methods of Calculation of $\Delta \Sigma$

A full description of the methods for the calculation of $\Delta \Sigma$ can be found in reference (17). A short summary for the sake of orientation is given here. The method

TABLE 7. COMPOSITION AND STRUCTURE OF CALCULATED SYSTEMS

Core Volume: 800, 1500, and 2500 liters				
Core Volume Fractions: Fuel and Fertile Isotopes, 0.25				
Structural Materials, 0.25				
Coolant, 0.50				
Fuel		Density (gm/cc)		
Pu-U ²³⁸		19		
PuO ₂ -UO ₂		8.4		
PuC-UC		11.39		
Plutonium Isotopic Composition				
Type (Notation Only for this Work)	Pu ²³⁹	Pu ²⁴⁵	Pu ²⁴¹	Pu ²⁴²
	Per Cent			
A	100	0	0	0
B	74.7	10.2	12.4	2.7
C	40	10	25	25
Coolant: Liquid Sodium; Density: 0.84 gm/cc.				
Blanket: Thickness of 45 cm.				
Blanket Volume Fraction				
U ²³⁸ (Metal), 0.6				
Na, 0.2				
Fe, 0.2				
Reflector: Thickness of 30 cm.				
Reflector Volume Fraction				
Fe, 0.6				
Na, 0.4				

used for the calculation of $\Delta\Sigma$ changed with the energy range involved and with the type of isotope. At very high energies, e.g., above 6.5 keV for Pu^{239} , 1 keV for Pu^{241} , and about 30 keV for U^{238} , and high temperatures, the method A of Nicholson⁽⁵⁾ was used. The two basic assumptions of this method are:

(1) At very high energies the resonances overlap strongly.

(2) The asymptotic expression for the ψ function can be used.

In the case of fissile isotopes at medium energies assumption (1) was replaced by the assumption:

(3) The total contribution of the resonant isotopes to the cross section is small.

In practical cases this assumption can be valid at lower energies than those at which (1) is valid. Although at high energies and temperatures (2) can be justified, this is not the case at low energies; hence, when assumption (3) was used, no use was made of (2). Because the second method consumes more computing time, it was replaced by the first method for energies above that at which both methods gave practically the same result (to within an accuracy of 1-3%).

Method B of Nicholson was used for fissile isotopes at very low energies and for fertile isotopes at low energies.

The effects of heterogeneity, interaction between resonances of the fertile and fissile isotopes, large sodium resonances on the effective cross section, etc., were treated by the use of the Monte Carlo method.

Complete tables of all the resonance parameters used in the calculation can be found in ref. (17). The basic cross-section set used was the 16-group cross-section set of Yiftah, Okrent, and Moldauer.⁽⁴⁾ The changes in the effective group cross sections for a specific system were obtained by averaging the changes in effective cross section obtained with the methods mentioned above, over the approximated neutron flux of the system. Examples of neutron fluxes are given in Fig. 5. In many cases the change in effective cross section decreases rapidly with energy; hence averaging is of importance. The flux histogram obtained for each system from the criticality calculation at 300°K with the multigroup model was approximated by a smooth curve which was later used for averaging the changes in effective cross section.

The 16-group cross-section set used extends from 10 MeV down to 0.5 keV. However, a non-negligible contribution to the total Doppler effect comes from below this energy. Since the 16th energy group is the last energy group in this cross-section set, it contains all the neutrons with energy below 2.1 keV, including those neutrons that in reality are scattered to lower energies than 0.5 keV. Since in this cross-section set there is no energy group below 0.5 keV, all the neutrons with energy below 0.5 keV remain in the 16th group. The overestimation of neutrons in this energy range is expressed in the maximum found in the case of large oxide-fueled reactor for the lowest-energy group (see Fig. 5). The procedure used was to assume that the 16th energy group extends down to 0.1 keV. The

FIG. 5. Spectra for Oxide-fueled Reactors of Two Different Sizes (2500 Liters and 800 Liters).

TABLE 8. RESULTS OF THE CALCULATION OF THE DOPPLER EFFECT $-\Delta k \times 10^4$

Core Volume (liters)	Fuel Type	Pu "A"		Pu "B"		Pu "C"	
		Δk (300-900°K)	Δk (300-1500°K)	Δk (300-900°K)	Δk (300-1500°K)	Δk (300-900°K)	Δk (300-1500°K)
800	Metal	14.7	20.0	15.9	21.8	16.7	23.0
	Oxide	12.4	19.2	13.8	20.5	19.4	27.4
	Carbide	24.1	33.8	29.6	40.9	38.5	48.2
1500	Metal	19.7	26.7	21.2	29.3	22.4	30.9
	Oxide	41.8	60.0	46.7	65.7	61.1	85.3
	Carbide	39.7	57.4	46.8	63.8	59.3	79.0
2500	Metal	24.2	32.8	25.6	35.3	27.3	37.4
	Oxide	63.3	89.8	81.2	100	86.3	129
	Carbide	54.1	75.3	68.7	86.4	76.2	102

change in the effective group cross section for this group was obtained by averaging the change in the effective cross section over the region 0.1-2.1 keV. The cross section used for the criticality calculation was not changed. The total number of neutrons below 0.5 keV is small and does not affect appreciably the criticality calculations. The averaging flux was assumed to be $\phi(E) \propto E^{1/2}$.

The procedure adopted here is not very accurate, and the calculations were repeated with additional energy groups in this energy range. It is estimated that the possible error introduced in the group effective cross section for this group by wrong averaging flux and neutron-flux overestimation for this energy is 15-20%.

Methods of Calculation of Δk

The calculation of the change in neutron multiplication factor k due to temperature rise was carried out by a one-dimensional diffusion approximation.

Two ways were tried: the successive k method and perturbation. In the first method the procedure is as follows: the neutron multiplication factor of the system at 300°K is found by the use of standard cross-section tables, in this case the YOM cross-section set. After convergence is reached, the changes in effective cross section corresponding to a given temperature rise are added to the cold cross-section set and the new k is sought. This procedure is repeated till the highest temperature is reached. The convergence criterion (the ratio between the difference in neutron source in the i 'th and $(i-1)$ 'th iteration, and the neutron source in the i 'th iteration) was 10^{-6} . The convergence is usually rapid, and only 4-5 iterations are needed. Table 8 summarizes the results for Δk (300-900°K) and Δk (300-1500°K).

In the second method, first-order perturbation was used. A comparison of the results of both methods has shown that in all cases the result obtained by perturba-

TABLE 9. COMPARISON OF GROUP FLUXES AT TWO TEMPERATURES

The fluxes are normalized to unity: $\sum_{g=1}^{16} \phi_g = 1$

Energy Range, keV	2500-liter Core Volume; Oxide Fuel. Pu A.		800-liter Core Volume; Metal Fuel. Pu A.	
	ϕ_g (300°K)	ϕ_g (1500°K)	ϕ_g (300°K)	ϕ_g (1500°K)
0.5-2.1	0.0139	0.00926	0.000809	0.000476
2.1-5.5	0.0156	0.0153	0.00217	0.00192
5.5-9.1	0.0165	0.0164	0.00355	0.00326
9.1-25	0.0277	0.0277	0.00820	0.00767
15-25	0.0525	0.0525	0.0232	0.0220
25-40.7	0.0617	0.0619	0.0354	0.0339
40.7-67	0.0744	0.0748	0.0556	0.0535
67-110	0.0999	0.1005	0.0773	0.0745
110-180	0.1245	0.1253	0.1299	0.1253
180-300	0.1339	0.1347	0.1581	0.1525
300-500	0.1220	0.1227	0.1777	0.1715
500-825	0.1024	0.1030	0.1451	0.1400
825-1350	0.0682	0.0685	0.0868	0.0837
1350-2225	0.0485	0.0488	0.0521	0.0502
2225-3668	0.0264	0.0265	0.0294	0.0284
3668-10000	0.0120	0.0121	0.0148	0.0142

tion theory is greater by about 20% than that obtained by the successive k method. Such a great difference between the first-order approximation and the exact calculation is surprising because the percentage change in $\Delta \Sigma$ is small, less than 5% at most. When all $\Delta \Sigma$ were halved, the difference in percent between the results obtained by the two methods did not change. This fact shows that the difference is of a systematic nature inherent with the first-order perturbation calculation. In Table 9 the group fluxes at two temperatures are compared. It can be seen that in the center of a core having a volume of 2500 liters and oxide fuel the change in neutron spectrum may be of the order of 20% at the very low-energy part. Hence, such a change in neutron spectrum over the whole core can cause such a difference between the successive k method in which

TABLE 10. COMPARISON OF RESULTS OF $-\Delta k(300-900^\circ\text{K}) \times 10^4$ OBTAINED IN KARLSRUHE AND ISRAEL

Core Volume (liters)	Fuel Type Pu "A"	$-\Delta k(300-900^\circ\text{K}) \times 10^4$	
		Israel	Karlsruhe
800	Metal	14.7	24.8
	Oxide	12.4	37.4
	Carbide	24.1	51.7
1500	Oxide	42	67
2500	Oxide	63	95.7

the exact flux is used and the first-order perturbation method in which the flux at 300°K is used.

Comparison of Results

The comparison between the results obtained in Israel and the results obtained in Karlsruhe was made by assuming the law $dk/dT = A/T$ for the change of neutron multiplication factor with temperature.

Table 10 summarizes the results obtained in Karlsruhe and Israel for the same systems. The results obtained at Karlsruhe are systematically greater than those obtained in Israel. A thorough comparison of the methods and data used has shown that practically the results obtained for $\Delta\Sigma$ or $d\Sigma/dT$ are the same. It is estimated that the cumulative error in these figures (error introduced by the various approximation in the calculation of $\Delta\Sigma$ and Δk) are of the order of 1%.

Under these conditions it follows that the difference between the results is caused by the use of different basic data. It is known^(18, 19) that the YOM cross-section set yields a harder neutron spectrum than that obtained by the use of the cross-section set of Abagjan et al.⁽¹⁸⁾ This will result in a smaller number of neutrons at low energies; hence, the Doppler effect which comes into play at low energies will have less neutrons to act on.

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Discussion

Mr. Till (ANL): We have just recently completed a series of measurements with ZPR-6 on the temperature dependence of the Doppler effect from 300 to 1100°K ; this was for a 2600-liter carbide core. The temperature dependence of the U^{238} Doppler effect seems to be almost exactly as $1/T$ between those temperatures, to within 2 or 3%.

Mr. Hummel (ANL): I would like to go a little more deeply into the discrepancy between the Karlsruhe results and those given by Yiftah. It seems to me that a discrepancy of this magnitude could be due only to a large disparity in the number of low-energy neutrons. It certainly is not due to differences in the resonance parameters of U^{238} or anything like that. When you talked about spectrum, is what you were referring to that the effect of elastic removal cross sections at low energy was determined to see how many neutrons were slowed down to below 25 keV or something similar?

Mr. Ott: Yes.

Mr. Ancarani (ANL): The dependence of x as shown by you seems to be related to the y value. Were the compositions of the reactors the same as far as coolant and structure material are concerned, or was there a change?

Mr. Ott: No, only the enrichment was changed. All of the ingredients of the reactor were the same.

Effect of the Fluctuations in Collision Density on Fast-reactor Doppler Effect Calculations

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I. Introduction

Because of its simplicity, the narrow-resonance (NR) approximation has been most widely used in studies of the Doppler effect for large fast reactors. In this approximation, the neutron collision density is taken to be constant in lethargy and temperature not only over a particular resonance, but also over a lethargy interval containing many resonances. Although the applicability of the NR approximation that greatly simplifies the Doppler calculation has been gratifying, it is important to know, however, whether or not the approximation introduces errors.

Even though all resonances in the energy region of interest for the fast reactor studies are nearly always narrow compared to the maximum increment in lethargy per collision, the collision density, strictly speaking, is lethargy and temperature dependent. Since it acts as a negative or positive source, a resonance will introduce localized transients in collision density, which are sometimes referred to as Placzek oscillations. To make the problem even more complicated, the fluctuation in collision density caused by resonances of one substance may affect the resonance integrals of other substances in the mixture. Hence, the resonance integral of a particular resonance depends, to some extent, on the resonance scattering and absorbing characteristics of resonances of the same and different nuclides at higher energies. Since the Doppler effect is accumulative from resonance to resonance over a rather broad energy span (~ 300 eV to 10 keV), these temperature-dependent and nonasymptotic contributions in the collision density may play a role in the Doppler studies.

The primary purpose of this paper is to investigate the behaviors of the fluctuations in collision density and their influence on the Doppler calculations for a large fast reactor. The present work is restricted to the case of an infinite homogeneous medium with composition typical of a dilute fast reactor. Three special topics concerning the Doppler theory have been investigated:

- (1) effect of Placzek oscillations on the reaction rate of individual resonances;
- (2) effect of incomplete recovery in neutron collision density between resonances on the group reac-

tion rates and the effective cross section of a single resonant material in the mixture of heavy scatterers;

- (3) a more general approach to the problem of resonance overlap between two different nuclides.

Investigations were carried out both analytically and numerically. The first and second topics were intended to illustrate the nature of the problem; the third topic is of great practical importance. An approximate method which accounts for the fluctuations in the collision density has been developed in order to treat the problem of resonance overlap in the region where resonances are only partially resolved for the fertile isotopes and completely unresolved for the fissile isotopes. It was found that the errors introduced by the fluctuation in the collision density are generally small when the total Doppler effect of the system is considered, even though the NR approximation may become unreliable in the lower end of the Doppler region.

II. Physical and Mathematical Aspects of the Slowing-down Problem

With no lack of generality, the well-known slowing-down equation for an infinite homogeneous mixture of N nuclides with a monoenergetic source of unit strength can be described by a Volterra integral equation of the second kind, as pointed out by Corngold.⁽¹⁾ Since, for practical purposes, the energy region of interest is well below the source energy, an arbitrary lower lethargy limit u_1 is chosen so that the collision density below u_1 is assumed to be uniform and unperturbed by the resonance structure. Hence, Corngold's equation can be rewritten into the following form:

$$\begin{aligned}
 F(u) &= \bar{\xi}P(u) - \int_0^u K(u,u')F(u')du' \\
 &\cong 1 - \int_{u_1}^u K(u,u')F(u')du'; \quad (1) \\
 u &> u_1 \gg 0,
 \end{aligned}$$

where $F(u)$ is the collision density normalized to its asymptotic value $1/\bar{\xi}$, with $\bar{\xi}$ the average lethargy

increment per collision for the mixture, and the kernel $K(u, u')$ is to be defined. $P(u)$ is the Placzek function^(1, 2) for the nonabsorptive mixture which assumes its asymptotic value $1/\bar{\xi}$ far from the source energy.

It is quite clear from Eq. (1) that the perturbation in $F(u)$ caused by resonances depends entirely on the characteristics of $K(u, u')$. This kernel $K(u, u')$, given in Ref. 1, can be rewritten into the following form:

$$\begin{aligned} K(u, u') &= \sum_1^N G_N(u - u') g_N(u') \\ &= P(u - u') \sum_1^h \frac{\Sigma_{Rh}(u')}{\Sigma_t(u')} \\ &\quad - \sum_1^h G_h(u, u') \frac{\Sigma_{RSh}(u')}{\Sigma_t(u')}, \end{aligned} \quad (2)$$

with

$$g_N(u') = \frac{\Sigma_{SN}(0)}{\Sigma_t(0)} - \frac{\Sigma_{SN}(0) + \Sigma_{RSN}(u')}{\Sigma_t(u')}, \quad (3)$$

where h designates resonance absorbers only, $\Sigma_{Rh}(u')$ and $\Sigma_{RSh}(u')$ total macroscopic resonance cross section and resonance scattering cross section of resonance absorber h , respectively, and $\Sigma_t(u')$ total macroscopic cross section of the mixture:

$$\Sigma_t(U) = \Sigma_t(0) + \sum_1^h \Sigma_{Rh}(u).$$

The function $P(u - u')$ is the Placzek function⁽²⁾ for the mixture, defined as

$$P(X) = \sum_1^N \frac{\Sigma_{SN}(0)}{\Sigma_t(0)} G_N(X), \quad (4)$$

FIG. 1. Variations of Functions $P(u - u')$ and $G_h(u - u')$.

and the Green's function $G_N(u - u')$ introduced by Corngold is the solution of the following equation:

$$G_N(u) = \left[\sum_{r=1}^N \frac{\Sigma_{Sr}(0)}{\Sigma_t(0)} \cdot \int_{u-\epsilon_r}^u du' \frac{e^{u'-u}}{1 - \alpha_r} G_N(u') \right] + S_N(u), \quad (5)$$

with

$$\begin{aligned} S_N(u) &= \frac{e^{-u}}{1 - \alpha_N} \quad \text{for } 0 < u < \epsilon_N \\ &= 0 \quad \text{for } u > \epsilon_N^+; \\ \alpha_N &= \left(\frac{A_N - 1}{A_N + 1} \right)^2; \end{aligned}$$

ϵ_N = maximum increment in lethargy per collision for nuclide N ;

A_N = atomic mass for nuclide N .

Note that neither $G_N(X)$ nor $P(X)$ depends on the resonance properties of the nuclides in the mixture. $G_N(u - u')$ can be pictured as the kernel describing neutrons whose first collision is with nuclide N , as pointed out by Corngold.

A. Characteristics of the Kernel $G_h(u - u')$ and $P(u - u')$ in a Typical Fast System

Consider an infinite homogeneous medium with two heavy absorbers with approximately the same atomic weight, presumably fertile and fissile isotopes, mixed with a number of scatterers. Let the subscripts h ($=1$ and 2) and m ($1, 2, \dots, N - h$) designate the heavy absorbers and background scatterers, respectively. Note that $\epsilon_h = \epsilon_1 \cong \epsilon_2$. It is generally true for a large fast system that $\epsilon_m \gg \epsilon_h$. The smallest ϵ_m for such system is that of stainless steel, which is more than 4 times larger than ϵ_h . By using the same techniques given by Placzek⁽²⁾ and directly differentiating Eq. (5), one obtains a first-order inhomogeneous differential equation for $G_N(u)$, which has a solution of the following form:

$$\begin{aligned} G_N(u) &= -e^{(Q-1)u} \int du' \sum_{r=1}^N \frac{\alpha_r}{1 - \alpha_r} \frac{\Sigma_{Sr}(0)}{\Sigma_t(0)} \\ &\quad \cdot G_N(u' - \epsilon_r) e^{(Q-1)u} + c e^{(Q-1)u} \quad \text{for } u \geq 0, \end{aligned} \quad (6)$$

where the constant c is to be determined by appropriate initial and boundary conditions, and

$$Q = \sum_1^N \frac{1}{1 - \alpha_N} \frac{\Sigma_{SN}(0)}{\Sigma_t(0)}. \quad (7)$$

By stepwise integration, Eq. (6) can be solved quite

readily at least for the first few intervals. The results are given in Appendix 1.

Figure 1 shows the variations of $G_h(u - u')$ and $P(u - u')$ normalized to $(1 - \alpha_h)$ versus $(u - u')$ for a system typical of a dilute fast reactor composition given in Table 1. As expected, $P(u - u')$ is relatively flat and insensitive to $(u - u')$, due to the competition in neutron scattering among all scatterers in the mixture. On the other hand, $G_h(u - u')$, which is the same for fertile and fissile isotopes with approximately identical $(1 - \alpha_h)$ values, is significantly higher than $P(u - u')$ in the first collision interval of $0 \leq u - u' \leq \varepsilon_h$ as a sharp discontinuity occurs at $u - u' = \varepsilon_h$. It is interesting to note that $G_h(u - u')$ has only one jump at $u - u' = \varepsilon_h$, whereas the jumps for $P(u - u')$ occur at all $u - u' = \varepsilon_N$. The second jump in $P(u - u')$ which appears in Fig. 1 occurs where $u - u'$ is equal to the maximum increment in lethargy per collision of stainless steel. It is quite clear from Fig. 1 that $G_h(u - u')$ and $P(u - u')$ become almost identical for $u - u' > \sim 2\varepsilon_h$ as both functions rapidly approach their common asymptotic value $1/\xi$. It is not difficult to see from Eq. (1), Eq. (2), and Fig. 1 that there is a sudden rise in $F(u)$ beyond its asymptotic value near a resonance whose neutron width is large.

In the limiting case, if there is only one type of nuclide in the mixture (i.e., $N = h = 1$), $G_h(u - u')$ and $P(u - u')$ become identical as $Q = 1/(1 - \alpha_h)$. The dotted line shows $G_h(u - u')$ for a "pure" U^{238} medium.

B. General Solution

In any finite lethargy region $u_1 \leq u' \leq u \leq u_2$ far away from the source, $K(u, u')$ is well defined and bounded everywhere in the triangle to the right of the 45° line as shown in Fig. 2. So long as $K(u - u')$ has only a finite number of discontinuities which are regularly distributed,^(3, 4) Eq. (1) has a unique continuous solution represented by the usual Neumann series:

$$\begin{aligned} F(u) = & 1 - \int_{u_1}^u K(u, u') du' + \int_{u_1}^u K(u, u') du' \\ & \cdot \int_{u_1}^u K(u, u'') du'' - \dots \\ & + (-1)^n \int_{u_1}^u K(u, u') du' \\ & \dots \int_{u_1}^u K(u^{(n-1)}, u^{(n)}) du^{(n)}, \end{aligned} \quad (8)$$

provided that the series converges uniformly in $u_1 \leq u \leq u_2$. Since both $P(u - u')$ and $G_h(u - u')$ are bounded, and the reaction rates in the interval $u_2 - u_1$

TABLE 1. ATOM DENSITY FOR THE FAST REACTOR UNDER STUDY

	Material				
	U^{238}	Pu^{239}	SS	Na	O
Atoms/cc	0.00637	0.00098	0.01355	0.01144	0.01568
$\sigma_s(0)$, b	10	10	10	3	3.5
$1 - \alpha_N$	0.01667	0.01667	0.0689	0.1597	0.2215

$$Q = 22.931$$

FIG. 2. The Region where $F(u)$ is Defined.

must be finite, it can be shown quite readily that the n th-order term in Eq. (8), $F_n(u)$, is convergent for

$$|F_n(u)| < \frac{M^n}{n!}; \quad u_1 \leq u \leq u_2, \quad n = 0, 1, 2, \dots,$$

where M is a positive and finite constant. Consequently, the series in Eq. (8) converges absolutely and uniformly in the interval $u_1 \leq u \leq u_2$.

Even though Eq. (8), in general, cannot be described explicitly by a simple analytical function of the closed form, it will lead to the following approximations under certain conditions:

- (1) Wigner's approximation;^(1, 5, 13)
- (2) Spinney's approximation⁽⁶⁾ for narrow and "isolated" resonances;
- (3) first-order approximation for the relatively high-energy region where $F(u)$ can be described adequately by its zero and first-order terms.

Wigner's approximation does not account for the Placzek oscillations characterized by the nonuniform part of the kernel $G_N(u - u')$. Spinney's approximation and the first-order approximation, however, are believed to be very useful in the Doppler effect studies for fast reactors and will be further discussed.

III. Placzek Oscillations due to "Isolated" Resonances

In contrast to the Wigner's approximation, the nonasymptotic behaviors of $F(u)$ near a resonance can

be estimated rigorously if 1) the extent of the resonance under consideration is small compared to the maximum lethargy increment per collision and 2) $F(u)$ recovers between resonances, or, in other words, resonances are considered as "isolated." Under these conditions, the kernel $G_N(u - u')$ in Eq. (8) can be replaced by its value in the first collision interval $0 < u - u' \leq \epsilon_h$ as given by Eq. (3A) and Eq. (6A). By substituting Eq. (3A) and Eq. (6A) of Appendix 1 into Eq. (8) and changing the order of integration, the collision density over a particular resonance under consideration becomes

$$\begin{aligned} F(u) &= 1 - \int_{u_1}^u e^{(Q-1)(u-u')} \sum_1^N \frac{g_N(u')}{1 - \alpha_N} du' \\ &\quad + \int_{u_1}^u du'' e^{(Q-1)(u-u'')} \sum_1^N \frac{g_N(u'')}{1 - \alpha_N} \\ &\quad \cdot \int_{u_1}^{u''} \sum_1^N \frac{g_N(u')}{1 - \alpha_N} du' \dots \\ &= 1 - \int_{u_1}^u e^{(Q-1)(u-u')} \sum_0^n \frac{(-1)^n}{(n+1)!} \\ &\quad \cdot \left(\sum_1^N \frac{g_N(u')}{1 - \alpha_N} \right) \left[\sum_1^N \int_{u_1}^u \frac{g_N(u'')}{1 - \alpha_N} du'' \right]^n. \end{aligned} \quad (9)$$

Equation (9) can also be obtained by an alternative approach in which the method of the resolvent kernel⁽⁴⁾ is used for solving Eq. (1). It is interesting to note that Eq. (9) becomes a simple analytical function of the closed form if $e^{(Q-1)(u-u')} \approx 1$. The latter condition is valid so long as the extent of the resonance is much smaller than ϵ_h . Hence, over an isolated narrow resonance, one has

$$\begin{aligned} F(u) &= \exp - \left[\int_{u_1}^u \sum_1^N \frac{g_N(u')}{1 - \alpha_N} du' \right] \\ &= \exp \left\{ - \int_{u_1}^u \left[Q \frac{\Sigma_{Rk}(u')}{\Sigma_t(u')} \right. \right. \\ &\quad \left. \left. - \frac{1}{1 - \alpha_n} \frac{\Sigma_{Rsh}(u')}{\Sigma_t(u')} \right] du' \right\}, \end{aligned} \quad (10)$$

which is exactly identical with the generalized form of Spinney's approximation⁽⁶⁾ obtained by an entirely different approach.

The "moderator effect" is included in Eq. (10). The presence of heavy scatterers in the mixture tends to reduce the sudden rise in $F(u)$ near the resonance, as clearly indicated in Eq. (10). Equation (10) becomes identical with the form given in Ref. 6 if the moderator effect is ignored, i.e.,

$$Q \approx \frac{1}{1 - \alpha_h} \frac{\Sigma_{sh}(0)}{\Sigma_t(0)}.$$

Under conditions that Eq. (10) is valid, the resonance integral for an isolated resonance can be written as

$$\begin{aligned} \text{RI} = (\text{RI})_{\text{NR}} \left\{ 1 + \sum_1^m \left[\left(\frac{\Gamma_n/\Gamma}{1 - \alpha_n} - Q \right) \right. \right. \\ \left. \left. \cdot \frac{\Gamma}{\sigma_p \Gamma_x} (\text{RI})_{\text{NR}} \right]^m / (m+1)! \right\}, \end{aligned} \quad (11)$$

where Γ_n , Γ_c , and Γ are neutron width, capture (or fission) width, and total width, respectively, and $(\text{RI})_{\text{NR}}$ is the resonance integral obtained by the narrow-resonance approximation.

Equations (10) and (11) can be used as a powerful tool in determining the effect of Placzek oscillation in the Doppler region where resonances are always narrow.

It is apparent from Eq. (11) that, for an isolated resonance, the NR approximation underestimates or overestimates the resonance integral depending on whether $\Gamma_n/\Gamma >$ or $< (1 - \alpha_n)Q$. For compositions typical of a large fast reactor with high concentrations of both stainless steel and sodium, Q is of the order of 20. Hence, $Q(1 - \alpha_n)$ is of the order of $1/3$. Table 2 gives some representative values of resonance integrals of U^{238} for the fast system described in Table 1 as obtained by NR approximation, Spinney's approximation, and the exact calculation of MISH-MASH* Code,⁽⁷⁾ respectively. The NR approximation based on uniform $F(u)$ appears to be reasonably accurate insofar as the magnitude of RI is concerned. Errors ranging from about 10% in $\delta(\text{RI})$ due to temperature were observed for all scattering resonances with large Γ_n , whereas there is virtually no error for resonances with small Γ_n .

Unlike the situation with U^{238} , resonances of Pu^{239} , in general, do not produce drastic perturbation in the localized $F(u)$ due to the relatively small Γ_n/Γ ratio and low concentration. Calculations of the statistically averaged fission and capture rates for Pu^{239} by means of Eq. (11) have been made. No noticeable effect was observed. In order to illustrate the problem of the Placzek oscillation further, calculations of the example given by Codd and Collins⁽⁸⁾ were repeated by using both MISH-MASH Code and an analytical method taking only the zero-order and first-order term in Eq. (8). The results are given in Fig. 3 together with the effect of a Pu^{239} resonance, obtained by use of the average parameters given in Ref. 8. The dotted curve shows the case in which the neutron width was increased five times; no significant effect is observed.

IV. Effect of Incomplete Recovery in $F(u)$ between Resonances for a Single Resonant Substance in the Mixture

A. U^{238} Resonances Alone

The Placzek oscillations in $F(u)$ are somewhat different when a group of resonances are considered. The

* Homogeneous version of RIFF-RAFF Code (Ref. 7).

TABLE 2. REPRESENTATIVE VALUES OF RESONANCE INTEGRALS AND THEIR DOPPLER CHANGE BY VARIOUS METHODS

Resonance Energy, keV	Γ_n	NR Approx.		Spinney's Approx.		Exact (MISH-MASH)	
		RI 500°K	$\delta(\text{RI})$ 1100-500°K	RI 500°K	$\delta(\text{RI})$ 1100-500°K	RI 500°K	$\delta(\text{RI})$ 1100-500°K
1.802	0.67	0.006090	0.000615	0.006281	0.0071	0.006227	0.000707
1.710	0.046	0.012965	0.001847	0.013048	0.001873	0.013079	0.00189
1.428	0.020	0.015390	0.001574	0.015392	0.001598	0.015470	0.001599
1.370	0.0020	0.003834	0.000058	0.003831	0.000058	0.003857	0.000059
1.255	0.236	0.012498	0.002306	0.013053	0.002529	0.012978	0.002552
1.1490	0.274	0.013318	0.002183	0.014060	0.002442	0.013964	0.002472
1.0610	0.051	0.024092	0.004865	0.024426	0.005014	0.02440	0.005020
0.9970	0.35	0.015067	0.001833	0.016290	0.002149	0.016090	0.002190
0.985	0.001	0.003887	0.000490	0.003884	0.000490	0.003910	0.00049
0.7930	0.0024	0.012540	0.000480	0.012507	0.000477	0.012587	0.000480
0.6970	0.057	0.038274	0.00900	0.039258	0.009518	0.039157	0.009594
0.5840	0.026	0.053292	0.011752	0.053785	0.011993	0.053818	0.0120

FIG. 3. Codd and Collins Problem.

localized increase in $F(u)$ due to the resonance scattering of a particular resonance may be partially or completely balanced out by the resonance absorptions of resonances at higher energy. Figures 4 through 6 show some interesting results in $F(u)$ due to U^{238} alone as obtained by MISH-MASH Code in different energy intervals. For energy intervals around or below about 1 keV, where the maximum lethargy increment per collision, ϵ_h , is equal to or smaller than the average spacing of U^{238} , $F(u)$ tends to drift below the asymptotic value as u increases with the exception of few localized rises caused by the resonances with extremely large neutron width. The true group reaction rates in these intervals will be smaller than those obtained by the NR approximation.

On the other hand, for energy intervals beyond about 1.4 keV, where ϵ_h becomes larger than the average spacing between U^{238} resonances, $F(u)$ fluctuates about the asymptotic value with increasingly smaller amplitude at the higher energy interval. Hence, it is not surprising that the group reaction rates obtained by the NR approximation become nearly identical with those obtained by exact calculations beyond ~ 1.4 keV as the nonasymptotic contribution due to individual resonances nearly washes out statistically, as indicated in Table 3. Table 3 also gives the effective cross section for each energy interval as obtained by the two methods.

The NR approximation tends to underestimate $\delta\bar{\sigma}_\gamma$ slightly in the last two groups, while it overestimates $\delta\bar{\sigma}_\gamma$ slightly in the higher-energy groups. Errors in both

the effective cross section and its Doppler change $\delta\tilde{\sigma}_\gamma$ appear to be unimportant.

B. Pu^{239} Resonances Alone

Unlike the case for U^{238} resonances, resonances of fissile isotopes in general exhibit a somewhat different behavior in the Doppler region. Since the absorption of a particular Pu^{239} resonance is nearly always more significant than the corresponding resonance scattering

FIG. 4. Variation of $F(u)$ between 2.5 and 2 keV (U^{238} Resonances Only).

FIG. 5. Variations of $F(u)$ between 1.4 and 1.0 keV (U^{238} Resonances Only).

FIG. 6. Variation of $F(u)$ between 0.55 and 0.3 keV (U^{238} Resonances Only).

on the statistical basis, as indicated by the results of calculation using Eq. (11), the localized collision density is practically unaffected by the existence of a particular Pu^{239} resonance. Instead, $F(u)$ beyond the first collision interval ε_2 from a given resonance will be depleted due to resonance absorption. By referring to Eq. (2) and Fig. 3, it is quite clear that

$$\begin{aligned} K(u, u') &= P(u - u') \frac{\Sigma_{a2}(u')}{\Sigma_t(u')} + \frac{\Sigma_{RS2}(u')}{\Sigma_t(u')} \\ &\quad \cdot [P(u - u') - G_2(u - u')] \\ &\approx P(u - u') \frac{\Sigma_{a2}(u')}{\Sigma_t(u')} \\ &\approx \frac{1}{\xi} \frac{\Sigma_{a2}(u')}{\Sigma_t(u')} \end{aligned} \quad (12)$$

as long as $\Sigma_{a2}(u') \gg \Sigma_{RS2}(u')$. The substitution of Eq. (12) into Eq. (8) gives the well-known Wigner's approximation^(1, 5)

$$F(u) = \exp \left[-\frac{1}{\xi} \int_{u_1}^u \frac{\Sigma_{a2}(u')}{\Sigma_t(u')} du' \right]. \quad (13)$$

TABLE 3. EFFECTIVE CROSS SECTIONS AND GROUP REACTION RATES FOR RESOLVED U^{238} RESONANCES*

Energy Interval, keV	NR Approximation				Exact Calculation (MISH-MASH CODE)			
	$\tilde{R}_\gamma$ at 500°K	$\delta\tilde{R}_\gamma$ 700-1100°K	$\tilde{\sigma}$ at 500°K	$\delta\tilde{\sigma}_\gamma$ 500-1100°K	R_γ at 500°K	$\delta\tilde{R}_\gamma$ 500-1100°K	$\tilde{\sigma}_\gamma$ at 500°K	$\delta\tilde{\sigma}_\gamma$ 500-1100°K
2.5-2.0	0.71161	0.07873	0.7501	0.09012	0.70269	0.07818	0.0749	0.08995
2.0-1.4	0.73080	0.1093	0.7998	0.13035	0.74450	0.10787	0.8086	0.12856
1.4-1.0	0.9895	0.1240	1.0513	0.14275	0.97695	0.10928	1.0885	0.13723
1.0-0.55	0.96510	0.1638	1.0454	0.19659	0.91621	0.14081	1.0753	0.20206
0.55-0.30	0.75019	0.11250	0.7782	0.12290	0.74683	0.10465	0.8641	0.14335

* Fink's⁽¹¹⁾ parameters were used beyond 1802 eV and Columbia Parameters⁽¹²⁾ were used for higher energies

Equation (13) is a relatively smooth function of u within a reasonably small lethargy interval $u_2 - u_1$. As far as the effective group cross section is concerned, the NR approximation is believed to be quite good in the sense that the depletion in the reaction rates due to resonance absorption within the group nearly cancels out the corresponding depletion in the group flux when Eq. (13) is used. To illustrate this point further, calculations have been made with use of the preliminary values of Pu^{239} parameters by Uttley⁽⁹⁾ between 100 eV and 300 eV and by MISH-MASH Code with results given in Fig. 7 and Table 4. It is interesting to note that $F(u)$ given in Fig. 7 can be approximated fairly accurately by use of Eq. (13).

V. First-order Approximations and Group Reaction Rates for Overlapping Resonances in the Doppler Region

For convenience, let

$$F(u) = 1 + T(u), \quad (14)$$

where $T(u)$ represents the nonuniform contributions to $F(u)$ given by the higher-order terms in Eq. (8). Since Eq. (8) is a convergent series within each finite lethargy group considered in multigroup calculations, all higher-order terms can be pictured as n th-order perturbations caused by resonances to the otherwise uniform $F(u)$ of zero order. In particular, for energy region beyond ~ 300 eV and reasonably small group width, the first-order term is believed to be the only significant contributor to $T(u)$ as other higher-order terms are expected to become negligible. For an infinite homogeneous medium consisting of two resonant isotopes and any number of backgroup scatterers, the first-order approximation gives

FIG. 7. Variation of $F(u)$ between 0.3 and 0.1 keV (Pu^{239} Resonances Only).

TABLE 4. EFFECTIVE CROSS SECTIONS FOR RESOLVED Pu^{239} RESONANCES (300-100 eV)

Energy Interval, eV	$\bar{\sigma}_f$ at 500°K	$\delta\bar{\sigma}_f$ at 1100-500°K	$\bar{\sigma}_\gamma$ at 500°K	$\delta\bar{\sigma}_\gamma$ at 1100-500°K
a) NR Approximation				
300-100	14.2846	0.7461	9.3887	0.8059
300-200	12.2455	0.6186	9.5169	0.7314
b) Exact Calculation (MISH-MASH)				
300-100	13.9882	0.6457	9.3530	0.7432
300-200	13.0891	0.6438	9.6364	0.7452

$$T(u) \cong - \sum_1^2 \int_{u_1}^u du \left[P(u-u') \frac{\Sigma_{Rh}(u')}{\Sigma_t(u')} - G_h(u-u') \frac{\Sigma_{RSh}(u')}{\Sigma_t(u')} \right] \quad (15)$$

$$= T_1^{(1)}(u) + T_2^{(1)}(u) + C_{12}(u),$$

where $T^{(1)}(u)$ is the first-order perturbation caused by the resonances of nuclide 1 alone as if the resonances of nuclide 2 did not exist, while $T_2^{(1)}(u)$ is the first-order perturbation due to resonances of nuclide 2 alone. $C_{12}(u)$ is a second-order "interfering" term which is composed of second-order terms such as

$$\int_{u_1}^u \frac{\Sigma_{R1}(u') \cdot \Sigma_{RS2}(u')}{[\Sigma_t(0) + \Sigma_{R1}(u')] \Sigma_t(u')} du', \text{ etc.}$$

Under the condition that the fluctuations caused by the fertile and the fissile isotopes individually are adequately described by their first-order terms $T_1^{(1)}(u)$ and $T_2^{(1)}(u)$, respectively, the quantity C_{12} will be ignored for reasonably small group width. $T_1^{(1)}(u) + T_2^{(1)}(u) \gg C_{12}$ is believed to be true for all cases considered in this work.

A. Effective Cross Section and Group Reaction Rate

The effective cross section of process x for a particular nuclide h within energy interval $E_1 - E_2$ (corresponding to the lethargy interval $u_2 - u_1$) is defined as

$$\bar{\sigma}_{xh} = \frac{\int_{u_1}^{u_2} du \frac{\sigma_{xh}(u) F(u)}{\Sigma_t(u)}}{\int_{u_1}^{u_2} du \frac{F(u)}{\Sigma_t(u)}} = \frac{\tilde{R}_{xh}}{f^*}, \quad (16)$$

where $\tilde{R}_{xh}$ is the group reaction rate of process x for nuclide h weighted by the asymptotic group flux $(u_2 - u_1)/\sigma_{Ph}$ and f^* is referred to as the flux correction factor. Here

$$\sigma_{Ph} = \frac{\Sigma_t(0)}{N_h}$$

and N_h is atom density of nuclide h .

Assume that the separation between any resonance k of nuclide 2 at E_k with respect to a particular resonance i of nuclide 1 at E_i is described by a random distribution since there is no correlation between resonances of 1 and 2. Using the identity

$$\frac{\sigma_{x2}(u)}{\Sigma_i(u)} = \frac{\sigma_{x2}(u)}{\Sigma_i(0) + \Sigma_{R2}(u)} - \left[\frac{\sigma_{x2}(u)\Sigma_{R1}(u)}{[\Sigma_i(0) + \Sigma_{R2}(u)][\Sigma_i(0) + \Sigma_{R1}(u)]} \cdots \right] \quad (17)$$

and substituting Eq. (15) into Eq. (16), one has for unresolved resonances

$$\begin{aligned} \tilde{R}_{x2} \cong & \sum_{\text{spin states}} \left\{ \frac{\sigma_{P2}}{\langle S_2 \rangle} \left\langle \int_{\Delta E} dE \frac{\sigma_{x2}(E)}{\Sigma_i(0) + \Sigma_{R2}(E)} \right\rangle \right. \\ & \cdot \left[1 - \frac{\left\langle \int_{\Delta E} \frac{dE \Sigma_{R1}(E)}{\Sigma_i(0) + \Sigma_{R1}(E)} \right\rangle}{\langle S_1 \rangle} \right. \\ & \left. + \frac{E_2 + E_1}{2\langle S_1 \rangle} \left\langle \int_{\Delta u} du T_1^{(1)}(u) \right\rangle \right. \\ & \left. + \frac{\sigma_{P2}(E_2 + E_1)}{2\langle S_2 \rangle} \right. \\ & \left. \cdot \left\langle \int_{\Delta u} \frac{\sigma_{x2}(u) T_2^{(1)}(u)}{\Sigma_i(0) + \Sigma_{R2}(u)} du \right\rangle \right], \quad (18) \end{aligned}$$

where $\langle \rangle$ is the standard symbol for the statistical averaging over distribution functions of resonance width and, if necessary, over spacings as well. Since $(u_2 - u_1) \cong 2(E_1 - E_2)/(E_1 + E_2)$ for small group width under consideration, the symbol $(E_1 + E_2)/(2\langle s \rangle) \cdot \langle \rangle$ can be replaced by

$$\frac{1}{u_2 - u_1} \int_{u_1}^{u_2}$$

wherever the resolved resonances are considered. Eq. (18) is identical with the usual overlap approximation based on constant $F(u)$ if $T_1^{(1)}(u)$ and $T_2^{(1)}(u)$ are ignored.

The reaction rate for U^{238} , $\tilde{R}_{\gamma 1}$, can be obtained by interchanging the subscripts 1 and 2 in Eq. (18). Similarly, the flux correction factor f^* can be obtained by the same procedure:

$$f^* \cong f + \frac{1}{u_2 - u_1} \sum_1 \left[\int_{\Delta u} du T_h^{(1)}(u) + \int_{\Delta u} du \frac{\Sigma_{Rh}(u) T_h^{(1)}(u)}{\Sigma_i(0) + \Sigma_{Rh}(u)} \right], \quad (19)$$

where f , the flux correction factor when $F(u)$ is taken to constant, is defined in Ref. 10.

The last term in Eq. (18) and Eq. (19) is, in general, rather difficult to evaluate unless the resonances are resolved. For resolved U^{238} resonances, this quantity is

just the difference between the reaction rates obtained by the NR approximation and the exact calculation using MISH-MASH Code when Pu^{239} resonances were ignored. From the values given in Table 3, it is believed that the quantity

$$\frac{\sigma_{P1}}{u_2 - u_1} \int_{u_1}^{u_2} \frac{\Sigma_{\gamma 1}(u) T_1^{(1)}(u) du}{\Sigma_i(0) + \Sigma_{R1}(u)}$$

is negligible compared to the zero-order term in Eq. (18) and Eq. (19) in the high-energy region beyond about 1.4 keV. For Pu^{239} , the latter quantity can be estimated only on the basis of its strong absorbing characteristics and of Eq. (13) discussed in Section IV. Since $T_2^{(1)}(u)$ is essentially lethargy independent in the interval where a given resonance is considered, the quantity

$$\left\langle \int \frac{\Sigma_{r2}(u) T_2^{(1)}(u)}{\Sigma_i(0) + \Sigma_{R2}(u)} du \right\rangle$$

can be approximated by

$$\begin{aligned} & \left\langle \int \frac{\Sigma_{x2}(u) T_2^{(1)}(u)}{\Sigma_i(0) + \Sigma_{R2}(u)} du \right\rangle \\ & \cong \overline{T_2^{(1)}(u)} \left\langle \int \frac{\Sigma_{x2}(u) du}{\Sigma_i(0) + \Sigma_{R2}(u)} \right\rangle, \quad (20) \end{aligned}$$

where $\overline{T_2^{(1)}(u)}$ is taken to be the average neutron loss within a given group, $u_2 - u_1$ containing many resonances, i.e.,

$$\begin{aligned} \overline{T_2^{(1)}(u)} & \cong \frac{1}{2\xi} \int_{u_1}^{u_2} \frac{\Sigma_{\gamma 2}(u) + \Sigma_{f2}(u)}{\Sigma_i(0) + \Sigma_{R2}(u)} du \\ & = \frac{E_1 - E_2}{2\xi E_0} \frac{\langle (\Gamma_f + \Gamma_\gamma) J_2 \rangle}{\langle S \rangle_2}. \quad (21) \end{aligned}$$

Here E_0 is the midpoint between E_1 and E_2 , and J_2 is the usual J function for nuclide 2. It is interesting to note that the quantity

$$\frac{\sigma_{Ph}(E_1 - E_2)}{2\langle S_h \rangle} \left\langle \int \frac{\sigma_{xh}(u) T_h^{(1)}(u) du}{\Sigma_i(0) + \Sigma_{Rh}(u)} \right\rangle$$

for both U^{238} and Pu^{239} becomes negligible in the higher-energy region beyond about 1.4 keV, so that the effective cross section can be approximated by

$$\tilde{\Sigma}_{xh} \cong \frac{\sum \frac{\sigma_{Ph}}{\langle S_h \rangle} \left\langle \int_{\Delta E} dE \frac{\Sigma_{xh}(E)}{\Sigma_i(0) + \Sigma_{Rh}(E)} \right\rangle}{1 - \frac{1}{\langle S_h \rangle} \left\langle \int_{\Delta E} \frac{\Sigma_{Rh}(E) dE}{\Sigma_i(0) + \Sigma_{Rh}(E)} \right\rangle + \frac{E_1 + E_2}{2\langle S_h \rangle} \left\langle \int_{\Delta u} du T_h^{(1)}(u) \right\rangle}. \quad (22)$$

The quantity $\langle \int_{\Delta u} du T_h^{(1)}(u) \rangle$ represents an additional overlap correction term, and this quantity may become of the same order of magnitude as the usual overlap term in the NR approximation,^(8, 10) especially in the energy region below about 1.4 keV. It will be shown that

$\langle \int du T_h^{(1)}(u) \rangle$ is related to J functions of the NR approximation in a rather simple way.

B. Evaluation of $\langle \int_{\Delta u} du T_1^{(1)}(u) du \rangle$

From the discussions given in Section II, it is clear that

$$G_N(u - u') \cong 1/\bar{\xi} \quad (23)$$

for all $2\varepsilon_n^+ \leq u - u'$. Using Eq. (23), and Eqs. (3A), (4A), (6A), and (7A) of Appendix I, and changing the order of integration, one has Eq. (24) (see foot of page). These constants depend only on the relative concentrations and the scattering properties characterized by

TABLE 5. $1/(u_2 - u_1) \int du T_1(u)$ FOR U^{238} IN THE RESOLVED AND UNRESOLVED REGION

Energy Interval, keV	Exact Calculation* (MISH-MASH)		First-order Approximation Eq. - X	
	500°K	1100°K	500°K	1100°K
1.5-9.0	Unresolved Region		+0.01307	+0.01520
9.0-6.0			+0.01266	+0.01544
6.0-4.0			+0.0001885	+0.002384
4.0-2.5			-0.00097	+0.00121
2.5-2.0			+0.01034	+0.01373
2.0-1.4	+0.0036	+0.0049	+0.0041	+0.0046
1.4-1.0	-0.0550	-0.0584	-0.0540	-0.0582
1.0-0.55	-0.0769	-0.0889	-0.0840	-0.0962
0.55-0.30	-0.1018	-0.1147	-0.1023	-0.1175

* Firk's parameters.

atomic weights of each nuclide in the mixture. All integrals in Eq. (2) can be obtained quite readily from any resonance integral code using the NR approximation. For unresolved resonances, Eq. (24) becomes

$$\begin{aligned} & \frac{1}{u_2 - u_1} \int_{u_1}^{u_2} T_h^{(1)}(u) du \\ & \cong \frac{-1}{\langle S_h \rangle} \left\{ \left\langle \left(Q\Gamma_h - \frac{\Gamma_{nh}}{1 - \alpha_n} \right) J_h \right\rangle (A_1 + A_2) \right. \\ & \quad - A_3 \left\langle \left(\frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \Gamma_h - \frac{\Gamma_{nh}}{1 - \alpha_n} \right) J_h \right\rangle \\ & \quad \left. + \ln \frac{\alpha_n^2 E_k}{E_2} \langle (\Gamma_{fh} + \Gamma_{gh}) J_h \rangle \right\}, \end{aligned} \quad (25)$$

where $u_2 - u_1 \gg \varepsilon_h$ and J_h is the usual J function for nuclide h defined in the Ref. 13.

Table 5 shows some results of exact calculations of

$$\frac{1}{u_2 - u_1} \int_{u_1}^{u_2} du T_1^{(1)}(u)$$

in the resolved region of U^{238} using MISH-MASH Code as compared to the approximation using Eq. (24), together with calculations for unresolved region using Eq. (25). Excellent agreement between the exact calculation and the approximation implies that $T_1(u)$ is adequately described by its first-order term $T_1^{(1)}(u)$. It is interesting to note that

$$\frac{1}{u_2 - u_1} \int_{u_1}^{u_2} du T_1^{(1)}(u)$$

becomes small beyond about 1.4 keV as the effect of

$$\begin{aligned} \frac{1}{u_2 - u_1} \int_{u_1}^{u_2} T_h^{(1)}(u) du = & - \frac{1}{u_2 - u_1} \left\{ A_1 \int_{u_1}^{u_2} \frac{\left[Q\Sigma_{Rh}(u') - \frac{1}{1 - \alpha_h} \Sigma_{RSh}(u') \right]}{\Sigma_t(0) + \Sigma_{Ra}(u')} du' \right. \\ & + A_2 \int_{u_1}^{u_2 - \varepsilon_h} du' \frac{\left[Q\Sigma_{Rh}(u') - \frac{1}{1 - \alpha_h} \Sigma_{RSh}(u') \right]}{\Sigma_t(0) + \Sigma_{Rh}} \\ & - A_3 \int_{u_1}^{u_2 - \varepsilon_h} du' \frac{\left[\left(\frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) \Sigma_{Rh}(u') - \frac{1}{1 - \alpha_h} \Sigma_{RSh}(u') \right]}{\Sigma_t(0) + \Sigma_{Rh}(u')} \\ & \left. + \ln \frac{\alpha_n^2 E_k}{E_2} \int_{u_1}^{u_2 - 2\varepsilon_h} \frac{\Sigma_{ah}(u')}{\Sigma_t(0) + \Sigma_{Rh}(u')} du' \right\}, \end{aligned} \quad (24)$$

where

$$\begin{aligned} A_1 &= \frac{e^{(Q-1)\varepsilon_h} - 1}{Q - 1}; \\ A_2 &= \frac{\alpha_h(\alpha_n e^{Q\varepsilon_h} - 1)}{Q - 1} \left[e^{Q\varepsilon_h} + \frac{\left(\frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right)}{(Q-1)(1 - \alpha_h)} \right] - \frac{\alpha_n^2 \varepsilon_h \left(\frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) e^{Q\varepsilon_h}}{(Q-1)(1 - \alpha_h)}; \\ A_3 &= \frac{\alpha_h(\alpha_n e^{Q\varepsilon_h} - 1)}{(1 - \alpha_h)(Q - 1)}; \\ E_k &= \text{resonance energy for the } k^{\text{th}} \text{ resonance within } u_2 - u_1. \end{aligned}$$

the fluctuations in $F(u)$ introduced by resonances of nuclide 1 on nuclide 2 nearly washes out statistically. For energy regions below 1.4 keV, however, the latter quantity is about the same order of magnitude as the overlap correction term obtained by NR approximation.

C. Results of Calculations and Discussions

The quantity $\int T_h^{(1)}(u) du$ in Eq. (18) represents an additional overlap correction term when the reaction rates of U^{238} and Pu^{239} are considered. The presence of this quantity together with the last terms in Eq. (18) will affect the relative magnitudes of the reaction rates in the system. The effect of these additional terms on the total Doppler effect of the system and on the effective cross sections of individual isotopes will be discussed separately in order as follows:

For fast reactors which have a spectrum soft enough to produce a large Doppler effect to be significant for safety, the overall Doppler effect of the system is expected to depend almost entirely on the relative reaction rates of fissile and fertile isotopes, as discussed in References 10 and 14. Hence, the significance of the nonuniform terms in $F(u)$ depends on whether their presence will affect the ratios $\tilde{R}_{\gamma 2}/\tilde{R}_{f 2}$ and $\tilde{R}_{\gamma 1}/\tilde{R}_{f 2}$ at various temperatures. Note that

$$\frac{\tilde{\sigma}_{\gamma 2}}{\tilde{\sigma}_{f 2}} = \frac{\tilde{R}_{\gamma 2}}{\tilde{R}_{f 2}} = \frac{\int \frac{\Sigma_{\gamma 2}(E)}{\Sigma_t(0) + \Sigma_{R2}(E)} dE}{\int \frac{\Sigma_{f 2}(E)}{\Sigma_t(0) + \Sigma_{R2}(E)} dE} \quad (26)$$

is affected neither by the presence of nonuniform terms when Eq. (18) and Eq. (20) are used, nor by the correction terms due to interaction between different nuclides when the usual NR approximation is used.

The question is then whether $R_{\gamma 1}/R_{f 2}$ will be affected.

Table 6 shows the quantities $\nu\tilde{\Sigma}_{f 2}/(\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1})$ at 500°K, and the Doppler change

$$\delta[\nu\tilde{\Sigma}_{f 2}/(\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1})]$$

between 1100 and 500°K obtained by various methods at various energy intervals. The NR approximation with overlap correction seems to overestimate the Doppler change slightly at high energy intervals, and underestimate values slightly below about 1 keV, as compared to the case where the nonuniform contributions are included. Nevertheless, the results indicate that NR approximation is fairly accurate for practical purpose.

Table 7 gives the effective cross section and the change due to temperature obtained by two different methods in which resonance interactions were taken into account. The magnitudes of $\tilde{\sigma}_x$ appears to be almost unaffected by the presences of the nonuniform terms in the collision density, as expected, whereas discrepancies of a few percent in $\delta\tilde{\sigma}_x$ were observed for both U^{238} and Pu^{239} . Since the discrepancies in $\delta\tilde{\sigma}_{\gamma 1}$, $\delta\tilde{\sigma}_{\gamma 2}$, and $\delta\tilde{\sigma}_{f 2}$ are small and oriented in the same direction, the result also indicates that the NR approximation is fairly accurate for practical purpose when the total Doppler effect of the system is considered.

It is important to realize, however, that all results given in Tables 6 and 7 were based on the assumption that the separation between any particular resonance of Pu^{239} with respect to a given resonance of U^{238} has a random distribution. In reality, the reaction rates of Pu^{239} depend to a great extent on the actual location of each Pu^{239} resonance with respect to U^{238} resonances, as indicated by the recent work of Kelber and Kier.⁽¹⁵⁾ Hence, the random distribution may not give an accurate estimation of $\tilde{R}_{\gamma 2}/\tilde{R}_{f 2}$ and $\tilde{R}_{\gamma 1}/\tilde{R}_{f 2}$. As far as the total Doppler effect of a particular group is concerned,

TABLE 6. η AND $\delta\eta$ AT VARIOUS ENERGY INTERVALS BY DIFFERENT METHODS

Energy Interval, keV	NR Approximation (no interaction)		RN Approximation (with interaction)		First-order Approximation [interaction and fluctuation in $F(u)$]	
	$\frac{\nu\tilde{\Sigma}_{f 2}}{\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1}}$	$\delta\left(\frac{\nu\tilde{\Sigma}_{f 2}}{\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1}}\right)$	$\frac{\nu\tilde{\Sigma}_{f 2}}{\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1}}$	$\delta\left(\frac{\nu\tilde{\Sigma}_{f 2}}{\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1}}\right)$	$\frac{\nu\tilde{\Sigma}_{f 2}}{\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1}}$	$\delta\left(\frac{\nu\tilde{\Sigma}_{f 2}}{\tilde{\Sigma}_{f 2} + \tilde{\Sigma}_{\gamma 2} + \tilde{\Sigma}_{\gamma 1}}\right)$
	at 500°K	500–1100°K	at 500°K	500–1100°K	at 500°K	500–1100°K
15–9.0	0.9909	–0.0198	0.9671	–0.0222	0.9748	–0.0212
9.0–4.0	0.9722	–0.0288	0.9463	–0.0325	0.9337	–0.0339
9.0–6.0	0.9750	–0.0267	0.9495	–0.0300	0.9569	–0.0286
6.0–4.0	0.9676	–0.0334	0.9414	–0.0377	0.9434	–0.0367
4.0–2.8	0.9719	–0.0394	0.9460	–0.0446	0.9477	–0.0437
2.8–2.0	0.9971	–0.0445	0.9725	–0.0538	0.9819	–0.0490
2.0–1.4	1.0360	–0.0545	1.0097	–0.0598	1.0169	–0.0603
1.4–1.0	0.9824	–0.0428	0.9714	–0.0466	0.9570	–0.0436
1.0–0.55	1.0875	–0.0548	1.0718	–0.0612	1.0733	–0.0630
0.55–0.30	1.2915	–0.0363	1.2977	–0.0375	1.2818	–0.0420

TABLE 7. EFFECTIVE CROSS SECTIONS (500–1100°K)

a) Pu ²³⁹ *								
Energy Interval (keV)	NR Approximation (no fluctuation)				First Order Approximation (with fluctuation)			
	$\tilde{\sigma}_f$ at 500°K	$\delta\tilde{\sigma}_f$	$\tilde{\sigma}_\gamma$ at 500°K	$\delta\tilde{\sigma}_\gamma$	$\tilde{\sigma}_f$ at 500°K	$\delta\tilde{\sigma}_f$	$\tilde{\sigma}_\gamma$ at 500°K	$\delta\tilde{\sigma}_\gamma$
15–9.0	1.2007	0.00355	0.6573	0.00189	1.2017	0.003277	0.6579	0.001735
9.0–4.0	1.8040	0.00978	1.0147	0.00542	1.8279	0.008452	1.0282	0.004672
9.0–6.0	1.5430	0.00775	0.9184	0.00426	1.6433	0.007067	0.9185	0.003871
6.0–4.0	2.1356	0.01490	1.2149	0.00843	2.1393	0.013407	1.2171	0.007577
4.0–2.8	2.7187	0.02719	1.5722	0.01586	2.7231	0.024493	1.5748	0.013401
2.8–2.4	3.4864	0.05056	2.0504	0.03047	3.4980	0.045594	2.0572	0.027557
2.0–1.4	4.1013	0.07580	2.4382	0.04676	4.1077	0.0703348	2.4420	0.043518
1.4–1.0	4.9777	0.12184	2.9964	0.07741	5.0190	0.1016004	3.0213	0.065246
1.0–0.55	6.2564	0.21209	3.8180	0.13987	6.3552	0.1867786	3.8783	0.124549
0.55–0.30	8.3160	0.41194	5.1450	0.28603	9.0783	0.4487196	5.6166	0.311630

b) U ²³⁸ **				
Energy Interval (keV)	NR Approximation (no fluctuation)		First Order Approximation (with fluctuation)	
	$\tilde{\sigma}_\gamma$ at 500°K	$\delta\tilde{\sigma}_\gamma$	$\tilde{\sigma}_\gamma$ at 500°K	$\delta\tilde{\sigma}_\gamma$
15–9.0	0.2661	0.01384	0.2619	0.12944
9.0–4.0	0.4139	0.03265	0.4310	0.03498
9.0–6.0	0.3752	0.02702	0.3693	0.02528
6.0–4.0	0.4931	0.04583	0.4917	0.04421
4.0–2.8	0.6176	0.07008	0.6162	0.06793
2.8–2.0	0.7420	0.10535	0.7291	0.09366
2.0–1.4	0.7998	0.13035	0.7882	0.12846
1.4–1.0	1.0513	0.14275	1.0947	0.13511
1.0–0.55	1.0454	0.19659	1.0580	0.19848
0.55–0.30	0.7782	0.12290	0.8881	0.15072

* $\nu = 1$ distribution for neutron width and $\nu = 2$ for fission width; $\langle\Gamma_f\rangle = 0.099$ eV; strength functions = 10^{-4} .

** Fink *et al.* parameters below 1.8 keV.

$\nu = 1$; strength function = 10^{-4} for unresolved resonances.

however, the NR approximation and the assumption of random separation probably will not lead to significant errors in the energy group beyond about 1.4 keV for the following reasons:

1. The localized variation in the collision density becomes relatively insensitive to the exact location of a particular resonance in the high-energy region, where the maximum energy loss per collision becomes large compared to the average spacing of each heavy nuclide.
2. U²³⁸ is the principal contributor to the total Doppler effect of the group. The relative location of a particular Pu²³⁹ resonance with respect to a U²³⁸ resonance is not likely to affect the ratio $\tilde{\Sigma}_{\gamma 1}/\tilde{\Sigma}_{f 2}$ significantly.
3. The presence of many resonances within a given group provides better statistics.

On the other hand, for lower-energy groups in which Pu²³⁹ resonances are significant, the error introduced by the usual NR approximation may become much greater than those inferred by Tables 6 and 7.

TABLE 8. CALCULATIONS OF CAPTURE-TO-FISSION RATIOS USING RESOLVED PARAMETERS FOR BOTH Pu²³⁹ AND U²³⁸

	NR Approximation		Exact Calculation	
	Without Interaction	With Interaction	Without Interaction	With Interaction
$\tilde{\Sigma}_{\gamma 2}/\tilde{\Sigma}_{f 2}$ at 500°K	0.6572	0.6572	0.6686	0.6939
$\delta(\tilde{\Sigma}_{\gamma 2}/\tilde{\Sigma}_{f 2})$, 500–1100°K	0.0209	0.0209	0.0212	0.0286

Table 8 shows some results of NR approximation and the exact calculations using resolved parameters^(9, 12) for both Pu²³⁹ and U²³⁸ between 300 and 100 eV. The results also indicate that, contrary to Eq. (26), the α -value $\tilde{\Sigma}_{\gamma 2}/\tilde{\Sigma}_{f 2}$ and its Doppler change are affected by the resonance interaction. For this reason, the statistical treatment may not be adequate in the low-energy groups when the resonance interaction is considered.

VI. Conclusion

In conclusion, the results of this study can be summarized as follows:

1. On the Resonance Integral of Individual Resonances

In the energy region of interest for Doppler studies of a large fast reactor, the effect of Placzek oscillation, which can be estimated accurately by Spinney's approximation, depends strongly on the relative magnitude of the neutron width and absorption width of the resonance. For U^{238} the NR approximation tends to underestimate the Doppler change in the resonance integral by about 10% for cases where $\Gamma_n \gg \Gamma_\gamma$, although it introduces practically no error for cases where Γ_n is comparable or less than Γ_γ . For Pu^{239} resonances the NR approximation is believed to be accurate.

2. On the Effective Cross Section of a Single Resonant Absorber in the Mixture

For U^{238} small discrepancies in $\bar{\sigma}_\gamma$ and $\delta\bar{\sigma}_\gamma$ obtained by the NR approximation and the exact calculation were observed in the low-energy groups. Neither the effective cross section nor the group reaction rate will be affected beyond about 1.4 keV, for the collision density begins to oscillate about its asymptotic value. For Pu^{239} , the effective cross section is nearly unaffected by the depletion in the collision density within the group so long as the group width is reasonably small. The NR approximation is believed to be accurate.

3. On the Total Doppler Effect when Resonance Interactions are Considered

The NR approximation appears to overestimate the Doppler effect slightly in all high-energy groups beyond about 1 keV, whereas it underestimates the Doppler effect in the low-energy groups. In general, the NR approximation is believed to be fairly accurate for practical purposes as far as the overall Doppler effect of a large fast reactor is concerned. The statistical treatment used in this work, however, may introduce

noticeable errors in the lower-energy groups for which the Doppler effect within a given group depends to a great extent on the actual locations of Pu^{239} resonances with respect to given U^{238} resonances.

Acknowledgment

The author is indebted to Dr. P. H. Kier for providing the MISH-MASH Code prior to its publication. Discussions with Dr. H. H. Hummel have been very helpful on this subject.

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Appendix I

The constant C in Eq. (6) can be determined by the initial condition

$$G_N(0^+) = \frac{1}{1 - \alpha_N} \quad (1A)$$

and the boundary condition

$$G_N(n\epsilon_n^+) = \lim_{\delta \rightarrow 0} G_N(n\epsilon_N + \delta), \quad (2A)$$

where $G_N(X)$ is defined by Eq. (5) and (6). By stepwise integrations, one has

(1) for heavy absorbers of approximately the same atomic weight,

$$G_h(X) = \frac{e^{(Q-1)X}}{1 - \alpha_h} \quad \text{when } 0 \leq X \leq \epsilon_h^-; \quad (3A)$$

$$= \frac{e^{(Q-1)X}}{1 - \alpha_h} \left[1 - \left(1 - \frac{\epsilon_h - X}{1 - \alpha_h} \cdot \frac{\sum_{sh}(0)}{\sum_t(0)} \right) e^{-Q\epsilon_h} \right] \quad \text{when} \quad \epsilon_h^+ \leq X \leq 2\epsilon_h^-; \quad (4A)$$

$$\begin{aligned}
&= \frac{e^{(Q-1)X}}{1 - \alpha_n} \left[1 - e^{-Q\varepsilon_h} \frac{\varepsilon_h e^{-Q\varepsilon_h}}{1 - \alpha_n} \sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right] \\
&\quad - \frac{\alpha_h e^{(Q-1)(X-\varepsilon_h)}}{(1 - \alpha_h)^2} \cdot \left(\sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) \\
&\quad \cdot \left[(X - 2\varepsilon_h) \left(1 - e^{-Q\varepsilon_h} \right) \right. \\
&\quad \left. + \frac{\varepsilon_h e^{-Q\varepsilon_h}}{1 - \alpha_h} \sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right] \quad (5A) \\
&\quad - \frac{(X - \varepsilon_h)^2 - \varepsilon_h^2}{2(1 - \alpha_h)} \\
&\quad \cdot \left(\sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) e^{-Q\varepsilon_h} \Bigg] \text{ when } 2\varepsilon_h^+ \\
&\leq X \leq 3\varepsilon_h^-;
\end{aligned}$$

etc.

(2) for moderators in the mixture,

$$G_m(X) = \frac{e^{(Q-1)X}}{1 - \alpha_m} \text{ when } 0 \leq X \leq \varepsilon_h^-; \quad (6A)$$

$$\begin{aligned}
&= \frac{e^{(Q-1)X}}{1 - \alpha_m} \left[1 + \frac{\varepsilon_h - X}{1 - \alpha_h} \right. \\
&\quad \cdot \left(\sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) e^{-Q\varepsilon_h} \Bigg] \text{ when } \quad (7A) \\
&\quad \cdot \varepsilon_h^+ \leq X \leq 2\varepsilon_h^-;
\end{aligned}$$

$$\begin{aligned}
&= \frac{e^{(Q-1)X}}{1 - \alpha_m} \left[1 - \frac{\varepsilon_h e^{-Q\varepsilon_h}}{1 - \alpha_h} \right. \\
&\quad \cdot \left(\sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) \\
&\quad - \left(\frac{\alpha_h}{1 - \alpha_h} \sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) \\
&\quad \cdot \frac{e^{(Q-1)(X-\varepsilon_h)}}{1 - \alpha_m} \left[(X - 2\varepsilon_h) \right. \\
&\quad \cdot \left(1 + \frac{\varepsilon_h e^{-Q\varepsilon_h}}{1 - \alpha_h} \sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) \Bigg] \\
&\quad - \frac{(X - \varepsilon_h)^2 - \varepsilon_h^2}{2(1 - \alpha_h)} \quad (8A)
\end{aligned}$$

$$\begin{aligned}
&\cdot \left(\sum_1^2 \frac{\Sigma_{sh}(0)}{\Sigma_t(0)} \right) e^{-Q\varepsilon_h} \text{ when} \\
&2\varepsilon_h^+ < X < 3\varepsilon_h^-.
\end{aligned}$$

Discussion

Mr. Chernick (BNL): I think you showed that at low energies it does make a difference just what the resonances are. At the energies of interest to fast reactors, that is, in the keV range, many resonances are smeared together into big blocks and lumps. What do you hope to do to resolve this into real resonances and to try to get at the real effect of interactions?

Mr. Hwang: We need more parameters to solve that problem. In my opinion, we should not push our argument too far down as regards energy, that is, below 1 keV.

Mr. Chernick: It is a little work, but could you get a set of random resonances that will be reasonable and will still fit what has been seen on, say, the BNL cross-section curves?

Mr. Hwang: This is a problem at which Dr. Kelber and Dr. Kier at Argonne are looking. I think I will pass that question to Dr. Kelber.

Mr. Kelber (ANL): We have attempted to verify a conjecture of Brissenden's which has been referred to earlier this morning, namely, that one can compute the effects of the unresolved resonances by simply sampling from the distribution and producing a set of parameters which obey all the known statistical laws. We have not attempted to do what Brissenden has suggested, that is, use forced sampling in those regions where we have broad resolution measurements. What we have directed our attention to is the variance in the results obtained by use of a number of different samples. I think it would be premature at this time to draw any broad conclusions. We have been able to show that the variance observed for a particular example is large, but for many cases of practical calculation is tolerable. This is the best we can do with the present structure. If this problem is to be resolved further, we have to introduce such schemes as forced sampling to take into account broad resolution measurements. This should remarkably reduce the variance because these measurements are in general available in just the regions of highest statistical worth.

Comparison of Monte Carlo and Resonance Integral Methods in the Determination of Doppler Effects in Fast Reactors

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1. Introduction

This paper describes a comparison of the calculations by the Monte Carlo method and resonance integral techniques, of Doppler effect in fast reactors. These comparisons have been based on a common set of ground rules which have been selected to minimize the effect of uncertainties in nonfuel cross sections. In this way discrepancies in results can be attributed to the differences in the treatments of the Doppler-broadened cross sections and the slowing-down models.

With the Monte Carlo method the physical behavior of neutrons in a reactor is simulated on a digital computer. Neutrons can be introduced into the reactor system, distributed in energy according to the fission spectrum, and followed in spatial propagation and in energy degradation as they experience scattering collisions with various nuclei until absorption occurs. The reactor geometry can be duplicated to a high degree of detail. The collection of statistical data as the neutrons slow down can provide information of capture rates, fission rates, scattering rates, flux distribution and spectrum; also values of derivatives of absorption rates with respect to some parameter (i.e., temperature) can be determined. In principle, Monte Carlo can be as accurate as the theoretical models describing microscopic interactions between neutrons and nuclei. The resonances of the light elements as well as heavy elements could be included in a Monte Carlo algorithm. In practice, because of computer size limitations and economies of running times, the resultant Monte Carlo model usually represents a departure from a true physical mockup while retaining the features important to the application under consideration. Furthermore, like physical experiments, Monte Carlo "experiments" have

statistical uncertainties inherent in the results due to the probabilistic nature of the calculation.

Resonance integral theory has been the standard method of calculating the Doppler coefficient in fast reactors.⁽¹⁻¹⁰⁾ Because of the rapidly varying flux spectrum over the energy range important to the Doppler coefficient, the gross slowing-down problem is generally treated by multigroup techniques, using Doppler-broadened, self-shielded cross sections obtained from the resonance-integral calculations. The purpose of this paper is to make a comparison between this method and Monte Carlo.

2. Monte Carlo Method

The Monte Carlo calculations were performed with the REPETITIOUS 3 code, which simulates the neutron slowing down and the cell geometry exactly. The original Monte Carlo code REP developed by Richtmyer *et al.*⁽¹¹⁻¹³⁾ permits only a single fertile resonance absorber, generally U-238. In order to study resonance interference effects between isotopes, this code was extended, as REPETITIOUS 3, to include a second resonance isotope which may have resonance characteristics of a fissile material, for example, Pu-239.

In the case of the fertile resonances, the REP treatment is retained intact; the Doppler-broadened cross sections are based on the Breit-Wigner single-level formula and are calculated for each neutron energy as needed in the Monte Carlo chains from tabulations of the Doppler-broadening functions ψ and χ . Above 1 keV, parameters for the unresolved resonances are taken as the proper average except for Γ_n^0 , which is selected from the Porter-Thomas distribution.

Due to storage limitations in the computer core and

the necessity of preserving reasonable execution time, certain approximations are made in regards to the second absorber. These relate to the explicit representation of the Doppler-broadened resolved and unresolved resonances for a fissile isotope in a Monte Carlo calculation. For the fissile absorber it is assumed that the Doppler-broadened cross sections may be represented by a sum of Gaussian-shaped pseudo-broadened functions each of which has the correct peak and integral value for the resonance it represents (details are given in Appendix A). Thus, the resonance parameters are broadened rather than the line-shape function. This parameter broadening is done for both resolved and statistically determined unresolved resonances. For the unresolved resonances, sets of resonance parameters and level spacings are selected from appropriate chi-squared distributions and then broadened. In order to obtain statistical averaging, a new set is selected by REPETITIOUS 3 after every n histories (n is arbitrary).

For a given neutron energy in the Monte Carlo slowing-down process, two neighboring fertile resonances and generally 20 neighboring unresolved fissile resonances are considered in determining the respective cross sections. Below 53 eV all resolved fissile resonances are included. In this way interference effects between resonances of the two different species and the overlap of the same species are taken into account. Further details of the REPETITIOUS 3 code are described in Appendix A.

3. Resonance Integral-Multigroup Method

In order to determine the Doppler effect on the same basis as in the Monte Carlo method, the resonance-integral technique is coupled with multigroup analysis.

The details of the resonance-integral method used in this study have been reported by Hwang^(14, 15) and will only be summarized here. The narrow-resonance approximation is used to determine the effective cross section of a given process exhibiting resonance phenomena. The total collision density is assumed to be constant in energy over a narrow bandwidth. The total macroscopic cross section in this instance includes the effective background scattering cross section and the total resonance cross section of each isotope present. In all cases the Breit-Wigner single-level formula is assumed and the Doppler broadening is accounted for by the usual ψ function. Interference between potential and resonance scattering is neglected; however, interference or overlap between resonances of the same or different species is included as discussed in the following paragraph.

As a result of the analyses reported in references 14

and 15, the effective cross section for a resonance absorber in a fast reactor spectrum can be described as the effective cross section determined by the isolated resonance approximation minus a correction term. The correction term embodies the effects of self-overlap of resonances of the same species and interference interactions of resonances of different species. This correction term can be evaluated by series expansion. For neutron energies below about 25 keV the resonances of the fertile isotope are assumed isolated, since its average separation is, in general, greater than its Doppler width. Therefore, the interference of only the nearest fertile resonance upon a set of fissile resonances is considered. However, self-overlap of many fissile resonances is included. The treatment for the unresolved resonances is performed by statistical averaging over the appropriate χ^2 distribution.

The Doppler effect on reaction rates is determined by employing the temperature-dependent effective cross sections of the resonance absorbers in calculations based on multigroup diffusion theory. The changes in multiplication constant and changes in reaction rates with temperature are observed. The lattice cell is simulated by homogenization of materials and cross sections; heterogeneity effects are accounted for by modification of the background scattering cross sections appearing in the resonance-integral calculations. Separate multigroup calculations are performed for each temperature, rather than employing perturbation theory, in order to also account for changes in multigroup flux spectra with temperature.

4. Geometry, Composition, and Cross-section Data

The physical model selected for this study is an infinite array of semi-infinite, cylindrical fuel rods in a hexagonal lattice cooled by sodium. Figure 1 represents a typical cell. The fuel pellet, 15 % enriched U²³⁸-Pu²³⁹ monocarbide, is sodium bonded to the stainless steel cladding. The geometry of the cell is given in Table 1,

FIG. 1. Typical Cell.
The unshaded areas represent sodium lattice.

TABLE 1. LATTICE CELL GEOMETRY

Fuel Pellet	0.268-in. OD; 0.3403-cm radius
Cladding	{ 0.280-in. ID; 0.3558-cm radius; 0.300-in. OD; 0.3810-cm radius.
Hexagonal Pitch	0.426 in. 1.0820-cm pitch
Cell Area	1.0139 cm ²

TABLE 2. COMPOSITION

Nuclide	Volume Fraction	Number Density, atoms/b-cm	
		In Lattice	Homogenized Over Cell
U ²³⁸	0.35883	0.02499	0.0089675
Pu ²³⁹	0.35883	0.00441	0.0015825
C	0.35883	0.02940	0.01055
Na	0.58364	0.02160	0.01261
SS 304*	0.05753	0.08520	0.00490

* SS 304 composition: 10.4 a/o Ni, 19.7 a/o Cr, 69.9 a/o Fe.

the composition in Table 2, and the cross-section data in Table 3.

U²³⁸

Resolved resonances: tabulation in Appendix B.

Unresolved resonances: 1.0–30 keV; $l = 0$ neutrons only.

$$g = 1.0;$$

$$\bar{D} = 18.5 \text{ eV};$$

$$\bar{S}_0 = 0.95 \times 10^{-4} \text{ eV}^{-1/2};$$

$$\bar{\Gamma}_\gamma = 24.6 \text{ mV (constant);}$$

$$\bar{\Gamma}_n^0 = 1.76 \times 10^{-3} \text{ eV}^{1/2} \text{ (Porter-Thomas distribution).}$$

Pu²³⁹

I. Resonance Treatment

Resolved resonances: tabulation in Appendix B.

Unresolved resonances: 53 eV to 5.5 keV; $l = 0$ neutrons only.

$$\bar{g} = 0.5;$$

$$\bar{D} = 2.9 \text{ eV } (\nu = 8);$$

$$\bar{S}_0 = 1.2 \times 10^{-4};$$

$$\bar{\Gamma}_\gamma = 37.51 \text{ mV (constant);}$$

$$\bar{\Gamma}_f = 100.92 \text{ mV } (\nu = 1);$$

$$\bar{\Gamma}_n^0 = 0.70 \times 10^{-3} \text{ eV}^{1/2} \text{ (Porter-Thomas distribution)}$$

Above unresolved resonances: 5.5–30 keV;

$$\sigma_f = 2.5 \text{ b (constant); } \alpha = 0.45;$$

$$\sigma_a = 3.625 \text{ b (constant).}$$

II. "1/v" treatment

$$\sigma_f = 222 \text{ b at 1 eV; } \alpha = 0.54;$$

$$\sigma_a = 342 \text{ b at 1 eV.}$$

5. Execution of Problem

Three uniform fuel temperatures were selected to determine the isothermal Doppler effect: 293.6, 888, and 1482.4°K.

A. Monte Carlo Procedure

There is provision in the REPETITIOUS 3 code to initiate histories from a source that simulates the asymptotic slowing-down density in a flux spectrum constant in lethargy and in space. For this application the histories are started by neutrons crossing the 30-keV energy boundary. Several thousand histories were followed from birth to death. The geometry routine duplicates Figure 1 exactly.

The following cases were run, each consisting of 1024 histories:

I. Pu²³⁹ treated as a resonance absorber below 5.5 keV: 16 cases at 293.6°K; 20 cases at 888°K; 16 cases at 1482.4°K.

II. Pu²³⁹ treated as a "1/v" absorber over the full energy range: 10 cases at 293.6°K and 10 cases at 888°K.

Spectral and regionwise reaction rates are determined in the REPETITIOUS 3 calculation. By performing the Monte Carlo computer runs for each temperature as sets of distinct cases, statistical information could be generated. The reaction rates for each case are average values for 1024 histories. The standard deviations reported in Section 6 are based on the assumption that this set of averages for each temperature follows a normal distribution.

The total reaction rates for U²³⁸ capture, Pu²³⁹ fission and capture, and nonfuel absorption are also available for subcases and therefore further tests of variances were made as well as normality checks based on the methods of Shapiro and Wilk.^(16, 17)

The second set of cases (Pu²³⁹ as "1/v") were selected to provide a simplified model for comparing Monte Carlo and resonance-integral results. The objective was to eliminate the uncertainties involved in the pseudo-broadening of Pu²³⁹ and to compare the methods when only U²³⁸ resonances are present.

B. Resonance Integral-Multigroup Procedure

The effective cross sections for the resonance nuclides were determined for each temperature by the method described in section 3. The values are tabulated in

TABLE 3. CROSS-SECTION DATA

Nuclide	Mass Number	ξ	σ_s , b*	$\sigma_a(1 \text{ eV})$, b
U ²³⁸	238	0.00814	10.7	—
Pu ²³⁹	239	0.00820	10.7	—
C	12	0.15785	4.7	0.0007
Na	23	0.08450	3.2	0.0804
SS 304	54.73	0.03610	10.53	0.9483

* σ_s is constant with energy. σ_a is "1/v" with energy except as noted in text.

Appendix C for the energy range from 30 eV to 30 keV. Two sets of cross sections are given for U^{238} : one for the presence of Pu^{239} resonances and the second for the "1/v" treatment of Pu^{239} . The main reason for the different values between the two sets is due to the correction factor for the integrated flux. As discussed in reference 15, since the same energy-dependent factor applies to both fertile and fissile isotopes, it can generally be neglected in the case of the mixture of U^{238} and Pu^{239} resonances without affecting the Doppler effect.

Since the primary concern in this study is that part of the energy spectrum below 30 keV, this low-energy tail was divided into a relatively fine structure for slowing-down calculations. Above 40.7 keV the YOM⁽¹⁸⁾ cross sections were used with modifications to update the fuel data and to account for the elastic scattering resonances of the light elements.⁽¹⁹⁻²¹⁾ Below 40.7 keV

several sets of elastic removal and "1/v" absorption and fission cross sections were calculated theoretically for various functional forms of flux shape. The assumed fluxes covered variations from dE/E to $E dE$. The initial multigroup structure was similar to that given in Appendix C except that groups above 0.3 keV with lethargy widths equal to 0.5 or larger were divided in half. The corresponding U^{238} and Pu^{239} cross sections for 888°K were obtained by interpolation assuming a smooth energy dependence. Several iterations were passed through in comparing assumed flux shapes to the resultant multigroup fluxes calculated by using elastic removal and "1/v" cross sections corresponding to the assumed flux shapes, until convergence was obtained. The generated flux spectra are presented in Appendix D.

Once convergence was obtained for 888°K, the multigroup libraries were compacted to the energy structure

TABLE 4. REACTION RATES AND DOPPLER EFFECTS ≤ 30 keV

	Pu as Resonance Absorber			Pu as "1/v"		
T (°K)	293.6	888	1482.4	293.6	888	1482.4
C, U^{238} Capture Rates						
RIM	0.3154	0.3362	0.3452	0.3250	0.3499	0.3604
MC	0.3022	0.3305	0.3460	0.3042	0.3343	
re	$\pm 0.4\%$	$\pm 0.3\%$	$\pm 0.3\%$	$\pm 0.4\%$	$\pm 0.3\%$	
diff	+4.4%	+1.7%	-0.2%	+6.8%	+4.7%	
F, Pu^{239} Fission Rate						
RIM	0.4412	0.4289	0.4238	0.4338	0.4178	0.4110
MC	0.4487	0.4327	0.4234	0.4471	0.4278	
re	$\pm 0.2\%$	$\pm 0.2\%$	$\pm 0.2\%$	$\pm 0.2\%$	$\pm 0.1\%$	
diff	-1.7%	-0.9%	+0.1%	-3.0%	-2.3%	
<hr/>						
T	293.6	1482.4	1482.4	293.6	1482.4	1482.4
T_0	888	888	293.6	888	888	293.6
<hr/>						
$\Delta C = C_T - C_{T_0}$						
RIM	-0.0208	0.0091	0.0299	-0.0249	0.0106	0.0354
MC	-0.0282	0.0155	0.0437	-0.0301		
re	$\pm 6. \%$	$\pm 9. \%$	$\pm 4. \%$	$\pm 5. \%$		
diff	-26. %	-42. %	-32. %	-17. %		
<hr/>						
$\Delta C/C_{T_0}$						
RIM	0.0619	0.0270	0.0947	0.0711	0.0302	0.1090
MC	0.0854	0.0468	0.1447	0.0900		
<hr/>						
$\Delta F = F_T - F_{T_0}$						
RIM	0.0122	-0.0052	-0.0174	0.0160	-0.0068	-0.0228
MC	0.0160	-0.0092	-0.0253	0.0193		
re	$\pm 8. \%$	$\pm 12. \%$	$\pm 5. \%$	$\pm 5. \%$		
diff	-24. %	-44. %	-31. %	-17. %		
<hr/>						
$\Delta F/F_{T_0}$						
RIM	0.0285	0.0120	0.0394	0.0382	0.0162	0.0524
MC	0.0370	0.0214	0.0563	0.0451		

Note: The minus sign in the reported differences indicates that the absolute values of RIM are less than MC.

given in Appendix C. One library, identified as RH7, was formed for the resonance treatment of Pu^{239} , and the other, identified as RH9, was formed for the "1/v" treatment of Pu^{239} . By means of these two libraries and the cross sections tabulated in Appendix C, further multigroup calculations were performed to determine the Doppler effect. As a test of the sensitivity of the results to the low-energy libraries, both RH7 and RH9 libraries were used for the resonance and "1/v" treatments of Pu^{239} ; no differences were distinguishable in the resultant total Doppler effects.

6. Results

In the Monte Carlo results the reaction rates in each material and region are presented as a fraction such that the total of reaction rates sums to one. In other words, the reaction rate in a particular material is also the probability that a neutron be absorbed in that material. This normalization is constant for all temperatures and arises because each neutron entered into the system as a source at 30 keV must be absorbed as it slows down. Fission rates are not handled any differently than capture rates in that no new secondary neutrons are injected into the Monte Carlo system if a fission occurs, but the fact is merely recorded.

By determining reaction rates at each temperature

with multigroup analysis as an eigenvalue problem using temperature-dependent cross sections from the resonance-integral calculation, the same normalization as in Monte Carlo is possible. In fact, the results of the multigroup analysis also indicate that the total absorption rate below 30 keV also remains constant with temperature, although not equal to one. This occurs because of the source renormalization that takes place in the eigenvalue problem and as a consequence of the fact that the absorption rate above 30 keV does not change with temperature. In the presentation of the results from the resonance integral-multigroup calculation, the reaction rates below 30 keV are normalized to the total absorption rate below 30 keV.

In Table 4 the capture rates in U^{238} and the fission rates in Pu^{239} are presented for each temperature for the MC (Monte Carlo) and RIM (resonance integral-multigroup analysis) results. These represent the reaction rates summed over all energy groups below 30 keV. Also the changes and relative changes in reaction rates with temperature are also given. The reference temperature is generally 888°K. The discrepancies between the RIM and MC results are reported relative to the MC results so that they can be compared with the relative error (re) from the statistical fluctuations of the MC calculations. The re are based on 1 σ standard deviation.

TABLE 5. COMPARISON OF α AND η

T (°K)	Pu as Resonance Absorber			Pu as "1/v"		
	293.6	888	1482.4	293.6	888	1482.4
η						
RIM	1.8757	1.8805	1.8834	1.8701	1.8701	1.8701
MC	1.8716	1.8805	1.8839	1.8701	1.8701	
diff	$\pm 0.2\%$	$\pm 0.0\%$	$\pm 0.0\%$	$\pm 0.0\%$	$\pm 0.0\%$	
η_{eff}						
RIM	1.2706	1.2353	1.2205	1.2492	1.2032	1.1837
MC	1.2923	1.2461	1.2195	1.2875	1.2319	
diff	$\pm 1.7\%$	$\pm 0.9\%$	$\pm 0.1\%$	$\pm 3.0\%$	$\pm 2.3\%$	
α						
RIM	0.5355	0.5315	0.5292	0.5400	0.5400	0.5400
MC	0.5387	0.5315	0.5289	0.5400	0.5400	
diff	$\pm 0.6\%$	$\pm 0.0\%$	$\pm 0.1\%$	$\pm 0.0\%$	$\pm 0.0\%$	
α_{eff}						
RIM	1.2667	1.3313	1.3597	1.3055	1.3936	1.4331
MC	1.2286	1.3112	1.3616	1.2368	1.3378	
diff	$\pm 3.2\%$	$\pm 1.5\%$	$\pm 0.2\%$	$\pm 5.5\%$	$\pm 4.2\%$	
T (°K)	293.6	1482.4	1482.4	293.6	1482.4	1482.4
T_0 (°K)	888	888	293.6	888	888	293.6
$\Delta\eta_{eff} = \eta_{eff,T} - \eta_{eff,T_0}$						
RIM	0.0353	-0.0148	-0.0501	0.0460	-0.0195	-0.0655
MC	0.0462	-0.0266	-0.0728	0.0556		

A minus sign attached to the difference (diff) in the Table indicates that the magnitudes of the RIM results are less than those of the MC results.

In Table 5 the comparative values of α (the ratio of Pu^{239} capture to fission), α_{eff} (ratio of total captures in all materials to Pu^{239} fissions), and η (neutrons emitted from fission per Pu^{239} absorption), and η_{eff} (the value of k_{∞} for the neutrons below 30 keV) are presented for each temperature.

In Tables 6–8, the partial spectrum comparisons are made. The energy range below 30 keV is also broken into subranges with break points at 5.5 and 1.0 keV. The partial reaction rates are presented to make a comparison over three energy bands. Between 5.5 and 30 keV U^{238} is treated as unresolved resonances and Pu^{239} is treated as constant or as “1/v.” Between 1.0 and 5.5 keV U^{238} is treated as unresolved resonances

and Pu^{239} as unresolved resonances or “1/v.” Below 1.0 keV U^{238} is treated as resolved resonances and Pu^{239} as unresolved (with the exception below 53 eV where Pu^{239} resonances are resolved) or “1/v.” The contribution of the lowest energy range to the Doppler effect is quite small; therefore the results are renormalized to make the comparison. The results are renormalized as if the total absorption below the reference energy, 30, 5.5, and 1.0 keV, respectively, is 1.0. This is done for the lower-energy ranges as an attempt to isolate each energy range and remove the effects of uncertainties above the reference energy.

In Figures 2 and 3 the spectra for the Pu^{239} fission rates at 888°K are given for the two cases: Pu^{239} as “1/v” and as a resonance absorber. The Doppler-effect spectra are presented in Figures 4 and 5 for the two cases. The reference temperature is 293.6°K for the

TABLE 6. U^{238} PARTIAL SPECTRAL COMPARISON

	5.5–30 keV			1.0–5.5 keV			0.03–1.0 keV		
T (°K)	293.6	888	1482.4	293.6	888	1482.4	293.6	888	1482.4
C, U^{238} Capture Rate									
Pu as Resonance Absorber									
RIM	0.1508	0.1595	0.1630	0.3436	0.3799	0.3961	0.3387	0.3819	0.4047
MC	0.1519	0.1656	0.1714	0.3097	0.3581	0.3846	0.2998	0.3380	0.3681
re	$\pm 0.4\%$	$\pm 0.3\%$	$\pm 0.3\%$	$\pm 0.7\%$	$\pm 0.5\%$	$\pm 0.5\%$	$\pm 0.9\%$	$\pm 1.2\%$	$\pm 1.0\%$
diff	-0.7%	-3.7%	-4.9%	$+11. \%$	$+6.1\%$	$+3.0\%$	$+13. \%$	$+13. \%$	$+10. \%$
Pu as “1/v”									
RIM	0.1736	0.1845	0.1889	0.3099	0.3467	0.3630	0.2957	0.3500	0.3774
MC	0.1660	0.1813		0.2716	0.3116		0.2563	0.3003	
re	$\pm 0.4\%$	$\pm 0.4\%$		$\pm 0.7\%$	$\pm 0.7\%$		$\pm 1.5\%$	$\pm 1.2\%$	
diff	$+4.6\%$	$+1.7\%$		$+14. \%$	$+11. \%$		$+15. \%$	$+17. \%$	
T (°K)	293.6	1482.4	1482.4	293.6	1482.4	1482.4	293.6	1482.4	1482.4
T_0 (°K)	888	888	293.6	888	888	293.6	888	888	293.6
$\Delta C = C_T - C_{T_0}$									
Pu as Resonance Absorber									
RIM	-0.0086	0.0035	0.0122	-0.0363	0.0162	0.0525	-0.0432	0.0229	0.0660
MC	-0.0137	0.0059	0.0196	-0.0484	0.0265	0.0749	-0.0382	0.0302	0.0683
re	$\pm 6. \%$	$\pm 12. \%$	$\pm 4. \%$	$\pm 6. \%$	$\pm 10. \%$	$\pm 4. \%$	$\pm 13. \%$	$\pm 18. \%$	$\pm 6. \%$
diff	$-37. \%$	$-40. \%$	$-38. \%$	$-25. \%$	$-39. \%$	$-30. \%$	$+13. \%$	$-24. \%$	$-3. \%$
$\Delta C/C_{T_0}$									
RIM	0.0542	0.0222	0.0808	0.0956	0.0426	0.1529	0.1131	0.0599	0.1950
MC	0.0827	0.0354	0.1287	0.1352	0.0739	0.2418	0.1129	0.0892	0.2277
$\Delta C = C_T - C_{T_0}$									
Pu as “1/v”									
RIM	-0.0109	0.0044	0.0153	-0.0368	0.0163	0.0531	-0.0543	0.0274	0.0818
MC	-0.0154			-0.0400			-0.0440		
re	$\pm 7. \%$			$\pm 7. \%$			$\pm 12. \%$		
diff	$-29. \%$			$-8. \%$			$+24. \%$		
$\Delta C/C_{T_0}$									
RIM	0.0589	0.0240	0.0880	0.1060	0.0471	0.1713	0.1552	0.0783	0.2765
MC	0.0848			0.1285			0.1465		

TABLE 7. Pu²³⁹ PARTIAL SPECTRAL COMPARISON

	5.5–30 keV			1.0–5.5 keV			0.03–1.0 keV		
<i>T</i> (°K)	293.6	888	1482.4	293.6	888	1482.4	293.6	888	1482.4
F, Pu ²³⁹ Fission Rate									
Pu as Resonance Absorber									
RIM	0.3064	0.3047	0.3040	0.2639	0.2545	0.2501	0.3658	0.3395	0.3271
MC	0.3033	0.3019	0.2998	0.2792	0.2663	0.2577	0.3878	0.3662	0.3479
re	±0.5%	±0.3%	±0.4%	±0.8%	±0.5%	±0.5%	±0.9%	±1.0%	±0.9%
diff	+1.0%	+0.9%	+1.4%	−5.5%	−4.4%	−3.1%	−5.7%	−7.3%	−6.0%
Pu as “1/v”									
RIM	0.2530	0.2509	0.2501	0.3566	0.3420	0.3355	0.4527	0.4174	0.3996
MC	0.2507	0.2480		0.3682	0.3528		0.4778	0.4496	
re	±0.4%	±0.5%		±0.7%	±0.7%		±1.1%	±1.2%	
diff	±0.9%	±1.2%		±3.1%	±3.1%		±5.2%	±7.2%	
<i>T</i> (°K)	293.6	1482.4	1482.4	293.6	1482.4	1482.4	293.6	1482.4	1482.4
<i>T</i> ₀ (°K)	888	888	293.6	888	888	293.6	888	888	293.6
$\Delta F = F_T - F_{T_0}$									
Pu as Resonance Absorber									
RIM	0.0018	−0.0007	−0.0025	0.0093	−0.0044	−0.0138	0.0263	−0.0124	−0.0387
MC	0.0015	−0.0031	−0.0035	0.0130	−0.0086	−0.0216	0.0216	−0.0183	−0.0399
re	±127%	±50.%	±55.%	±20.%	±23.%	±12.%	±23.%	±26.%	±12.%
diff	+20.%	−77.%	−30.%	−28.%	−48.%	−36.%	+22.%	−32.%	−3.%
$\Delta F/F_{T_0}$									
RIM	0.0057	0.0023	0.0080	0.0367	0.0174	0.0522	0.0774	0.0366	0.1058
MC	0.0048	0.0102	0.0117	0.0488	0.0322	0.0772	0.0589	0.0500	0.1028
$\Delta F = F_T - F_{T_0}$									
Pu as “1/v”									
RIM	0.0021	−0.0009	−0.0029	0.0146	−0.0065	−0.0211	0.0353	−0.0178	−0.0531
MC	0.0027			0.0154			0.0281		
re	±58.%			±23.%			±28.%		
diff	−23.%			−5.%			+26.%		
$\Delta F/F_{T_0}$									
RIM	0.0082	0.0034	0.0115	0.0428	0.0190	0.0592	0.0846	0.0426	0.1173
MC	0.0107			0.0437			0.0625		

latter two figures. The error flags on the MC results are shown for one standard deviation.

7. Discussion of Results and Conclusions

The study is not complete but from the results presented, the following observations are made.

Total Spectrum ≤ 30 keV

Agreement between the MC and RIM methods in determining the U²³⁸ capture rates and Pu²³⁹ fission rates are quite good; only a discrepancy of a few per cent is noted. A trend seems to be that agreement improves with increasing temperature. For U²³⁸ capture rates the RIM method predicts the larger values, and for Pu²³⁹ fission rate the MC method gives the larger values. The agreement in both reaction rates is gen-

erally better when Pu²³⁹ is treated as a resonance absorber than in the simplified “1/v” cases.

However, for the Doppler-effect determination, agreement is better in the “1/v” cases. In fact, for the temperature changes involving the 1482.4°K temperature in the Pu²³⁹ resonance treatment, rather large disagreements are noted, as large as 40 % for a temperature change from 293.6 to 1482.4°K. The Doppler coefficient in an infinite reactor is approximately proportional to $\Delta F/F$; therefore differences in $\Delta F/F$ tend to induce the same uncertainties in the Doppler coefficient.

Table 5 indicates good agreement between the two methods in the calculation of α and η for Pu²³⁹, the discrepancy being only a fraction of a per cent. However, generally greater discrepancies occur in the determination of α_{eff} and η_{eff} (partial k_{∞}), except for

TABLE 8. PARTIAL SPECTRAL COMPARISON OF α AND η

T (°K)	5.5–30 keV			1.0–5.5 keV			0.03–1.0 keV		
	293.6	888	1482.4	293.6	888	1482.4	293.6	888	1482.4
η									
Pu as Resonance Absorber									
RIM	1.9862	1.9862	1.9862	1.6788	1.6778	1.6787	1.6164	1.6049	1.6038
MC	1.9862	1.9862	1.9862	1.6867	1.6908	1.6913	1.6167	1.6135	1.6057
Pu as “1/v”									
RIM	1.8701	1.8701	1.8701	1.8701	1.8701	1.8701	1.8701	1.8701	1.8701
MC	1.8701	1.8701		1.8701	1.8701		1.8701	1.8701	
η_{eff}									
Pu as Resonance Absorber									
RIM	1.4730	1.4504	1.4414	0.9461	0.8902	0.8661	1.0528	0.9783	0.9416
MC	1.4642	1.4322	1.4157	1.0137	0.9372	0.8944	1.1167	1.0546	1.0018
Pu as “1/v”									
RIM	1.2845	1.2575	1.2458	1.1876	1.1206	1.0917	1.3035	1.2030	1.1509
MC	1.2982	1.2590		1.2554	1.1803		1.3759	1.2950	
α									
Pu as Resonance Absorber									
RIM	0.4500	0.4500	0.4500	0.7155	0.7165	0.7156	0.7817	0.7945	0.7958
MC	0.4500	0.4500	0.4500	0.7075	0.7034	0.7028	0.7814	0.7849	0.7936
Pu as “1/v”									
RIM	0.5400	0.5400	0.5400	0.5400	0.5400	0.5400	0.5400	0.5400	0.5400
MC	0.5400	0.5400		0.5400	0.5400		0.5400	0.5400	
α_{eff}									
Pu as Resonance Absorber									
RIM	0.9544	0.9855	0.9983	2.0436	2.2348	2.3251	1.7337	1.9452	2.0571
MC	0.9631	1.0108	1.0343	1.8412	2.0731	2.2201	1.5790	1.7308	1.8747
Pu as “1/v”									
RIM	1.2426	1.2913	1.3117	1.4252	1.5698	1.6380	1.2088	1.3957	1.5024
MC	1.2185	1.2875		1.2940	1.4400		1.0931	1.2240	

1482.4°K, where the agreement is within 0.2%. This suggests that the relative capture and fission rates are in very good agreement with each other but that the absolute reaction rates relative to the complete system are generally in error by a few per cent. The latter is in agreement with the observations made in the first paragraph. In fact, the discrepancies noted between RIM and MC in α_{eff} and η_{eff} are due to the discrepancies in Pu²³⁹ fission rates since the total absorption rate below 30 keV has been normalized to 1.0. Likewise the re in the MC statistics is the same for α_{eff} and η_{eff} as those given in Table 4 for the fission rates. Therefore the MC re and discrepancies for the change in α_{eff} and η_{eff} with temperature are the same as reported in Table 4 for ΔF .

Partial Spectrum

Examination of Tables 6–8 for the partial reaction rates over 3 different energy ranges shows that the contribution to the discrepancies discussed in the previ-

ous paragraphs can be partially synthesized. The discrepancies between the two methods appear to be larger for the partial reaction rates below 5.5 keV than above, and also there is a change in sign. For the highest energy range the agreement gets better with a decrease in temperature, which is opposite to the effect noted above for the total reaction rates. The “plus” and “minus” discrepancies above and below 5.5 keV tend to cancel each other at 1482.4°K. As mentioned in Section 6, the results for the two lower energy ranges have been renormalized so that the total absorption below the maximum energy of the specified range is 1.0; therefore the three energy ranges do not contribute equally to the total reaction rate.

For the changes in partial reaction rates with temperature, the MC statistics become substantially worse due to dividing the results into three different energy ranges. The only statistically significant difference for the Doppler effect on the partial fission rate is noted for the energy range from 1.0 to 5.5 keV when Pu²³⁹ is

FIG. 2. Fission Rate Spectrum below 30 keV at 888°K; Pu^{239} Treated as "1/v".

FIG. 3. Fission Rate Spectrum below 30 keV at 888°K; Pu^{239} Treated as Resonance Absorber.

FIG. 4. Doppler Effect below 30 keV for Pu^{239} Treated as "1/v".

FIG. 5. Doppler Effect below 30 keV for Pu^{239} Treated as Resonance Absorber.

treated as a resonance absorber. For the Doppler effect on the partial U^{238} capture rate, the greatest significant difference between the two methods appears generally for the energy range above 5.5 keV, the discrepancies being larger than 30 %, with the MC method predicting a larger Doppler effect. It is noted that in this energy range the differences can be attributed only to the U^{238} resonance treatments or the slowing-down treatments since plutonium cross sections are either constant or " $1/v$."

Figures 2-5 further illustrate the spectral results for reaction rates and Doppler effect. There is no renormalization below 5.5 keV as discussed in the presentation of Tables 6-8. Figures 2 and 3 show good agreement in the fission rate spectra generated by MC and RIM. The RIM results exhibit a faster attenuation of reaction rates as the neutrons slow down.

In conclusion, there is generally good agreement in reaction rates considering the rather different assumptions made in the two methods. However, because of the small changes in reaction rates with temperature, the

reasonably small discrepancies in reaction rates produce quite large differences in Doppler effect. The results indicate that uncertainties in the Doppler effect and therefore in the Doppler coefficient are large enough to justify further sophistication in methods, both for MC and for RIM.

Acknowledgments

It is a pleasure to acknowledge Dr. R. A. Dannels for his development and write-up of REPETITIOUS 3, Drs. H. H. Hummel and G. H. Minton for their valuable discussions, Messrs. E. L. Hill and R. P. Price for their very able assistance in computer programming and computations, and Miss Roseann Litzinger for her fine typing.

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Appendix A

The REPETITIOUS 3 Code

REPETITIOUS 3 is a multiregion, cell-geometry, slowing-down, Monte Carlo code which permits inclusion of one primary isotope having strong isolated resonances characteristic of U^{238} or other fertile material, and of another secondary (lesser amount present) isotope which may have resonance characteristics of fissile materials. Thus, the competition which occurs when two resonance absorbers are present is included in the REPETITIOUS calculation. Because of limitations in the core storage available with the IBM 7094, and in order to obtain reasonable computation times, the methods for accounting for Doppler broadening in the two absorbers are somewhat different; greater accuracy is obtained in the calculation for U^{238} . Effects arising from overlap of resonances in a single absorber or from competition between the resonances of the two materials are limited to the accuracy inherent in a Breit-Wigner single-level representation for the resonances and, for the second material, in the approximations discussed below.

The relative probabilities of interactions of various kinds at a given point in the calculation are determined by the cross sections of the individual materials for various processes. The cross sections are determined as follows:

1) Resonance Absorber No. 1 (e.g., U^{238})

It is assumed that, in addition to a background $1/v$ component to the cross section, only the two adjacent resonances (i.e., the first resonance having the energy of its maximum greater than E and the first resonance having the energy of its maximum less than E) contribute to the cross section at energy E . It has been found that such a treatment is quite adequate for U^{238} , in which the average level spacing is of the order of 18 eV.

For energies less than a designated energy E_R (which separates the resolved from the unresolved resonances), the cross sections are given by

$$\sigma_a(E) = \frac{\text{const}}{\sqrt{E}} + \left[\sqrt{\frac{E}{E_0}} \psi(x, \theta) \sigma_0 \frac{\Gamma_\gamma}{\Gamma} \right]_1$$

$$\begin{aligned} & + \left[\sqrt{\frac{E}{E_0}} \psi(x, \theta) \sigma_0 \frac{\Gamma_\gamma}{\Gamma} \right]_2; \\ \sigma_s = \sigma_{\text{pot}} & + \left[\psi(x, \theta) \sigma_0 \frac{\Gamma_n}{\Gamma} + \sqrt{\sigma_{\text{pot}} \sigma_0} \frac{\Gamma_n}{\Gamma} \chi(x, \theta) \right]_1 \\ & + \left[\psi(x, \theta) \sigma_0 \frac{\Gamma_n}{\Gamma} + \sqrt{\sigma_{\text{pot}} \sigma_0} \frac{\Gamma_n}{\Gamma} \chi(x, \theta) \right]_2, \end{aligned}$$

where:

- $x = 2(E - E_0)/\Gamma$;
- $\sigma_0 = 4\pi\lambda_0^2 g(\Gamma_n/\Gamma)$;
- $\theta = 4E_0 k T / A \Gamma^2$;
- λ_0 = the wavelength of the neutron in the center-of-mass system at energy E_0 ;
- k = Boltzmann's constant;
- T = the absolute temperature;
- A = ratio of nuclear mass to neutron mass;
- E_0 = the energy of the neutron at the maximum of the appropriate resonance;
- E = the energy of the neutron at which the cross section is calculated;
- Γ = the total resonance half-width;
- Γ_γ = the resonance half-width for neutron capture;
- Γ_n = the resonance half-width for neutron scattering;
- g = the statistical spin factor for the nuclide and level involved.

Here Doppler broadening is explicitly contained in the development of the ψ and χ functions through the parameter θ . Tables of ψ and χ are contained in the code.

In the unresolved region ($E > E_R$), resonances are determined statistically. The energies corresponding to the maxima for the resonances are fixed with uniform spacing corresponding to the average level spacing determined from data on resolved resonances. For each resonance, the value of Γ_γ is constant, and the value of Γ_n is determined on the basis of a random selection from a Porter-Thomas distribution. The same formulas used for the determination of the values for the cross sections of resolved resonances are applicable to those of the unresolved resonances.

2) Resonance Absorber No. 2 (e.g., Pu^{239})

For this material, an approximation was made in the treatment of Doppler broadening. It is assumed that the broadened shape is a Gaussian having the correct area and peak value for the cross section. This assumption eliminates the necessity for calculation of the ψ and χ functions at each energy E for which the cross section is required.

The cross sections for the resolved region are given by

$$\begin{aligned} \sigma_c &= \sum_{\text{all resolved}} \left[\sqrt{\frac{E}{E_0}} \sigma_0 \psi(0, \theta) \frac{\Gamma_\gamma}{\Gamma} \frac{1}{1 + x'^2} \right]; \\ \sigma_f &= \sum_{\text{all resolved}} \left[\sqrt{\frac{E}{E_0}} \sigma_0 \psi(0, \theta) \frac{\Gamma_f}{\Gamma} \frac{1}{1 + x'^2} \right]; \\ \sigma_s &= \sigma_{\text{pot}} + \sum_{\text{all resolved}} \left[\sigma_0 \psi(0, \theta) \frac{\Gamma_n}{\Gamma} \frac{1}{1 + x'^2} \right], \end{aligned}$$

where

$$x' = \frac{2(E - E_0)\psi(0, \theta)}{\Gamma}.$$

Above the region of resolved resonances, statistical resonances are assumed. For each such resonance, Γ_γ is taken to be constant, and Γ_f , Γ_n , and D (energy spacing between resonances) are drawn from χ -squared distributions with degrees of freedom selected to obtain a good fit to the corresponding distributions determined from experimental data for the resolved resonances. The g values for the statistical resonances may be determined by a random-number sampling such that the frequency of values matches that of resolved resonances. The cross sections at energy E are then calculated as for the resolved energy range except that the sum is limited to twenty resonances: ten above and ten below the energy E . The actual distribution in energy and values of parameters for the statistical resonances are read from a library tape at the beginning of the calculation, and a different set is read periodically after a specified number of histories. Above the statistical region, the cross sections for the second resonance absorber are given by

$$\begin{aligned} \sigma_f &= \frac{\text{constant}}{\sqrt{E}} \quad \text{or constant}; \\ \sigma_\gamma &= \alpha \sigma_f; \\ \sigma_s &= \sigma_p = \sigma_{\text{potential}}. \end{aligned}$$

Values for σ_p , α , and the constant in σ_f are input quantities.

3) Nonresonance Materials

The cross sections for nonresonance materials are obtained at all energies as follows:

$$\begin{aligned} \sigma_a &= \frac{\text{constant}}{\sqrt{E}}; \\ \sigma_s &= \sigma_p = \sigma_{\text{potential}}. \end{aligned}$$

Values for σ_p and the constant are provided for each material.

Appendix B

RESONANCE PARAMETERS⁽²³⁻²⁵⁾

U-238			Pu-239				
E_0 (eV)	Γ_γ (MV)	Γ_n (MV)	E_0 (eV)	Γ_γ (MV)	Γ_n (MV)	Γ_f (MV)	G
6.68	25.0	1.48	-1.2	39.00	1.265	201.0	0.50
10.2	24.6	.0014	.296	38.60	0.114	55.4	0.50
21.0	25.0	9.00	7.9	38.00	0.880	42.0	0.75
36.8	26.0	33.00	11.0	32.00	1.840	147.0	0.75
66.3	20.0	23.00	11.9	40.93	1.070	22.0	0.75
81.1	24.6	2.10	14.3	37.00	0.830	55.5	0.50
90.0	24.6	0.08	14.7	37.00	6.600	30.3	0.25
102.8	21.0	70.00	15.5	40.00	0.840	760.0	0.75
117.0	21.0	18.00	17.6	39.10	1.600	46.3	0.75
145.9	24.6	0.80	22.2	31.26	6.740	75.0	0.25
165.7	14.0	3.50	23.9	37.00	0.128	38.5	0.50
190.0	22.0	135.00	26.2	37.00	2.660	37.3	0.50
209.1	26.5	55.00	27.3	37.00	0.199	2.8	0.50
238.0	20.5	32.00	32.3	37.00	0.430	190.0	0.50
274.5	22.5	27.00	35.3	37.00	0.470	4.1	0.50
292.0	19.0	19.00	41.4	59.13	6.170	10.7	0.75
312.5	24.6	1.00	44.5	27.60	19.200	4.2	0.25
349.0	22.0	55.00	47.6	37.00	2.700	310.0	0.50
378.0	24.6	1.50	50.0	37.00	6.800	33.2	0.50
399.0	40.0	10.00	52.6	34.10	15.200	7.7	0.75
412.0	18.0	17.00					
436.0	20.0	14.00					
456.0	24.6	0.70					
465.0	18.0	7.00					
480.0	35.0	1.50					
491.0	24.6	1.00					
520.0	28.5	37.00					
537.0	24.3	54.00					
558.0	24.6	1.00					
582.0	23.0	42.00					
597.0	23.0	66.00					
622.0	24.0	39.00					
630.0	24.6	9.00					
663.0	25.5	125.00					
696.0	25.2	50.00					
711.0	33.0	20.00					
724.0	24.6	1.50					
733.0	24.6	4.25					
767.0	24.6	9.00					
783.0	24.6	3.00					
793.0	15.0	11.00					
826.0	32.0	60.00					
856.0	24.6	130.00					
860.0	24.6	60.00					
869.0	24.6	2.20					
911.0	34.7	90.00					
930.0	24.6	37.00					
942.0	25.0	195.00					
962.0	23.5	190.00					
997.0	30.0	400.00					

Appendix C

RI U-238 CROSS SECTIONS

E_L (keV)	Δu	σ_a (b), (Pu ²³⁹ as "1/v")			σ_a (b), (Pu ²³⁹ as Resonance Absorber)		
		293.6°K	888°K	1482.4°K	293.6°K	888°K	1482.4°K
40.7							
30.0	0.306	0.111 ^(a)	0.111 ^(a)	0.111 ^(a)	0.111 ^(a)	0.111 ^(a)	0.111 ^(a)
24.7	0.194	0.1311 ^(b)	0.1340 ^(b)	0.1363 ^(b)	0.1263 ^(a)	0.1289 ^(a)	0.1310 ^(a)
15.0	0.500	0.17302	0.18121	0.18431	0.16548	0.17272	0.17543
9.1	0.500	0.25834	0.27662	0.28389	0.24532	0.26120	0.26743
5.5	0.500	0.3631	0.4022	0.4189	0.3422	0.3757	0.3897
4.0	0.321	0.4788	0.5304	0.5523	0.4513	0.4954	0.5138
2.8	0.347	0.59067	0.68280	0.72520	0.55375	0.63202	0.66698
2.0	0.347	0.72877	0.86676	0.93355	0.68196	0.79913	0.85397
1.4	0.347	0.8253	1.0003	1.0880	0.77191	0.92068	0.99261
1.0	0.347	0.9443	1.1698	1.2875	0.8831	1.0755	1.1723
0.55	0.602	1.0340	1.3216	1.4870	0.9602	1.2036	1.3388
0.41	0.301	1.1869	1.5135	1.6873	1.1393	1.4358	1.5903
0.30	0.301	0.7837	1.0065	1.1325	0.7581	0.9635	1.0776
0.173	0.549	1.0877	1.2935	1.4291	1.0122	1.1934	1.3100
0.100	0.549	0.6767	0.8980	1.0486	0.6548	0.8601	0.9977
0.030	1.204	1.8536	2.1033	2.2820	1.7281	1.9463	2.0998

^(a) Results from ARES calculations⁽²²⁾^(b) ARES results modified by extrapolated values of Hwang's "f" factor.⁽¹⁵⁾Note: Scattering cross section per U²³⁸ atom, σ_p , is 50.1 barns.

RI Pu 239 CROSS SECTIONS

E_L (keV)	u	σ_f (b)			σ_γ (b)			σ_a (b)			$\nu\sigma_f$ (b)		
		293.6°K	888°K	1482.4°K	293.6°K	888°K	1482.4°K	293.6°K	888°K	1482.4°K	293.6°K	888°K	1482.4°K
40.7													
30.0	0.306	2.50	2.50	2.50	1.125	1.125	1.125	3.625	3.625	3.625	7.20	7.20	7.20
24.7	0.194	2.50	2.50	2.50	1.125	1.125	1.125	3.625	3.625	3.625	7.20	7.20	7.20
15.0	0.500	2.50	2.50	2.50	1.125	1.125	1.125	3.625	3.625	3.625	7.20	7.20	7.20
9.1	0.500	2.50	2.50	2.50	1.125	1.125	1.125	3.625	3.625	3.625	7.20	7.20	7.20
5.5	0.500	2.50	2.50	2.50	1.125	1.125	1.125	3.625	3.625	3.625	7.20	7.20	7.20
4.0	0.321	1.872	1.874	1.873	1.259	1.264	1.258	3.131	3.138	3.131	5.393	5.397	5.393
2.8	0.347	2.3348	2.3459	2.3455	1.6465	1.6550	1.6545	3.9813	4.0009	4.0000	6.7241	6.7563	6.7551
2.0	0.347	2.9756	3.0047	3.0082	2.1611	2.1852	2.1878	5.1367	5.1900	5.1960	8.5697	8.6536	8.6636
1.4	0.347	3.4824	3.5333	3.5428	2.5777	2.6218	2.6294	6.0602	6.1551	6.1722	10.0294	10.1760	10.2031
1.0	0.347	4.1948	4.2881	4.3114	3.1737	3.2590	3.2790	7.3685	7.5471	7.5904	12.0809	12.3498	12.4168
0.55	0.602	5.1731	5.3403	5.3901	4.0076	4.1728	4.2193	9.1807	9.5131	9.6094	14.8985	15.3801	15.5235
0.41	0.301	6.2737	6.6423	6.7992	4.9723	5.3567	5.5080	11.2460	11.9990	12.3072	18.0683	19.1298	19.5817
0.30	0.301	7.7265	8.1918	8.3899	6.1238	6.6063	6.7965	13.8503	14.7981	15.1864	22.2523	23.5924	24.1629
0.173	0.549	8.3343	9.0023	9.3203	6.6506	7.4289	7.7740	14.9849	16.4312	17.0943	24.0028	25.9266	26.8425
0.100	0.549	9.9704	10.7527	11.1297	7.9562	8.8733	9.2832	17.9266	19.6260	20.4129	28.7148	30.9678	32.0535
0.030	1.204	12.9568	13.9465	14.5534	9.8878	11.3186	12.1052	22.8446	25.2651	26.6586	37.3156	40.1659	41.9138

Note: Scattering cross section per Pu²³⁹ atom, σ_p , is 283.9 barns, $\nu = 2.88$ below 40.7 keV.

Appendix D

FIG. D.1. Flux Spectrum at 888°K of Pu^{239} Treated as "1/v".

FIG. D.2. Flux Spectrum at 888°K of Pu^{239} Treated as Resonance Absorber.

Discussion

Mr. Yiftah (Israel AEC): I have two questions. You mentioned using Monte Carlo to calculate the Doppler effect and the reaction rate, rather than the reactivity. My first question is, why? Secondly, since Monte Carlo has been used at Westinghouse for calculating the total Doppler effect for typical big fast reactors, as mentioned in your 1000-MW study, the question is how do these calculations compare with other methods, such as perturbation or successive k as far as numerical results, computing time, etc? The main point of my second question is, of course, do you consider the Monte Carlo technique useful for calculating the Doppler effect of fast reactors?

Mr. Olhoeft: I think it is useful. I will answer your last question first. At the present stage of technology it is expensive and therefore one cannot use it for a complete design study. You have to look at typical cases, but I think it is very useful for making comparisons between the Monte Carlo results and some of

the other methods. As computers become faster and faster, computer economics will become much more favorable for computational times; then I think Monte Carlos will actually see some greater application. The reason why the Doppler effect was compared rather than the Doppler coefficient is that what we tried to do here was to make the comparison as basic as possible in the respect that we wanted to remove any uncertainties at the higher energy of the spectrum and also any uncertainties of reactor size which would have an effect on leakage. Certainly the Doppler coefficient is sensitive to the reactor, whereas what we were trying to do here was to compare methods for energies below 30 keV. The eigenvalue calculation compares much more directly with the Monte Carlo. Because of the fact that the total absorptions below 30 keV can be normalized to one, if you do a perturbation analysis, you cannot do this. So this seemed to be a natural way to do it.

Mr. Levine (BNL): You mentioned that the Doppler

broadening of the Pu^{239} resonances is not accomplished in quite the same way as that of the U^{238} . Do you know how much discrepancy might be caused by that?

Mr. Olhoeft: We have investigated this somewhat. If one uses the approximation that is built into the code, one can use this as an approximation for the ψ function. Then if you introduce this into the J function for calculating by the resonance-integral method, you may compare these results with, for example, the J calculated by a more exact method. For the case of plutonium where we are quite dilute, the approximation seems to be quite good as regards the reaction rate. We only looked at a fairly typical set of resonances. We used the average resonance parameters. But at about 2 keV there is about a 2% difference in reaction rates. In the Doppler effect, there is a larger difference, as I recall of the order of 20%. But the approximate method actually underestimates the change in the Doppler effect of Pu^{239} . Of course, in this particular study, the U^{238} is what contributes the largest effect to the total Doppler effect, so that 20% is probably reduced to more like a 4 or 5% effect in this study. This would tend to bring closer agreement in the results.

Mr. Codd (Winfrith): Could you please repeat the

standard deviation of your Monte Carlo results with the Doppler coefficient?

Mr. Olhoeft: I did not report the absolute numbers. What I reported was a relative error which would be the ratio of the standard deviation to the quantity that it was referring to. In terms of the reaction rates, we had a relative error of about 0.4%. This was for roughly 10,000 histories when plutonium was introduced as $1/v$ material, and between 16, and 20,000 histories when plutonium was introduced as a resonance absorber. The actual standard deviations will be reported in the proceedings.

Mr. Codd: Yes. I wasn't clear how the discrepancies you obtained between the two methods were related to the standard deviation of your Monte Carlo. That was the point of my question.

Mr. Olhoeft: I see. Well, it is certainly related, but I believe that in most of the cases the discrepancies were outside of the statistical limits. For instance, in the $1/v$ case we had a difference between the two methods of 17%; the Doppler effect of Pu^{239} and the 3σ standard deviations was actually about 15%, so that it was outside of the limits even if we go so far as to say a 3σ standard deviation.

Transient Temperature Distributions in Fast Reactor Fuels with Widely Varying Thermal Diffusivity

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Introduction

The limitations on the thermal performance of reactor fuels, both statically and dynamically, are governed by the thermal diffusivity of the fuel material. Estimates of the peak central temperatures are necessary to determine limiting power densities for normal operation and of physical behavior in abnormal circumstances. In addition, complete temperature distributions are necessary to determine coupling between heat generation and cooling, both statically and dynamically.

It has been possible to meet this problem in the past because fuel materials have had high thermal conductivity and because fuels have had operating temperatures limited by low melting points. When the conductivity is high, its variation with temperature low, and thermal gradients small, the equation for heat conduction can be greatly simplified. Errors in assuming "constant" values for the thermal diffusivity are insignificant and can be ignored.

Now, however, UO_2 fuels have been proposed for large fast reactors.^(1, 2) In calculating temperature distributions for these fuels, large errors will ensue if simplified methods are employed. UO_2 has very low thermal conductivity which decreases with temperature to a minimum, then increases sharply. It is desirable to operate a reactor fueled with UO_2 with centerline temperatures approaching the melting point in order to realize maximum power densities. The large center-to-edge temperature ratios result in conductivities on both sides of the minimum, with thermal radiation strongly influencing heat flow.

In addition, UO_2 fuels are subjected to transient heating experiments in TREAT which may result in rapid temperature rises to well above the melting point. For these tests it is necessary to predict the temperature so that the burst intensity can be properly planned. This is true especially if the damage threshold is approached cautiously in a series of shots. The unreliability of temperature measurements in the UO_2 make extrapolations to the next test in a series useless.

Because of these design and test requirements, a study has been made in which new calculational

methods for determining transient temperature distributions in UO_2 fuel elements have been devised and determination of transient temperature and heat flow behavior undertaken.

Significance of Variable Conductivity

The effect of neglecting variation of thermal conductivity and its gradient can be shown by analysis of the usual steady-state heat-conduction equation for a flat plate:

$$\frac{d}{dx} \left[K \frac{dT}{dx} \right] + q = 0, \quad (1)$$

with boundary conditions

$$dT/dx|_{x=0} = 0; \quad (2)$$

$$-K dT/dx|_{x=l} = ql, \quad (3)$$

where T is temperature, x is the space coordinate, q is volumetric heat-generation rate, l is the plate half-thickness, and K is thermal conductivity.

Equation (1) may be rewritten as

$$K \frac{d^2 T}{dx^2} + \frac{dK}{dT} \left(\frac{dT}{dx} \right)^2 + q = 0. \quad (4)$$

When dealing with materials in which variation of K with T is insignificant, and the thermal gradient dT/dx is small, the quantity in parentheses in Eq. (4) is neglected.⁽³⁻⁷⁾ However, this simplification would lead to significant error if the material were UO_2 .

From reported measurements,⁽⁸⁻²⁶⁾ the thermal conductivity of UO_2 is small at low temperatures and decreases significantly as temperature increases. Then, as thermal radiation becomes significant in transferring heat, the conductivity begins to increase sharply. The term expressing conductivity as a function of temperature in the range below that where radiation is significant can be taken as

$$K = A/(B + T), \quad (5)$$

where A and B are constants and have been assigned the values $A = 42$ and $B = 400$ on the basis of experimental observation.⁽¹⁶⁾

Through the use of Eq. (5), Eq. (4) has been solved for $T(x)$ exactly and with the term in parentheses neglected. The comparative results are displayed in Fig. 1 for $q = 1000 \text{ W/cc}$ and a coolant temperature of 500°K . The center-plane temperature yielded by the simplified solution is seen to be 50 % higher than that given by the exact solution.

This is a simple steady-state problem for illustrative purposes, since temperatures reached are in the range in which thermal radiation is significant. With the radiation component of K increasing nearly as the cube of the temperature, the thermal gradient of K takes on greater significance in the central region of the plate. However, the example chosen dramatically illustrates the errors which may be introduced by neglecting the temperature and conductivity gradients in calculating temperature distributions in low-conductivity materials like UO_2 . For example, this feature makes the ARGUS code⁽⁴⁾ in its present form unsuitable for use in calculation of temperature distributions in UO_2 elements (especially those to be tested in TREAT).

Thermal Diffusivity of UO_2

Before going into the study of transient effects, some discussion of the particular make-up of thermal diffusivity of UO_2 is in order. The diffusivity is defined as the ratio of thermal conductivity to heat capacity (which in turn is the product of specific heat and density). Diffusivity is the key parameter signifying the space-time-dependent behavior of heat flow and temperature. In many materials it varies little with temperature and is frequently considered constant in making calculations. However, in a low-conductivity material like UO_2 it is dominated by $K(T)$ and varies significantly.

With respect to $K(T)$, the predominant mechanisms of heat transfer in ceramics are generally recognized to be phonon conduction and photon conduction (internal radiation).

At low temperatures, phonon conduction is dominant; its effect, in most cases, diminishes with temperature. On the other hand, photon conduction becomes increasingly significant at higher temperatures. In materials such as UO_2 , photon conduction is so significant at higher temperatures (say above 1800°K) that it causes the overall conductivity of these materials to increase with temperature. This has been observed by several investigators.^(14, 24) The phonon-conduction component of the thermal conductivity is usually expressed as in Eq. (5):^(16, 25)

$$K_{\text{phon}} = A/(B + T). \quad (5a)$$

The photon-conduction component is considered to be⁽²⁶⁾

FIG. 1. Comparison of the Approximate with the Exact Temperature Distribution in a UO_2 Plate.

$$K_{\text{phot}} = (16n^2\sigma/3\beta_r)T^3, \quad (6)$$

where n is the index of refraction, σ the Stefan-Boltzmann constant, and β_r the inverse mean free path of radiation. It has been found⁽²⁶⁾ that β_r is essentially proportional to T in crystalline UO_2 . The temperature dependence of β_r is much more complex in noncrystalline UO_2 and is not fully understood. The total effective thermal conductivity is evidently the sum of (5a) and (6):

$$K(T) = [A/(B + T)] + (16n^2\sigma/3\beta_r)T^3. \quad (7)$$

The values of A and B , and the coefficient of T^3 may be obtained from experimental data. However, to facilitate the calculations which follow it was found more convenient to express $K(T)$ as a third-degree power series:

$$K = K_0 + K_1T + K_2T^2 + K_3T^3. \quad (8)$$

This allows determination of the coefficients K_0 , K_1 , $\dots$ either from Eq. (7) or directly from an experimental plot of K versus T by a curve-fitting procedure⁽²⁸⁾ which leads to errors of less than $1/2\%$. Use of the series expression has distinct computational advantages over the form of Eq. (7) and facilitates extension of the computational methods to other ceramic materials.

After a careful study of the recent publications on the conductivity of UO_2 (both experimental and theoretical),^(3, 8, 10, 14, 18-26) it was found that the variation of conductivity with temperature can be given in a certain range of values shown by curves K_2 , K_3 , and K_4 in Fig. 2. In this figure, curve K_1 is obtained by assuming the unrealistic condition of no radiative transfer in the medium and hence represents the value

FIG. 2. Conductivity of UO_2 vs Temperature.

used earlier in the simple steady-state problem as given by Eq. (5) with values of A and B from Ref. 16, that is,

$$K = 42/(400 + T). \quad (9)$$

The other component of the diffusivity (heat capacity) also varies with temperature.⁽³⁾ Although variation is not pronounced, the variation is taken into account in determining the transient behavior of the temperature. Depending on the porosity, irradiation level, and stoichiometry, the volumetric heat capacity may increase or decrease with temperature. It appears that this variation is linear and within $\pm 8\%$ in the temperature range from 500 to 3000°K. Hence it will be necessary to express the product of specific heat $c(T)$ and density $\rho(T)$ as

$$c(T)\rho(T) = g_0 + g_1T, \quad (10)$$

where

$$g_0 = 3.31 \text{ W-sec/cc};$$

$$g_1 = \pm 1.27 \times 10^{-4} \text{ W-sec/cc/}^\circ\text{K}.$$

These elements of the thermal diffusivity have a profound effect on the transient heat-transfer processes within the UO_2 medium. It is this effect that is examined in the study to follow.

Transient Thermal Characteristics of UO_2

A critical evaluation of the transient performance of a reactor system involves the simultaneous solution of a complex system of differential equations describing heat transfer in the fuel element and the coolant, and the neutron kinetics in the reactor.

This particular study is directed towards analyzing the transient thermal characteristics of UO_2 as the first step in the evaluation without reference to any specific

reactor system. Later it is planned to extend the studies to a system represented by a UO_2 -fueled, high-performance fast reactor for the purpose of understanding the influence of the thermal characteristics on the dynamics of the system. At present, indications are that the future system study will employ an analog-digital hybrid computer. For this portion of the study, the analog computer is best suited because of the parametric nature of the study and the time dependence. A brief account of this initial phase is presented here.

Although the fuel elements in the fast reactor of interest will probably be cylindrical, flat-plate geometry is used here, with no cladding to simplify the space dependence. In addition, the temperature of the coolant and the heat transfer coefficient at the fuel-coolant interface are assumed to be constant. (As the next step in the study, these constraints will be removed and the computational scheme will be extended to include cylindrical geometry.)

Under these constraints, the time-dependent conservation of energy in one-dimensional space is expressed as

$$\frac{\partial}{\partial x} \left[K(T) \frac{\partial T}{\partial x} \right] + q = c(T)\rho(T) \frac{\partial T}{\partial t}. \quad (11)$$

The boundary conditions chosen are

$$\frac{\partial T}{\partial x} \Big|_{x=0} = 0; \quad (12)$$

$$-K(T) \frac{\partial T}{\partial x} \Big|_{x=l} = h(T_l - T_c), \quad (13)$$

where x and t are the independent space and time coordinates, respectively, l is the plate half-thickness, q is the volumetric heat-generation rate, h is the heat transfer coefficient at the fuel-coolant interface, T_l is the fuel boundary temperature, and T_c is the coolant

FIG. 3. Sectionalization of Space for Analog Computation.

temperature. $K(T)$ is given by Eq. (8), and $c(T)\rho(T)$ is given by Eq. (10).

It is assumed that the material is isotropic and that the volumetric heat-generation rate is uniform, so that the temperature distribution is taken to be symmetrical with respect to the plate midplane.

The half-thickness is divided into N sections of width h as shown in Fig. 3. The temperature distribution between centers of any two consecutive sections is assumed to be linear. This enables one to integrate Eq. (11) within each of these sections, thereby leaving only $T_0, T_1, T_2, \dots, T_N, T_{N+1}$ as the unknown functions of time. Both T_0 and T_{N+1} are outside the boundaries. This method of solution of the heat-conduction equation is found to require the minimum number of sections. Several other differential-equation formulations were investigated before arriving at this conclusion.

Now consider any section i and integrate Eq. (11) in this section:

$$K_{i,i+1} \frac{(T_{i+1} - T_i)}{h} - K_{i-1,i} \frac{(T_i - T_{i-1})}{h} + qh = \int_{(i-1)h}^{ih} c\rho \frac{\partial}{\partial t} T dx, \quad (14)$$

where $K_{i,i+1}$ is the conductivity at the boundary of section i and $i + 1$, and is evaluated at

$$T = \frac{1}{2}(T_i + T_{i+1}).$$

To show the influence of variable conductivity alone, $c\rho$ is assumed constant. The added influence of variable

FIG. 4. The Effect of Radiative Transfer upon Temperature Distribution in a Plate of UO_2 .

FIG. 5. The Effect of Radiative Transfer upon Temperature Gradient.

heat capacity is studied separately. Hence two sets of equations must be developed.

If $c\rho$ is not a function of T , the right-hand side of Eq. (14) may be expressed as

$$\left. \begin{aligned} c\rho \frac{\partial}{\partial t} \int_{(i-1)h}^{ih} T dx \\ = c\rho \frac{\partial}{\partial t} \left[\int_0^{h/2} \left(T_i + \frac{T_{i-1} - T_i}{h} x \right) dx \right. \\ \left. + \int_0^{h/2} \left(T_i - \frac{T_i - T_{i+1}}{h} x \right) dx \right] \end{aligned} \right\} \quad (15)$$

Because of the symmetry about the center plane,

$$T_0 = T_1 \quad (\text{first boundary condition}). \quad (16)$$

The outer boundary temperature is given by

$$T_l = \frac{T_N + T_{N+1}}{2}.$$

Application of the second boundary condition yields

$$-K_{N,N+1} \left(\frac{T_{N+1} - T_N}{h} \right) = h(T_l - T_c). \quad (17)$$

Thus, one may write N equations of the form of Eq. (14), for $i = 1, 2, \dots, N$. The boundary conditions give two more equations. These $N + 2$ equations are used to solve for the $N + 2$ unknown temperatures $T_0, T_1, \dots, T_{N+1}$.

However, if $c\rho$ is assumed to vary linearly with respect to T , as given by Eq. (10), the right-hand side of Eq. (14) may be integrated to yield

FIG. 6. Distribution of Temperature Resulting from a Step Input of Heat with $K(T) = K_1(T)$.

FIG. 7. Distribution of Temperature Resulting from a Step Input of Heat with $K(T) = K_4(T)$.

$$\begin{aligned}
 & \int_{(i-1)h}^{ih} c(T)\rho(T) \frac{\partial T}{\partial t} dx \\
 &= \int_{(i-1)h}^{ih} (g_0 + g_1 T) \frac{\partial T}{\partial t} dx \\
 &= hg_0 \frac{\partial}{\partial t} \left[\frac{1}{8} T_{i-1} + \frac{3}{4} T_i + \frac{1}{8} T_{i+1} \right] \\
 &+ \frac{1}{6} hg_1 \frac{\partial}{\partial t} \left[\frac{1}{4} T_{i-1}^2 + \frac{11}{2} T_i^2 + \frac{1}{4} T_{i+1}^2 \right].
 \end{aligned} \quad (18)$$

It is noted that the above method of solution of Eq. (11) has the distinct advantage that there are no memory requirements in the analog computer to deal with the nonlinearity of the equation and the boundary conditions. Further, the method is equally applicable to cylindrical geometry and is found to require the minimum number of spatial subdivisions to achieve a certain desired accuracy in the solution.

Since an exact analytic solution of Eq. (11) is avail-

able for the steady-state case,⁽²⁹⁾ it was compared with the analog solution for various numbers N of subdivisions. It was found that $N = 5$ is adequate for this study, since the error for this case is less than $\frac{1}{2}\%$. For this example, $K(T)$ was taken from curve K_4 of Fig. 2; other quantities used were $q = 1000$ W/cc, $l = 0.25$ cm, $T_c = 500^\circ\text{K}$, $h = 6.67$ W/cm²/°K, and $c\rho$ was held constant but given by Eq. (10) with $g_1 = 0$. It is noted that the computed curve touches the theoretical curve in each subsection and that the small error is always ≥ 0 .

The following significant results of the present study include the effect of internal radiation upon the steady-state and transient distribution of the temperature, its gradient within the medium, and the effect of $c\rho$ being a function of temperature.

The steady-state distribution of temperature for each of the curves of conductivity versus temperature in Fig. 2 is shown in Fig. 4. This figure clearly shows that photon conduction has a significant influence upon the steady-state temperature distribution.

Radiative transfer also has a sizable effect upon the temperature gradient in the UO_2 plate (which incidentally determines the thermal stress in the material). This is illustrated in Fig. 5. The effect of increasing the radiative transfer upon the transient response is illustrated in Figs. 6 and 7 in which has been considered the time-dependent temperature distribution resulting from a step increase in the internal heat-generation

TABLE 1. TIME CONSTANTS (SEC)

Conductivity	$T_c = 500^\circ\text{K}$	$T_c = 1000^\circ\text{K}$
$K_1(T)$	3.40	4.80
$K_2(T)$	3.25	4.00
$K_3(T)$	2.95	3.45
$K_4(T)$	2.60	2.85

FIG. 8. Frequency Response of the Center-plane Temperatures.

rate of 1000 W/cc for $T_c = 1000^\circ\text{K}$. Each figure is normalized to the corresponding steady-state center-plane temperature rise. $K_1(T)$ and $K_4(T)$ from Fig. 2 were used. The relative speed with which the temperature distributions attain their steady states may be observed from these figures.

The familiar "time constant" used in connection with linear systems may also be chosen as a measure to indicate the speed of response. This "time constant" is defined as the time taken by the response to reach $[1 - (1/e)]$ of its final value for a step increase of the forcing function. The "time constants" associated with the center-plane temperature responses corresponding to the various conductivity curves of Fig. 2 for coolant temperatures of 500 and 1000°K , respectively, are obtained for a step increase in the volumetric heat-generation rate of 1000 W/cc (see Table 1).

The effect of internal radiation on the transient behavior may also be illustrated by considering the frequency response. This consists of observing the amplitude and phase delay of oscillations of the center-plane temperature for a variation of the volumetric heat-generation rate q of the form

$$q = q_0 + A \sin \omega t, \quad (19)$$

where q_0 is a certain steady value.

The amplitudes and phase responses of the center plane temperatures are given in Fig. 8 for a coolant temperature of 1000°K . It is apparent that the internal radiation effects are more pronounced at higher temperatures and for lower frequencies.

It is also clear that the attenuation of the amplitude of the response is quite rapid with increasing frequency. This means that high-frequency fluctuations in the heat-generation rate encountered in reactor operations have little disturbing effect upon the UO_2 fuel temperature for frequencies of practical interest,* even though its thermal conductivity (and consequently its ability to diffuse heat) is considerably smaller than the metallic fuels.

A linear variation of c_p with respect to T is considered next. c_p is chosen as in Eq. (10), and the integration is carried out with the use of Eq. (18). q is taken as 1000 W/cc (applied as a step function), $T_c = 1000^\circ\text{K}$, and the conductivity is given by curve K_4 of Fig. 2. Figure 9 gives the response of the center-plane temperature for the $+$ and $-$ values of g_1 . Note that the "time constant" is 3.1 sec for the positive value of g , and 2.3 sec for the negative value of g_1 .

In conclusion it is stressed that internal radiation has a significant effect upon both the steady-state and

FIG. 9. The Effect of c_p upon the Response.

transient thermal response of UO_2 , which in turn affects the reactor design involving use of this material. The recent experimental determinations of thermal conductivity indicate that there is a substantial contribution from internal radiation. However, at present there is disagreement in the quantitative functional relationship of conductivity with temperature. The range of experimental functional relationships shown in Fig. 2 for UO_2 covers a wide variation of its properties, such as oxygen content, porosity, and structure. An experimental determination of the variation of conductivity with temperature corresponding to the exact form of UO_2 used in any design is, therefore, essential to a realistic evaluation of the physical behavior of that specific reactor design.

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Discussion

Mr. Fell (Winfrith): In your first graph, which presented a fairly marked difference in center temperature between the results with a constant K and the results

with varying K , what value of K did you use for the constant value? Was it in fact the low-temperature value or was it that associated with the mean temperature of the fuel?

Mr. Sanathanan: I think you have misunderstood what I said. I said that there was a great difference between the exact and the approximate solutions of the heat conduction equation. This was because the approximate solution was obtained by neglecting the term containing dK/dT in the equation. This approximation is good in materials where K does not vary appreciably with temperature. But for $K(T) = 42/(400 + T)$, such an approximation is erroneous.

Mr. Lellouche (BNL): How many zones did you use in your analysis?

Mr. Sanathanan: We used 5 zones, and the errors were of the order of 1% or less.

Mr. Lellouche: When you say you used five, did you try ten or fifteen to start with?

Mr. Sanathanan: We first tried to solve the equation by writing it as a difference equation and considering 10 zones. We found that the errors were of the order of 20%. We used up the entire analog facility at this point. It was this disappointing result that lead us to the new method of solution. First, we tried to solve the equation with 3 zones; and the errors were only of the order of 4%. When we increased the number of zones to five, our errors were within 1%. We did not go for more zones. The errors that I have been referring to are for the steady state distribution of temperature.

Mr. Lellouche: Few-zone techniques, while they very well mockup steady-state calculations of this sort, very often are far wrong in predicting the transient phenomena. This is one point. Secondly, why not go to a digital instead of an analog computer? You could then use one or two hundred points and get a relatively exact solution. The multipliers in your analog, being mechanical or electrical, are often wrong when small multiplications are involved. Then perhaps the problem must be rescaled in the middle of it.

Mr. Sanathanan: This is a historical question—the conflict between the use of analog and digital. As far as I am concerned, it doesn't make any difference; if any computer can solve the problem to the accuracies that you are expecting to solve, I think it is well and good. The analog computer had a particular advantage for our kind of parametric study. In this problem we had no difficulty with scaling.

Let me answer your previous question. The approximation in our few-zone technique is that the temperature varies linearly between mid-points of the zones. The temperature at the mid-points are kept completely unrestricted. Therefore, during transients or steady state, if the temperature distribution is not very ir-

regular in the material, the present few-zone technique is quite adequate.

Mr. Cobb (Nuclear Power Group, England): In your curves of the various conductivities that you showed, it seemed to me you had a rather general fit to various conductivity values. My understanding was that the irradiation term didn't come in until much higher temperatures, something over 2000°K. It seems to me that the differences between the K_1 and K_4 values was due almost really to differences in mean conductivity levels, rather than just to irradiation effect at high temperatures. Secondly, I notice you have only solved in slab geometry; what about the case of cylindrical geometry which is what the pin designs represent?

Mr. Sanathanan: The method we used is very general. There is no difference in the analog simulation. Both cylindrical and slab amount to the same pro-

graming, identical except that the pot settings are different.

To answer your first question, it is certainly obvious that when you increase the radiation contribution, the mean value of the conductivity also is going to increase with temperature. We are interested in temperature ranges from 2000 to 3000°K. We are not planning to devote our attention to temperature ranges with which we are very familiar in the case of metallic fuels, that is, around 1500°K.

Mr. Cobb: My point was that you have in fact fitted the curves to a cubic equation. I don't see at the low temperatures where you can say whether it is radiation or just differences in the basic data.

Mr. Sanathanan: At low temperatures, say up to 1500°K, you don't have to worry about radiation at all; the contribution will be less than 5 % or so.

Transient Temperature Distribution in Fuel Elements

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Introduction

From a reactor systems analysis point of view, calculation of fuel rod and cermet (ceramic-metallic expansion controlling rod)* temperatures must provide accurate feedback to the remainder of the plant model. It must also be concise enough to minimize the amount of analog equipment or digital computing time required.

The representation for the problem is usually a one-dimensional sectionalized nodal model, as shown in Figure 1, with the following defining equation:

$$(\rho C_p)_i T_i = R_{i+1,i}(T_{i+1} - T_i) - R_{i,i-1}(T_i - T_{i-1}) + Q_i, \quad (1)$$

where the heat-transfer resistances are defined from appropriate steady-state profiles or by the heat-transfer properties of the fuel element. The disadvantage of employing such a nodal approach is a possible inaccuracy when only a few sections are used. Furthermore, computed temperatures are limited to the centers of selected sections, and accurate information about the temperature gradient is not given directly, although the latter is important in determining stresses on the rod or cermet. Another disadvantage arises from the need to determine the value at each node selected in order to obtain averages. These values may fluctuate widely during transients because of the spatial distribution within the nodal regions.

Alternatives to the nodal approach involve finding transform solutions, in which spatial modes, $\psi_n(z)$, describe the spatial dependency of the solution, as given by

$$T(z, t) = \sum a_n(t) \psi_n(z). \quad (2)$$

* In the Westinghouse fast breeder reactor design, the fuel rods are assembled into two bundles, in line with each other. Each such bundle is one-half of the core height. The bundles are connected by central cermet rods, fabricated by powder metallurgy from a mixture of fuel, ceramic, and metal, that serve as continuous structural members through the core. Thermal expansion of the cermet rods thus results in a net reactivity decrease for the core.

An advantage of the transform-solution method is that the temperature and its derivative may be given directly at any point on the profile. In fast reactors especially, due to the high power densities, fuel and clad motion (by thermal expansion) exert a determining effect on stability and control, and thus have a strong bearing on power distribution. The system analyst is interested in the temperature distributions arising in the fuel, clad, and controlling device (such as the cermet rod), as well as their average temperatures. These average temperatures provide elements for evaluating the fuel Doppler feedback, clad-expansion feedback, controlling CEX feedback, etc. In calculating average temperatures by modal analysis, the higher the rank of the modes, the smaller their influence due to their wavelike spatial behavior. As a result, average temperature calculations require only a few terms to attain sufficient accuracy for transient temperature representation.

For the above reasons, temperature computation by finite transformation was applied to analyses of the Westinghouse Fast Breeder Reactor design utilizing the CEX (Controlled Cermet Expansion) concept. Initial work in this area employed separate transformations for the fuel and clad.^(1, 2)

Use of finite transform techniques (with kernels possessing analytical finite discontinuities) in treating temperature distributions in multiregion problems has been discussed by Kaplan⁽³⁾ and was applied to an analysis of a nuclear fuel rod.⁽⁴⁾

This paper describes a transform solution for temperature distributions within a clad fuel rod. Discontinuous kernels and mixed geometry are used with finality and completeness† preserved in the transformation. By considering the problem in mixed geometries, there is considerable simplification of the transcendental equation that yields the eigenvalues of the transformation. Also included are quantitative checks with nodal method solutions and an analysis of several TREAT experiments, in which the fast periods considered are typical of those which may be expected in a fast reactor.

† Finality and completeness—see reference 5, paragraph 1.

FIG. 1. Section through Fuel rod Nodal Model.

Summary of Finite Transform Solution for Cylindrical Fuel, Rectangular Clad Representation

Since the cladding is thin relative to the radius of the fuel rod, steady-state considerations show that slab representation of the clad gives negligible error.⁽⁶⁾ The problem considered is shown in Figure 2.

With the substitutions*

$$\begin{aligned} u_f &= T_f^* - T_{\text{avg cool}}^* ; \\ u_c &= T_c^* - T_{\text{avg cool}}^* , \end{aligned} \quad (3)$$

where * denotes the deviation from steady state, the appropriate conduction equations for the fuel and clad become

$$\begin{aligned} \frac{1}{z_f} \frac{\partial}{\partial z_f} \left(z_f k_f \frac{\partial u_f}{\partial z_f} \right) + Q(z_f, t) - \rho_f C_{pf} \frac{\partial T_{\text{avg cool}}^*}{\partial t} \\ = \rho_f C_{pf} \frac{\partial u_f}{\partial t} \end{aligned} \quad (4)$$

for $0 \leq z_f \leq a$, and

$$\frac{\partial}{\partial z_c} \left(k_c \frac{\partial u_c}{\partial z_c} \right) - \rho_c C_{pc} \frac{\partial T_{\text{avg cool}}^*}{\partial t} = \rho_c C_{pc} \frac{\partial u_c}{\partial t} \quad (5)$$

for $0 \leq z_c \leq l$, where $Q(z_c, t) = 0$. Boundary conditions for the problem are:

$$\begin{aligned} \nabla u_f &= 0 & @ z_f &= 0; \\ \left. \begin{aligned} k_f \nabla u_f &= k'_c \nabla u_c \\ -k_f \nabla u_f &= h_{fc}(u_f - u_c) \end{aligned} \right\} & @ z_f &= a; \quad z_c = 0; \\ -k_c \nabla u_c &= h'_{cw} u_c & @ z_c &= l, \end{aligned} \quad (6)$$

where $k'_c = k_c[(a + \frac{1}{2}l)/a]$ and $h'_{cw} = h_{cw}[(a + l)/a]$ are the appropriate modifications to account for slab representation of the clad. The transform solution is as

* Convergence of series solutions using the dependent variables defined in (3) has been checked⁽⁶⁾ and found to be more rapid than series solutions for the variable T_f^* when there are variations in $T_{\text{avg cool}}^*$.

given in (2):

$$u(z, t) = \sum_n a_n(t) \psi_n(z), \quad (7)$$

where $a_n(t)$ is given by the time behavior of the transformed variable divided by the normalizing function and where the transform kernel $\psi_n(z)$ is the composite function

$$\psi_n(z) = \delta_{fi} \psi_{nf}(z_f) + \delta_{ci} \psi_{nc}(z_c); \quad (i = f, c). \quad (8)$$

$\psi_{nf}(z_f)$ and $\psi_{nc}(z_c)$ are given by

$$\frac{1}{z_f} \frac{d}{dz_f} \left(z_f k_f \frac{d}{dz_f} \psi_{nf}(z_f) \right) + \lambda_n^2 (\rho_f C_{pf}) \psi_{nf}(z_f) = 0 \quad (9)$$

and

$$\frac{d}{dz_c} \left(k_c \frac{d}{dz_c} \psi_{nc}(z_c) \right) + \lambda_n^2 (\rho_c C_{pc}) \psi_{nc}(z_c) = 0, \quad (10)$$

which are the eigenfunction equations from (4) and (5), respectively. $\psi_{nf}(z_f)$ and $\psi_{nc}(z_c)$ also satisfy the boundary conditions given in (6).

The unique transformed equation is found by multiplying equation (4) by ψ_{nf} and integrating over the fuel region, and similarly multiplying equation (5) by ψ_{nc} and integrating over the clad domain and summing the results. The domain elements are $2\pi z_f dz_f = dR_f$ for the fuel and $2\pi(a + \frac{1}{2}l) dz_c = dR_c$ for the clad, since rectangular coordinates are used for the clad. Performance of these operations yields

FIG. 2. Fuel-rod Nodal Model.

$$\begin{aligned}
& \int_0^a \frac{1}{z_f} \frac{\partial}{\partial z_f} z_f k_f \frac{\partial u_f}{\partial z_f} \psi_{nf} dR_f + \int_0^l \frac{\partial}{\partial z_c} k_c \frac{\partial u_c}{\partial z_c} \psi_{nc} dR_c \\
& - \int_0^a \left[Q(z_f, t) - \rho_f C_{pf} \frac{dT_{\text{avg cool}}^*}{dt} \right] \psi_{nf} dR_f \\
& \quad - \int_0^l \rho_c C_{pc} \frac{dT_{\text{avg cool}}^*}{dt} \psi_{nc} dR_c \\
& = \int_0^a \rho_f C_{pf} \psi_{nf} \frac{\partial u_f}{\partial t} dR_f + \int_0^l \rho_c C_{pc} \psi_{nc} \frac{\partial u_c}{\partial t} dR_c.
\end{aligned} \quad (11)$$

Define the transformed variable as

$$\int_0^a \rho_f C_{pf} \psi_{nf} u_f dR_f + \int_0^l \rho_c C_{pc} \psi_{nc} u_c dR_c = \bar{u}_n. \quad (12)$$

With this and with

$$Q(z_f, t) = Q(z_f)Q(t), \quad (13)$$

$$\int_0^a Q(z_f) \psi_{nf} dR_f = Q_n, \quad (14)$$

and

$$\int_0^a \rho_f C_{pf} \psi_{nf} dR_f + \int_0^l \rho_c C_{pc} \psi_{nc} dR_c = G_n, \quad (15)$$

(11) becomes

$$\begin{aligned}
& \int_0^a \frac{1}{z_f} \frac{d}{dz_f} z_f k_f \frac{du_f}{dz_f} \psi_{nf}(z_f) dR_f \\
& + \int_0^l \frac{d}{dz_c} k_c \frac{du_c}{dz_c} \psi_{nc}(z_c) dR_c \\
& + Q_n Q(t) - G_n \frac{dT_{\text{avg cool}}^*}{dt} = \frac{d\bar{u}_n}{dt}.
\end{aligned} \quad (16)$$

A double integration by parts of the above integral terms interchanges ψ_{nf} with u_f and ψ_{nc} with u_c . Then, using (9) and (10), and the transform definition, the time behavior of the transformed variable is given by

$$-\lambda_n^2 \bar{u}_n + Q_n Q(t) - G_n \frac{dT_{\text{avg cool}}^*}{dt} = \frac{d\bar{u}_n}{dt}. \quad (17)$$

Since $\bar{u}_n(t)$ is defined only by the n^{th} mode, the transform possesses the property of finality.

Using the orthogonality property for $\psi_n(z_i)$ (see reference 7), the series solution for $u(z_i, t)$ becomes

$$u(z_i, t) = \sum_n \frac{\bar{u}_n [\delta_{fi} \psi_{nf}(z_f) + \delta_{ci} \psi_{nc}(z_c)]}{N_n}, \quad (18)$$

where

$$\begin{aligned}
N_n &= \int_0^a \rho_f C_{pf} \psi_{nf}^2(z_f) dR_f \\
&+ \int_0^l \rho_c C_{pc} \psi_{nc}^2(z_c) dR_c.
\end{aligned} \quad (19)$$

From (18), the equations expressing the average temperatures in the fuel and the clad are given by

$$\begin{aligned}
u_{i, \text{avg}}(z_i, t) &= \sum_n \frac{\bar{u}_n}{N_n R_i} \int_{R_i} \psi_{ni} dR_i; \\
R_i &= \int_{R_i} dR_i.
\end{aligned} \quad (20)$$

Solution Eigenvalues

The solution of (9) and (10), subject to the boundary conditions of (16), yields the functions $\psi_{nf}(z_f)$, $\psi_{nc}(z_c)$, and the values of λ_n^2 , thus satisfying the problem. With the aid of the first three conditions listed in (6), for constant thermal conductivity, we obtain

$$\psi_{nf}(z_f) = A_{nf} J_0(p_{nf} z_f); \quad (21)$$

$$\begin{aligned}
\psi_{nc}(z_c) &= -\frac{p_{nf} k_f}{p_{nc} k_c} J_1(p_{nf} a) \sin(p_{nc} z_c) + \\
&\left\{ J_0(p_{nf} a) - \frac{p_{nf} k_f}{h_{fc}} J_1(p_{nf} a) \right\} \cos(p_{nc} z_c),
\end{aligned} \quad (22)$$

where

$$p_{ni} = \sqrt{\frac{\rho_i C_{pi}}{k_i}} \lambda_n; \quad i = c, f.$$

By means of (22) and the final condition of (6), the transcendental equation for the solution eigenvalues with $\mathbf{u}$ as the root is

$$\frac{b(\tan(\mu \mathbf{u}) + \nu \mathbf{u})}{\nu \tan(\mu \mathbf{u}) - 1} + \frac{J_0(\mathbf{u})}{J_1(\mathbf{u})} - \xi \mathbf{u} = f(\mathbf{u}) = 0, \quad (23)$$

where

$$\begin{aligned}
b &= \sqrt{\frac{\rho_f C_{pf} k_f}{\rho_c C_{pc} k_c}}; & \gamma &= \frac{p_{nf}}{p_{nc}} = b \frac{k_c}{k_f}; \\
\mu &= \frac{l}{a\gamma}; & \nu &= \frac{k_c}{a\gamma h_{cw}}; & \xi &= \frac{b k_c}{h_{fc} a \gamma}
\end{aligned} \quad (24)$$

and $\mathbf{u} = a\gamma p_{nc} = ap_{nf}$, where (23) depends on the five parameters listed above, which are defined by the heat transfer properties of the fuel rod. A typical plot of the function, whose roots may be readily evaluated by a digital computer, is given in Fig. 3.

With the fuel-rod parameters specified, and the eigenvalues and eigenfunctions determined from (21), (22) and (23), plus knowing the form of $Q(t)$ and $dT_{\text{avg cool}}^*/dt$, transient temperature profiles may be developed by (18).

Sample Calculations

The transform solution outlined above was applied to several transient test cases (power steps, ramp, and impulses) for parameters representing both thermal and

FIG. 3. Solution for Transcendental Function.

fast reactor fuel rods. As a quantitative check of the method, Fig. 4 shows the 3-mode transient profile for a 10% step in reactor power (using thermal rod parameters) as compared to the "more precise" solution using seven modes and checked using a 5-node calculation. The consistency between these solutions for this "mild" transient serves as a check on the modal method of solution. Rapid convergence of the computed average temperatures, as given by the transformation method for this transient, is illustrated in Figures 5 and 6. Convergence of the average fuel temperature is very rapid, since the modes beyond the first oscillate spatially throughout the fuel, and their integral effect becomes progressively smaller as the order increases. Convergence of the average clad temperature is slower, however, since for this problem, the modes of lower order are of the same sign. The physical reason for this behavior is that the response of the clad is faster than

that of the fuel, and hence requires more modes for an accurate description.

A direct comparison between the modal and nodal methods of comparable complexity is given in Fig. 7 for 3-mode and 3-node solutions. A 7-mode solution is considered as the "more precise" answer. Close observation of the figure shows the modal solution is more accurate. This comparison indicates that the modal method offers an improvement over a nodal solution, especially when only a few terms are involved, and thus merits further consideration.

As a qualitative check, transient profiles were determined by finite transformation for TREAT experiments CEN 65 and CEN 67⁽⁸⁾ (see Figures 8, 9, and 10). For these transients, a nonuniform heat-generation profile having a 17% drop from the outer edge to the center of the pellet was used. The input to the pellet was approximated from the initial period of the reac-

FIG. 4. Transient Profile for 10% Step in Power at $T = 0$.

FIG. 5. Modal Solution Average Values for Fuel Temperature.

tor, maximum reactor power, and the total energy imparted to the sample, and was considered to follow the form of the transient behavior calculated for the TREAT reactor.⁽⁹⁾ Energies imparted to the samples were 244 and 240 cal/gm, respectively.

Calculations for CEN 65 showed that the sample would reach fuel melting first at (r/R) equal to approximately 0.67. For test CEN 67, heat transfer from the sample was insufficient to produce fuel melting. Considering the greater probability of clad failure when molten fuel is formed in the vicinity of the clad, the calculations show that clad under test CEN 65 would

rupture, while that in test CEN 67 should remain intact. These conclusions are consistent with the experimental results.

Conclusions

Finite transformation represents a classical method of solution for temperature distributions in solids. Complexity of the analysis of the nuclear fuel rod by finite transformation may be reduced considerably, especially in the transcendental equation defining the eigenvalues of the problem, by considering a problem in discontinuous (two) geometries—cylindrical fuel

FIG. 6. Modal Solution Average Values for Clad Temperature.

FIG. 7. Modal and Nodal Solutions for a 1-sec Full-power Pulse.

FIG. 8. Energy Release in TREAT Experiment CEN 65 as a Function of Radius.

with slab-geometry clad. From the sample calculations performed, the method of modal analysis appears comparable to nodal models of the same complexity for "mild" or slow transients, and for fast transients is considered to be an improvement, especially when only a few modes or a few sections are involved. Furthermore, the modal method is analytical and now ready for further detailed improvements that will add to its versatility and accuracy. Nevertheless, further merits or disadvantages of finite transformation for represent-

FIG. 10. Energy Release in TREAT Experiment CEN 67 as a Function of Radius.

ing fuel-rod temperatures can be perceived only by further application and checks of the method.

It is stressed that the above approach represents only a step forward to achieving more accurate modal solutions. The inclusion of variable properties with temperature is the next goal in the advancement of the method. The nonlinear problem that arises from considering properties as a function of average temperature may be treated directly by the above method, using a stepwise procedure in time.

FIG. 9. Energy Release in TREAT Experiment CEN 65 as a Function of Time.

Glossary of Terms

Sub f	denotes fuel
Sub c	denotes clad
Sub avg cool	denotes average coolant

<i>Symbol</i>	<i>Location</i>	<i>Description</i>	<i>Dimensions</i>
a	Fig. 2	Fuel-pellet radius	L
$a_n(t)$	(2)	Coefficient of series solution	—
b	(24)	Coefficient ratio of fuel or clad	—
$f(\mathbf{u})$	(23)	Eigenvalue function	—
h_{fc}	Fig. 2	Fuel-to-clad film coefficient	$\text{HL}^{-2}\text{T}^{-1}\Theta^{-1}$
h_{cw}	Fig. 2	Clad-to-coolant film coefficient	$\text{HL}^{-2}\text{T}^{-1}\Theta^{-1}$
h'_{cw}	(6)	Modified h_{cw}	$\text{HL}^{-2}\text{T}^{-1}\Theta^{-1}$
k_i	Fig. 2	Thermal conductivity	$\text{HL}^{-1}\text{T}^{-1}\Theta^{-1}$
k'_c	(6)	Modified clad conductivity	$\text{HL}^{-1}\text{T}^{-1}\Theta^{-1}$
l	Fig. 2	Clad thickness	L
p_{ni}	(22)	Eigenfunction of region i	L^{-1}
$u_i, u(z, t)$	(3)	Relative temperature of region i	Θ
$\bar{u}_n$	(12)	Transformed relative temperature	HL^{-1}
$\mathbf{u}$	(23)	Root of the eigenvalue transcendental equation	—
z_i	Fig. 2	Independent variable of region i	L
A_{nf}	(21)	Kernel coefficient	—
C_{pi}	(1)	Specific heat of region i	$\text{H}\Theta^{-1}\text{M}^{-1}$
G_n	(15)	Notation	$\text{H}\Theta^{-1}\text{L}^{-1}$
$J_n(\mathbf{u})$	(21)	Bessel function of first kind of order n and argument $\mathbf{u}$	—
N_n	(19)	Transform normalizing function	$\text{H}\Theta^{-1}\text{L}^{-1}$
$\mathcal{Q}_n$	(14)	Notation	L^2
$Q_i, Q(z_i, t)$	(4)	Volumetric heat generation in region i	$\text{HL}^{-3}\text{T}^{-1}$
$Q(t)$	(3)	Volumetric power time function	$\text{HL}^{-3}\text{T}^{-1}$
$Q(z_f)$	(3)	Spatial power distribution	—
R_i, r_{i+1}	(1)	Nodal-model heat-transfer resistance	$\text{HL}^{-2}\text{T}^{-1}\Theta^{-1}$
$\mathcal{R}_i$	(11)	Region i domain	L^2
T_i	(1)	Temperature of region i	Θ
γ	(24)	Eigenvalue ratio	—
δ_{fi}	(8)	Kronecker delta symbol	—
λ_n	(22)	Eigenvalue of order n	T^{-1}
μ	(24)	Fuel, clad coefficient ratio	—
ν	(24)	Clad dimensionless coefficient	—
ξ	(24)	Clad dimensionless coefficient	—
ρ	(1)	Specific mass	ML^{-3}
$\psi_n, \psi_n(z)$	(2)	Eigenfunction of order n	—
$\psi_{ni}(z_i)$	(8)	Eigenfunction in region i	—
$\partial/\partial z_i$	(4)	Partial derivative with respect to coordinate z_i	—
∇	(6)	Denotes gradient	—
$(-)$	(17)	Denotes transformed variable	—
$(*)$	(3)	Denotes deviation from steady state	—

Dimension Symbols

H	Energy
L	Length
M	Mass
T	Time
Θ	Temperature

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Discussion

Mr. Cobb (Nuclear Power Group, England): In some of the temperature profiles you showed there was actually a dip at the center of the pin. What was the reason for that; was it a flux depression?

Mr. Restelli: No. I forgot to mention that in the TREAT experiments there was nonuniform heat generation. In ANL-6250⁽⁸⁾ there is a calculation that indicates a 17% rise in power across the fuel pin, and this was included in the calculation.

Mr. Cobb: Otherwise the conductivity and specific heat were assumed constant across the pin?

Mr. Restelli: Yes.

Mr. Lellouche (BNL): You stated that the mathematical technique you used permitted you to get a single equation for each mode.

Mr. Restelli: Yes, time-dependent.

Mr. Lellouche: But when you use a different type of mode in the clad, the amplitude of the mode would no longer be a single function of a single interior mode, but a sum of the amplitudes of the interior. They wouldn't separate, would they?

Mr. Restelli: They will separate, yes. It is possible to obtain a match, via the boundary conditions, of first mode in the fuel with first mode in the clad.

Mr. Lellouche: Then did you use a time-dependent or a time-independent boundary condition?

Mr. Restelli: It doesn't matter. We matched thermal fluxes and film coefficients.

Mr. Godts (WAPD): We used, in fact, a series of dis-

continuous functions. When we speak of a mode, that mode describes the properties within the fuel element as well as in the clad. That means that the transform equation will be representative of the behavior of the fuel and the clad at the same time. If we have a time variation of the properties, like the film coefficient between fuel and clad, we must have a new series of eigenfunctions. If we use a digital computer, these eigenfunctions can be easily tabulated, offering a very easy possibility to solve those problems.

Mr. Lellouche: In a full-feedback model in which you try to mock up the dynamics, I no longer think that this would be a valuable technique as opposed to a straight numerical multipoint analysis because of the nonlinearities that arise.

Mr. Godts: This might be true although there is something working for us. We found out for the same complexity, that means, the same number of modes and nodes, analog computing requires slightly less equipment; I suppose that with digital techniques we would have slightly shorter time except maybe for certain specific nonlinearities.

Mr. Fell (Winfrith): It seemed to me from your graphs that in all cases you are using essentially a linear temperature fall through the clad, which I think is obviously the right thing to do. Although you might use many modes in the fuel, there is no sense in ever using more than one mode in the clad. Now, if you have many modes in the fuel and are sticking to one mode in the clad, it should effectively come out the same if you can put your boundary conditions as a single boundary condition on the surface of the fuel. So the mathematics turns out really to be no different from doing effective mode shapes over the fuel alone.

Mr. Restelli: I don't think I agree with that. I did plot all the drops across the clad linearly and didn't bother to calculate the intermediate points from the calculation. However, the effect of the higher modes in the clad had quite a bit of influence on the solution. In fact, since the clad is thin, it has a fast response, and it really requires more modes than the fuel for an accurate representation.

Mr. Fell: I am essentially saying that you can always treat the clad as if it were simply a conductor in parallel with the heat-sink term. In fact, this will fail to be true only if you have to take into account modes which put a node in the middle of the clad.

Mr. Restelli: In other words, just add a node to the outer edge of the solution, is what you are saying. Well, yes, that would be one way of treating the problem. I think I like the modal approach through the clad just as well.

Session VI-A
October 13, 1965

Fuels and Fuel Behavior

Chairman: S. LAWROSKI
Argonne National Laboratory

Secretary: H. Savage
Argonne National Laboratory

Fast Reactor Safety Considerations Related to Fuel Macrostructure

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Introduction

Inherent safety in fast reactor core design has continued to receive substantial attention as evidenced by the abundance of work reported on the Doppler effect.^(1, 2) Controlled fuel expansion also has been suggested as an alternative or augmenting mechanism for limiting fast power reactor excursions.⁽³⁾ In each case fuel design plays a dominant role in achieving maximum safety characteristics for a given reactor design.

Fuels of the ceramic type, such as oxides, carbides, nitrides, and sulphides, possess some measure of inherent safety by virtue of the negative Doppler effect associated with the fertile fuel isotopes. Metallic or cermet fuels offer similar protection because of a relatively large negative reactivity expansion characteristic. It is often assumed in the analysis of transient reactor behavior that both of these mechanisms provide instantaneous negative reactivity feedback in proportion to the energy generated. The validity of this assumption depends upon the rapidity with which the temperature of the bulk fuel rises relative to the energy generated. If the fuel has a completely homogeneous structure or if the excursion is in the subprompt critical range, then instantaneous feedback is an adequate assumption. However, if some small amount of time is required for heat transfer to take place from fissile components of the fuel, such that the bulk fuel temperature lags the energy generation, then the assumption of instantaneous feedback for very fast excursions can be substantially in error. Further, in the case of an expanding fuel, inertia limits the response following a temperature rise in addition to thermal time delays encountered. It is reasonable to conclude that these considerations assume importance only in the range of superprompt accidents, and it is in this regime that the present work applies.

The transient response of the fuel to a very fast burst, whether it be entirely thermal or thermoelastic, depends upon such macroscopic properties as density, homogeneity, thermal conductivity, specific heat, and elastic modulus. Investigations have been undertaken to

assess the importance of homogeneity to prompt Doppler feedback in mixed oxide fuels.^(4, 5) However, no evaluations of this kind have been published for other ceramic fuels or for cermets. Further, it is of interest to investigate the importance of other macroscopic fuel properties to inherent safety as well. This paper summarizes recent work relating these properties to fast reactor safety when the reactor undergoes a prompt burst accident.

Transient Heat Transfer in Inhomogeneous Fuels

Transient heat transfer within the bulk fuel is a consideration when the fissile component is not homogeneously mixed with the bulk fuel. Inhomogeneities in fuel can be due to the fuel design, method of manufacture, or may develop under certain conditions of irradiation.

Some examples of inhomogeneous structure are shown in Fig. 1. An indication of the wide range in dispersion of fissile material in different fuel types is apparent.

Three mechanisms by which heat is transported within the fuel are conduction, radiation, and recoil of fission fragments. In selecting a physical model for analysis of these processes, the fuel is assumed to be composed of spherical particles of fissile material uniformly dispersed in the bulk fuel, as shown in Fig. 2. Although the choice of this model is somewhat arbitrary, the results are relatively insensitive to exact geometry.

The diffusion equation governing the heat-conduction process in this model is

$$\nabla^2 u(r, t) = \frac{1}{K} \frac{\partial u}{\partial t}(r, t), \quad (1)$$

where u is the temperature at point r and time t , and K the thermal diffusivity.

Solutions have been obtained⁽⁴⁾ for the temperature rise ΔT_1 of the fissile particle and that of the surrounding fuel, ΔT_2 , of the forms

$$\Delta T_1 = A\psi + B_1 \sum_{n=1}^{\infty} g_n \quad (2)$$

FIG. 1 Photomicrographs Showing Inhomogeneities in Some Mixed Fuels Containing UO_2

and

$$\Delta T_2 = 4\psi - B_2 \sum_{n=1}^{\infty} q_n, \quad (3)$$

where the first term in each case is the heat generation and the second term represents the sum of all the modes of heat conduction into the fuel. The q_n are governed by time constants related to the fuel composition as follows

$$\tau_n = \frac{d_1^2}{4KE^{1/3}\lambda_n^2}, \quad (4)$$

where d is the fissile particle diameter, E the enrichment volume fraction, λ , the eigenvalue, and

$$K = \text{thermal diffusivity} = k/\rho c_p$$

Here k is the thermal conductivity, ρ the fuel density, and c_p the fuel specific heat.

Thus, the fuel parameters affecting the time delay are thermal diffusivity (involving thermal conductivity, density, and specific heat), particle size, and enrichment. Values for thermal properties of some

selected fuel types are shown in Table 1 near normal operating fuel temperatures.

It is interesting to note that the carbides and nitrides have the most favorable transient heat transfer characteristics. The diffusivity of carbides exceeds even that of stainless steel because of the higher specific heat of the latter. Oxides and sulphides, on the other hand, are the least favorable because of their low conductivity. Time constants for fundamental mode heat transfer are shown in Fig. 3 as a function of particle diameter and enrichment. Since a large fraction of the heat transfers in this mode, neglect of the terms of higher order does not introduce large error.

In the case of heat deposited in the bulk fuel by fission fragment recoil from fissile particles, the process is effectively instantaneous. However, the amount of heat transferred by this mechanism depends upon fissile particle size and separation, i.e., homogeneity. The mean range of these fragments is from about 9 to 12 μ in ceramic type fuels. Analyses have been carried out which show that for very small particles, of approximately 1 μ diameter, close to 90% of the energy

escapes in this manner; in the limit of very large particles ($>100 \mu$), the fraction approaches 33 %.⁽⁵⁾ In any case, if fissile particles are separated by no more than about twice the mean range, substantial instantaneous heating of the bulk fuel is obtained. As a point of reference the particle separation distance for several particle sizes and fuel enrichments is shown in Table 2, for which a uniform distribution has been assumed.

One can also add to the heat from fission-fragment recoil, the threshold fission heating in U-238 or Th-232 if the bulk fuel is a fissile-fertile mixture. This fraction is near 12 % in a 1500-liter carbide core with U-238, but is only about 4 % with thorium fuel, because of its lower fission cross section and higher fast fission threshold. Radiation undoubtedly supplements the other heat-transport mechanisms, although its net effect is uncertain because of photon scattering at grain boundaries and because of increasing opacity to infrared wavelengths with increasing fuel temperature. Some conservatism is retained by neglect of this component in the present work.

Thermal Expansion Kinetics

For a fuel in which inherent safety is derived from a negative reactivity coefficient of thermal expansion, inertial effects become important in a fast burst in

FIG. 2. Physical Model.

$$\nabla^2 u = \frac{1}{K} \frac{\partial u}{\partial t}$$

$$\tau_1 = -\frac{1}{\lambda_j^2 K}$$

TABLE 1. THERMAL PROPERTIES OF FUEL MATERIALS

Fuel Material	Theoretical Density (g/cm ³)	Thermal Conductivity (cal/cm-sec-°C)	Specific Heat (cal/g-°C)	Thermal Diffusivity (cm ² /sec)	Melting Point (°C)
UO ₂	10.97	0.008	0.078	0.009	2750
PuO ₂	11.47	(a)	(a)	0.009	2280
UC	13.63	0.06	0.045	0.098	2350
PuC	13.6	(a)	(a)	0.098	1850
UN	14.32	0.05	0.077	0.045	2885
PuN	14.23	(a)	0.061	0.058	2750
US	10.87	0.004	0.055	0.007	2462
PuS	10.60	(a)	(a)	0.007	2400
ThO ₂	10.00	0.0078	0.073	0.01	3250
Stainless Steel	7.9	0.05	0.12	0.05	1400

(a) Value not available; assumed the same as uranium compound.

addition to the thermal delay considered above. The problem in expansion reactivity feedback has been investigated in connection with the design of fast pulsed reactors.⁽⁶⁾ The time required for expansion feedback to become effective is governed by the time required for the elastic wave to propagate throughout the fuel. In the case of a fuel rod in which the wavelength of the induced stress wave is long relative to the transverse dimension, the wave equation yielding the displacement is

$$\frac{\partial^2 y(x,t)}{\partial x^2} \frac{1}{v^2} - \frac{\partial^2 y(x,t)}{\partial t^2} = \beta \frac{\partial T(x,t)}{\partial x}, \quad (5)$$

where

y = the longitudinal displacement at point x and time t ;

β = coefficient of linear expansion;

$T(x,t)$ = temperature distribution function;

w = wave velocity = $\sqrt{M/\rho}$;

M = Young's modulus;

ρ = mass density.

The solution to this equation for stress-free ends is readily obtained for a generalized temperature function.⁽⁷⁾ Soodak has shown that for a temperature $T = T_0 e^{at} \cos Bx$, the linear expansion coefficient β is reduced by an inertial factor such that⁽⁸⁾

$$\beta_{\text{eff}} = \frac{\beta}{1 + (\alpha^2/\omega^2)}, \quad (6)$$

where

α = inverse reactor period;

ω = natural rod frequency = $\pi v/2L$;

L = rod length;

v = wave velocity.

FIG. 3. Time Constants for Fundamental-mode Heat Transfer $\tau_p = d_i^2/4KE^{3/2}\lambda_p^2$.

TABLE 2. PARTICLE SEPARATION DISTANCE

Particle Diameter (μ)	Enrichment Volume Fraction		
	0.10	0.20	0.30
10	11.5	7.1	4.9
100	115	71	49
1000	1150	710	490

Thus, the importance of inertia to the speed of response depends upon the ratio of the inverse period to the natural frequency of the fuel rod. Properties affecting fuel expansion are shown in Table 3 for several cermet fuels. Expansion coefficients reported here are mean values between 20 and 1000°C.

It is evident that the wave velocity in all the above cermets is near 4000 m/sec if the elastic modulus is not strongly affected by composition. There appear to be little data available on this latter property, however. It should be noted, that if the effective density of a rod is increased by attaching to it additional nonexpanding mass, the response is slowed substantially from that indicated by the calculated wave velocities in Table 3.

A further point of interest is that during measurements on the expansion coefficient of SS-PuO₂ cermets at Battelle-Northwest, a certain amount of stress relieving was observed during the first temperature cycle of the specimen. Although this does not affect the expansion coefficient or reactor safety, it does indicate the desirability to anneal the fuel before use to avoid a reactivity penalty from initial lengthening of the fuel.

Energy Release in Fast Bursts

To permit a comparison of the importance of thermal and inertial delays and to relate these to fuel design

criteria, an estimate of the energy release under assumed accidental conditions is desirable. Since only a relative comparison is necessary, an exponential-burst approximation is adopted, that is, the reactor power is assumed to rise exponentially on a constant period until the peak of the excursion is reached. At this point the reactivity feedback mechanisms become instantaneously effective and reduce the total reactivity to zero. The energy release to the peak of the burst is given by

$$A_p = A \int_0^{t_p} e^{\alpha t} dt = \frac{A}{\alpha} (e^{\alpha t_p} - 1) \approx \frac{A}{\alpha} e^{\alpha t_p}, \quad (7)$$

where

A = initial power;

α = prompt reactor period;

t_p = elapsed time to peak of burst.

In general, the reactivity balance at the peak of the burst is

$$\Delta k + \gamma_1 \Delta T_1 = (\gamma_2 + \lambda) \Delta T_2, \quad (8)$$

TABLE 3. EXPANSION PROPERTIES OF CERMETS

Cermet Type	Density (g/cm ³)	Elastic Modulus (dynes/cm ²) ^(a)	Mean Expansion Coefficient (cm/cm-°C × 10 ⁶)	Wave Velocity (cm/sec × 10 ⁻⁵)
SS-24.5 v/o UO ₂	8.7	1.38 × 10 ¹²	19.5	3.98
SS-50 v/o UO ₂	9.4	1.38 × 10 ¹²	18.8	3.84
SS-63.5 v/o UO ₂	9.7	1.38 × 10 ¹²	18.1	3.77
SS-20 v/o PuO ₂	8.6	1.38 × 10 ¹²	17.5	4.0

^(a) Exact values not available; assumed same as stainless steel.

where

- Δk = excess prompt reactivity;
- γ_1 = Doppler coefficient of fissile fuel;
- γ_2 = Doppler coefficient of fertile fuel;
- λ = reactivity coefficient of expansion;
- ΔT_1 = temperature rise of fissile fuel particle;
- ΔT_2 = temperature rise of surrounding fuel.

By means of equations (2), (3), and (6), the energy release to the peak of the burst in terms of the thermal and inertial delay factors and reactivity coefficients may be obtained, recalling that in (2) and (3) the first term is identical with equation (7).

It is convenient to evaluate the ratio of the energy release at the peak of the burst to that obtained if the feedback were instantaneous. This quantity, which will be referred to as the energy release ratio, is as follows:

$$\frac{Q'_p}{Q_p} = \frac{\gamma_2 + \lambda - \gamma_1}{\gamma_2 \left[1 - \frac{B_2 \alpha}{A} \sum_{n=1}^{\infty} g_n \right] + \frac{\lambda}{1 + \alpha^2 / \omega^2} \left[1 - \frac{B_2 \alpha}{A} \sum_{n=1}^{\infty} g_n \right] - \gamma_1 \left[1 + \frac{B_1 \alpha}{A} \sum_{n=1}^{\infty} g_n \right]}; \quad (9)$$

where all terms have been defined previously except for B_1 , B_2 , and the g_n , which are given by

$$B_1 = \frac{6}{\rho C_p} \frac{(q_{01} - q_{02})}{E}; \quad (10)$$

$$B_2 = \frac{6}{\rho C_p} \frac{(q_{01} - q_{02})}{1 - E} \quad (11)$$

with

E = enrichment volume fraction;

q_{01} = the initial power density in fuel particle;

q_{02} = the initial power density in surrounding fuel;

and for the exponential-burst approximation,

$$g_n \approx \frac{a_n \tau_n}{1 + \alpha \tau_n}, \quad (12)$$

where

$$a_n = \frac{\left[\sin \left(\frac{\lambda_n r_1}{r_2} - \frac{\lambda_n r_1}{r_2} \right) \cos \left(\frac{\lambda_n r_1}{r_2} \right) \right]^2}{(\lambda_n^2 - \sin^2 \lambda_n) \lambda_n^2}.$$

The energy release ratio as given by equation (9) has been evaluated for two different fuel types. The first, typical of a mixture of plutonium and uranium compounds, incorporates positive and negative Doppler but no expansion. The second, characterizing a cermet, takes account of positive Doppler and negative expansion. Both are for an assumed enrichment of about 20 v/o. In Figure 4, results of the first calculation are shown. The energy release ratio is given as a function of the ratio of the fundamental-mode time constant to the prompt period. Parameters of each curve are the ratio of positive to negative Doppler coefficients, γ_1/γ_2 , and the fraction of the energy appearing directly in the bulk fuel, ϵ .

The significance of a positive Doppler coefficient in fuel of inhomogeneous structure is rather striking, as shown by the large increase in relative energy release with increasing time delay and/or decreasing period. Direct heating of the fuel, either by fission fragment recoils or threshold fission, aids substantially in holding the energy release down. Thus, from a fuel design standpoint it is important to keep the thermal time constant below the shortest credible period anticipated in a power excursion. Referring to the time constants shown in Fig. 3, carbides and nitrides offer an advantage over oxides and sulphides in this regard.

In the second type of fuel investigated, that in which expansion is the principal shutdown mechanism, the energy release ratio as a function of inverse period was evaluated for a range of thermal time constants from

0 to 1 ms and a ratio of positive Doppler to negative expansion coefficients of 0 and 10%. The results obtained are shown in Fig. 5 for a fuel rod 200 cm in length. The curves for $\gamma_1 = 0$ represent only the inertial delay, which is seen to become important when the reactor period falls below about 1 ms in this case. The effect of thermal delay is, of course, most pronounced when positive Doppler feed-back exists. However, inclusion of a small amount of fertile material may be used to introduce sufficient negative Doppler to neutralize this. Nevertheless, the inertial factor

FIG. 4. Energy Release Ratio as a Function of Time Constant/Prompt Period Ratio.

FIG. 5. Energy Release Ratio vs Inverse Period in Cermet Fuel. Length = 200 cm.

remains limiting in fuel of this type. Direct heating by fission-fragment recoils is probably not nearly as effective in cermet as in ceramic fuels because of the shorter range ($\sim 2.5 \mu$ in stainless steel). Some advantage is also gained by incorporating a fuel having a high diffusivity, such as carbide or nitride, in the steel matrix. Reducing the fuel length has the largest effect in reducing the inertial factor. In the present case, reducing the length by a factor of two decreases by about the same factor the period at which a sharp rise in the energy release ratio takes place.

There are several defects in the present calculations which perhaps should be mentioned before attempting to interpret these results. First, the exponential-burst model is known to be in error near the peak of an excursion. However, since we are calculating only ratios of energy release rather than absolute magnitudes, less sensitivity to burst shape is expected. Second, reactivity coefficients that are linear with temperature were used for convenience. This approximation will more strongly affect those cases in which large temperature changes take place in one fuel component. Finally, no changes in state are assumed for the fuel, again affecting energy release for the more severe accidents. It is believed that in spite of the preceding

limitations, the periods and/or time constants for which the energy release rises above that of instantaneous feedback response are reliably predicted.

Conclusions and Recommendations

It is evident from the preceding analyses that thermal and elastic properties related to fuel macrostructure can be of utmost importance in fast reactor power excursions in the prompt-critical range. A certain amount of inhomogeneity in fast reactor fuels may be tolerated, depending upon the shortest credible reactor period one postulates, the amount of positive reactivity feedback, and the amount of direct heating by fission fragment recoils or threshold fission. With positive Doppler feedback the energy release is very sensitive to the ratio of the time constant to the period. Thus in a large fast oxide breeder, for example, $20\text{-}\mu$ PuO_2 particles would appear adequate to assure inherent safety for reactor periods down to about 0.1 ms. Carbides and nitrides would permit an increase in particle size by about a factor of three for the same range of periods.

For ceramic fuels coprecipitation of the components will yield excellent homogeneity in fuel manufacture. Similar results have been obtained by physically

mixing $\text{UO}_2\text{-PuO}_2$ powders and dry ball-milling.⁽⁹⁾ While the possibility exists that gross inhomogeneities in the fuel mixture may occur, the probability of such an occurrence is considered to be very small. When proper techniques and mixing times are employed, physically mixing UO_2 and PuO_2 powder has consistently yielded a uniform mixture. Once a uniform mixture of $\text{UO}_2\text{-PuO}_2$ is obtained, the mixture can be consolidated (by pneumatic impaction or sintering) to maintain a uniform distribution of the two materials during subsequent processing. To guard against the possibility of PuO_2 segregation, the fabricated fuel pins can be subjected to a gamma-scanning test. This test for nonuniformity of PuO_2 distribution is simple, rapid, and inexpensive, and can be applied to each fabricated fuel pin.

Cermet fuels will provide adequate inherent safety by fuel expansion providing the inverse reactor period remains smaller than the natural vibration frequency of the fuel rod. The energy release climbs very rapidly beyond this point, establishing a threshold beyond which the response is too slow to be effective. The effect of a thermal delay in heating of the bulk fuel is to move this threshold towards longer periods. In the example presented in this study the threshold occurs for periods between 0.1 and 1 ms, depending on the choice of the amount of positive Doppler feedback and time delay. It is concluded that cermet fuels must be critically examined if employed in a reactor in which a prompt accident is credible.

Finally, the question should be answered whether fuel designed and manufactured to provide nuclear safety continues to retain those properties throughout long residence in a reactor. Observed structural changes in physically mixed $\text{UO}_2\text{-PuO}_2$ indicate that in the vicinity of 1700°C and higher, extensive homogenization occurs. Similar homogenization probably occurs in carbides and nitrides considered here as well. Of particular concern would be any strong tendency for migration and reconcentration of the fissile component with exposure. Investigations of segregation mechanisms have been carried out for oxides and to a lesser extent for carbides.^(10, 11) Results so far appear encouraging. Whether or not the other properties change appreciably with long exposure can not be determined without additional measurements on irradiated fuel.

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Discussion

Mr. Lawroski (ANL): In regard to your remarks about the situation with respect to cermets, up to what loading of the ceramic in the cermet does this apply?

Mr. Peterson: In the particular case this was a 20 v/o cermet.

Mr. Kummerer (Karlsruhe): Concerning the reactivity feedback coming out from the thermal expansion, the inertia plays a role and the inertia is connected with the density of material. Is this influence coming from the inertia linear or nonlinear with the density?

Mr. Peterson: The wave velocity is equal to the square root of the modulus of elasticity divided by the density. Thus it is a square-root dependence.

Mr. Okrent (ANL): Up to what temperature would the stainless steel cermet be temperature-independent, and where would its expansion properties markedly be affected by unusual effects?

Mr. Peterson: The effect depends upon the modulus of elasticity. I would expect less response at higher temperatures due to a decrease in the modulus.

Mr. Siegel (AI): I notice you used the modulus for stainless steel in the absence of a value for the cermet. Doesn't one have to be rather careful in these rather heterogeneous materials if the two wave velocities are significantly different, as they possibly might be?

Mr. Peterson: Yes.

Mr. Siegel: I think stainless steel probably has the higher wave velocity.

Mr. Peterson: I would have to defer that to someone who has investigated the physical properties of UO_2 to some greater extent than I have.

Molten Plutonium Alloys as Fast Reactor Fuels

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ABSTRACT

Molten plutonium alloys are being investigated by LASL as fuels for fast reactors. LAMPRE-I, a 1-MW reactor experiment, utilized a Pu-Fe alloy of 16 g Pu/cc and demonstrated the feasibility of using molten alloys as reactor fuel. Current effort is being directed toward developing the less corrosive Pu-Co-Ce alloy system which will be employed in the LASL Molten Plutonium Burnup Experiment (MPBE). Results of current research and development regarding the Pu-Co-Ce fuel system are summarized.

Introduction

Molten metal alloys for utilization as fuel in fast reactors have several attractive properties, such as: capability of release of the bulk (approaching 100%) of volatile fission products; a large thermal expansion coefficient ($\sim 90 \times 10^{-6} (\Delta v/v)/^{\circ}\text{C}$); the potential for simplified fuel handling and low fabrication costs; and the elimination of fuel slump as an accident consideration.

LAMPRE-I, a 1-MWt reactor experiment, employed Pu-Fe fuel of 16 g Pu/cc and demonstrated, in a small system, the feasibility of using a molten alloy as a fast reactor fuel. However, Pu-Fe fuel is not a suitable fuel for large power reactors (~ 1000 MWe) because of its high plutonium density (and consequently low allowable specific power).

An alternative fuel alloy, Pu-Co-Ce, will be utilized in the Molten Plutonium Burnup Experiment (MPBE),^(1, 2) which is presently being designed. This fuel alloy offers two major advantages over Pu-Fe: (1) the plutonium concentration is variable, and over the entire range of alloys from 0 to 13 g Pu/cc the melting point is less than 450°C (see Fig. 1); (2) the fuel is far less corrosive to the selected containment materials than is Pu-Fe at equivalent test conditions.

Coupled with the advantages of molten alloys listed above, three areas are of concern. The first, and most obvious, is the potential for corrosion of the container and loss of the fuel. Present experience indicates that tantalum or Ta-W alloys are the most likely container materials. The development effort which has been expended on this facet has resulted in improvements to

the extent that reliable long-term fuel containment may be anticipated.

The second problem area is that Pu-Co-Ce fuels exhibit large volumetric expansions during freezing of from 1–3% for plutonium concentrations from 0–8 g Pu/cc (see Fig. 2). This situation poses a potential operational nuisance in that the core may have to be maintained in a molten condition or under controlled conditions if mechanical damage to the container is to be avoided.

The third area is associated with the formation of a Ta-Co intermetallic reaction layer which may be damaged during irradiation and cause undesirable mass-transport effects. Preliminary evidence obtained by radiotracer experiments in the absence of irradiation indicate that no major difficulties will be presented by this phenomenon.

The following sections will summarize the major results of current research on the various aspects of the problem of development of molten alloys and define areas which require in-pile evaluation.

Corrosion Behavior

One of the most important aspects of developing a molten-alloy fuel system is demonstrating the capability of containing the molten metal for extended periods of time. It was established early in the LAMPRE program that the corrosion-failure mechanism involved with liquid plutonium alloys contained in refractory metals was that of intergranular penetration (IGP) of the plutonium through the grain boundaries of the container.⁽³⁾ It was also realized, although somewhat later, that corrosion behavior as measured by length of time before a fuel element showed detectable plutonium on the surface was statistical in nature, with a ratio of shortest observed lifetime to longest of <0.1 . Figure 3 shows the fraction failed versus test time for Pu-Fe and Pu-Co-Ce under all test conditions up to 850°C , plotted according to the Weibull distribution.

Because the time interval required to obtain meaningful corrosion data at temperatures of interest in reactor applications (500 – 750°C) is in the thousands of hours, test equipment designed to obtain lifetime

data at temperatures up to 1150°C has been recently employed. Figure 4 shows mean lifetime data obtained at elevated temperatures in vacuum ($\sim 1 \times 10^{-7}$ Torr) for both plutonium and Pu-Fe eutectic alloy. It is significant that for the Pu-Fe fuel, the high-temperature and the lifetime data obtained for a sodium environment may be represented by the same straight line, which lends support to the validity of extrapolating high-temperature data to obtain corrosion-behavior information at lower temperatures.

Tests to determine the lifetime of Pu-Co-Ce fuels of interest to the MPBE are in progress. Corrosion tests have encompassed fuel concentrations from 5–8 g Pu/cc in contact with tantalum and Ta-W alloys (up to 10 w/o W) and at temperatures up to 1100°C. Since there have been few failures associated with

tests carried out at 850°C and below, no definite conclusion can be drawn concerning expected lifetime of this fuel system. However, several generalizations can be drawn from data available:

a. When all information from Pu-Co-Ce alloy corrosion tests (that is, all combinations of fuel composition, container material, and test temperature) have been pooled, it is observed that of 135 tests 27 have resulted in failures, of which 26 have been in welds.

b. The test results indicate that container walls are not penetrated by Pu-Co-Ce (in dramatic contrast to Pu-Fe fuel) but that only welds are subject to penetration, up to the maximum observation time to date, 7500 hr at 850°C (see Fig. 3).

c. When welds are removed from consideration, there is an indication that significant corrosion of

FIG. 1. Liquidus Projection of the System Plutonium-Cobalt-Cerium.

FIG. 2. Volumetric Expansion during Freezing of Pu-Co-Ce Alloys as a Function of Plutonium Concentration.

tantalum or Ta-W alloys will not occur at temperatures up to 850°C.

d. The lifetime prior to failure of Pu-Co-Ce alloys contained in tantalum or Ta-W alloys will be significantly better than for Pu-Fe alloy (see Fig. 3).

It has been found that a carburized layer on the internal surface of the container or the diffusion of carbon into the tantalum grain boundaries is exceptionally effective in preventing plutonium penetration with the highly corrosive Pu-Fe fuel. Evidence also indicates the latter treatment to be effective in prevention of plutonium penetration of welds by Pu-Co-

Ce fuel. For example, when carbon was allowed to diffuse into the tantalum grain boundaries from the external surface of the container in a run with Pu-Fe fuel, the tantalum capsule did not fail after 380 hr at 1100°C, whereas without the carburization treatment the lifetime is less than 48 hr (see Fig. 4). Under similar conditions, two tests with 8 g Pu/cc Pu-Co-Ce fuel did not exhibit failure after 700 hr at 1100°C, and one test lasted 1500 hr without failure (see Fig. 4). All tests were discontinued before failure was observed. It is anticipated that exploitation of the pronounced deterrent effect on plutonium penetration of carbon in the tantalum grain boundaries will eliminate IGP in a reactor system.

It should be pointed out that in all the corrosion work mentioned in this paper a "failure" due to IGP is defined as detectable plutonium (by alpha counting) on the surface of the container following a given test. In general, the failures obtained with Pu-Co-Ce fuels have been of the order of $<10,000$ cpm or $<10^{-7}$ g plutonium on the outer surface of the containers. It is obvious that the presence of plutonium on the surface of a capsule of the magnitude described will not constitute a "failure" in the sense of being of operational importance in a reactor. No evidence at this time indicates that a "failure" of $\sim 10^{-7}$ g plutonium will ever progress to a magnitude that will allow *liquid fuel* to flow from the containers into the sodium coolant. In fact, some evidence exists that in certain instances so-called "failures" may become nonfailures if the test is allowed to continue after initial detection of plutonium. Table 1 shows data obtained on two test capsules that are under current investigation. Fur-

FIG. 3. Failure Analysis of PuFe and Pu-Co-Ce Fuels. (Weibull Distribution Plot.)

FIG. 4 Mean Lifetime of Plutonium and Plutonium Alloys Contained in Unalloyed Tantalum. Note the effect of external carburization of the tantalum on lifetime of PuFe and PuCoCe alloys.

TABLE 1 SURFACE PLUTONIUM ALPHA ACTIVITY OF TWO POSSIBLE CAPSULE FAILURES^a

Test Condition	Test Time (hr)	After Pre acid Wash, cpm	After 1st Acid Wash, ^b cpm	After 2nd Acid Wash, cpm	After 3rd Acid Wash, cpm
Ta Container, 8 g Pu/cc PuCoCe, 850°C	1500	2000	250	—	—
	2500	1200	200	100	—
	3500	None	—	—	—
Ta 10W Container, 8 g Pu/cc PuCoCe, 850°C	2500	200	None	—	—
	3500	10,000	1900	500	500
	4500	2000	1100	500	300

^a Failure defined as ≥ 500 cpm after 3 acid washes

^b Acid wash used was HF:HNO₃:H₂SO₄ in 2:2:5 volume ratio for 5 sec.

ther studies are in progress concerning the conditions which govern the size of plutonium leaks.

Mass Transfer

The transition metals Fe, Co, and Ni form intermetallic compounds with refractory metals Nb, Ta,

and W. This intermetallic compound formation has been observed when both Pu-Fe⁽⁴⁾ and Pu-Co-Ce⁽⁵⁾ fuels are in contact with tantalum or its alloys. Figure 5 shows a reaction layer formed between 6.2 g Pu/cc Pu-Co-Ce fuel and tantalum. The formation of this layer presents two potential problems with respect to long-term behavior at high temperatures.

First, since these compounds, TaCo or Ta₂Co, have lower tantalum activities than does pure tantalum, it might be possible to establish a "short circuit" pitting corrosion or spallation if the intermetallic should occur. The pitting would be due to dissolution of bare tantalum followed by precipitation of tantalum as the intermetallic compound at an immediately adjacent sink.

Direct spallation of the TaCo layer has not been observed in any test run to date, although in some cases the precipitated crystals (see Fig. 4) have been dislodged from the surface. However, in no case has

FIG. 5 TaCo Layer Formed on Tantalum by 6.2 g Pu/cc Fuel

the diffusion layer been separated from the tantalum substrate.

No pitting corrosion due to preferential TaCo crystal removal has been observed. However, tests at 1100°C with Pu-Fe fuel (which forms the similar reaction layer TaFe) have definitely indicated pitting

FIG. 6 Pitting Corrosion of Tantalum in Contact with PuFe Alloy for 16 hr at 1100°C. Pit has occurred where Ta-Fe intermetallic compound spalled off the tantalum wall

TABLE 2 EXPERIMENTAL CONDITIONS FOR MASS TRANSFER

Run No	Pu-Co-Ce Fuel Tested, g Pu/cc	Type of Active Tab and Capsule	Temperature Conditions, °C
3	5	Ta capsule, annealed Ta tab at bottom	700 ^a to 610 ^b
4	5	Ta capsule, highly worked tab at bottom	700 ^a to 610 ^b
5	5	Carbided Ta capsule, an- nealed uncarbided Ta tab at bottom	700 ^a to 610 ^b
6	8	Ta capsule, annealed Ta tab at bottom	750 ^a to 650 ^b
7	8	Ta capsule, annealed Ta ring in wall at gas/liq uid interface	750 ^a to 650 ^b

^a Temperature at bottom of capsule

^b Temperature at fuel/gas interface

TABLE 3 OVERALL RATES OF MASS TRANSFER

Run No	Average Rate, mils/year	Maximum Rate, mils/year	Time of Run, hr
3	0.006	0.014	3300
4	0.025	0.038	3100
5	0.038	0.104	2000
6	0.185	0.95	720
7	0.55	6.20	384 (in progress)
Run No	Comments		
3	No Ta ₂ Co formed on the capsule walls. Ta ₂ Co precipitated at gas/liquid interface. Large quantities of Ta ₂ Co formed in the zone between the tab and the bottom end cap of the capsule.		
4	The run was similar to Run 3 except the mass transfer rates were higher by a factor of 3 to 4.		
5	No Ta ₂ Co formed on the carbide layer. The carbide layer along the capsule walls was intact after the run was completed. A thin layer (less than a micron in thickness) which was probably tantalum carbide formed on the active Ta tab. However, the Ta did dissolve from the tab and mass transferred to the gas/liquid interface, where it precipitated as TaC or Ta ₂ C.		
6	Ta ₂ Co crystals precipitated only at the gas/liquid interface. No Ta ₂ Co formed on the walls of the capsule. The overall rate of mass transfer appeared to be controlled by the rate of precipitation at the interface. Periodically, showers of crystals would form at the interface, and during these periods the mass transfer from the tab would markedly increase.		
7	A very large quantity of Ta dissolved from the ring inserted during the first 1½ days. Then, no more transfer was noted. Probably Ta ₂ Co crystals precipitated on the Ta surfaces on and around the ring insert and prevented further mass transfer. The formation of the layer and its structure will be checked by post mortem analysis of the capsule.		

and subsequent IGP through the unprotected container wall in the pit (see Fig. 6).

Since the TaCo intermetallic compound acts as a precipitation sink for tantalum in solution in the fuel, mass transfer of tantalum is expected because of the free energy differences in different parts of the capsule arising from temperature gradients. Thermal-gradient mass transfer of tantalum has been demonstrated by tracer techniques. Tables 2 and 3 summarize the available data. Mass-transfer rates of the magnitudes cited should present little difficulty, providing pitting is not initiated.

The second potential problem posed by the forma-

All sections 1 in. in length.

* = Samples analyzed.

FIG. 7. Composition Variation of the Pu-Co Ce Film that Climbed the Tantalum Alloy Capsule Wall during Testing at 700°C for 500 Hr. Note plutonium enrichment as cobalt concentration decreases due to Ta-Co intermetallic compound formation.

tion of the intermetallic compound is that molten-plutonium alloys all exhibit a "climb" phenomenon on metal walls. This phenomenon is manifest by a <1-3- μ -thick fuel layer climbing on tantalum to heights of 5 in. or more above the meniscus. As the fuel climbs, the cobalt in the alloy forms a TaCo or Ta₂Co intermetallic compound; consequently the plutonium content of the fuel becomes enriched, thereby giving the fuel increased tendency for intergranular penetration of the tantalum wall.

Figure 7 shows the analysis of the fuel film on a capsule wall after exposure for 500 hr at 700°C. Plutonium was observed on the external surface of this capsule by alpha counting in the region of sections 11 and 12, even though no visible plutonium was observed on the internal surfaces of these sections. Leaching of the internal surface with HCl gave results as shown. The enrichment of plutonium is caused by the depletion of cobalt via intermetallic formation up to approximately 5.5 in. above the fuel level. Since about a 10°C temperature gradient exists along this length of capsule (top ~10° cooler than the fuel level), further enrichment in plutonium is caused by sliding along isotherms of the phase diagram (see Fig. 1), caused by precipitation of β Ce at approximately 700°C and 4.5 a/o Co.

Although plutonium was found by chemical analysis in the section 10, the mass of plutonium involved was extremely small, i.e., 11.625 mg. Consequently, it is not expected that a leak in this area would cause macroscopic amounts of fuel to be lost from the capsule. It is known that the presence of a Ta-Co or other intermetallic compound prevents diffusion of plutonium in tantalum grain boundaries. Consequently, the method that would prevent IGP is to prevent the fuel film from forming TaCo as it climbs.

Preliminary work indicates that the presence of a Ta_xC layer on the container surface completely prevents Ta-Co intermetallic layer formation and may be self-healing. The carbide layer is established by bulk diffusion of carbon into the tantalum and is not subject to spallation in thin layers (i.e., <5 μ), although spallation of a portion of the layer (at the TaC-Ta₂C interface) has been observed with thick layers (i.e., 10-15 μ). Run 5 (see Table 3) indicates that although no Ta₂Co formed as a sink for the active tantalum in the carburized-container run, the dissolved tantalum apparently precipitated as Ta_xC and transferred at a rate approximately 4 times that for the uncarburized system. However, the rate observed, i.e., 0.04 mil/yr, is considered not to be significant from a practical standpoint.

The presence of a carburized surface has been shown to enhance the IGP resistance of tantalum as well as to prevent Ta₂Co intermetallic compound formation. Therefore, because of the uncertainty associated with the tantalum mass-transport phenomenon and pitting corrosion, a carbide layer has been specified for the reference capsule of the MPBE core, until and unless it can be proven by further experimentation that such a layer is not necessary.

Mechanical Properties of the Fuel Container

The mechanical properties of the container, as well as its resistance to corrosion, are important considerations in selecting a suitable container material. Tungsten additions to tantalum have pronounced effects on hardness, ultimate and yield strengths, and total per cent elongation. Increasing tungsten content of the alloy rapidly increases ultimate and yield strengths, and moderately lowers percentage elongation. Figure 8 shows yield strengths and ultimate ten-

FIG. 8. Elevated-temperature Yield Strength Properties for Ta, Ta-5 w/o W, and Ta-10 w/o W Sheet.

side strengths for Ta, Ta-5 w/o W, and Ta-10 w/o W alloys. The high temperature yield strength of Ta is markedly improved by W additions. It is seen that for temperatures of interest for fast reactor applications (500–750°C) the yield strength of Ta-5 w/o W is 5–7 times that for tantalum. Consequently, since container strength is important for preventing capsule distortion in the event of inadvertent freezing of the fuel, and since tungsten additions to tantalum may be beneficial to corrosion performance, the material selected for use in the MPBE is Ta-5 w/o W.

Irradiation Studies

The effect of irradiation on the mechanical properties of Ta-W alloys is virtually unknown. Room-temperature tensile tests on tantalum irradiated to $\sim 9.4 \times 10^{19}$ nvt (fast) indicates an increase in yield strength of greater than 200% and an elongation decrease of $\sim 80\%$.⁽⁹⁾ New data on the effect of irradiation on the room-temperature tensile properties of tantalum and Ta-10 w/o W have been recently re-

ported.⁽¹⁰⁾ However, information concerning the quality of the test material is inadequate, and the data are of little value in predicting the behavior of Ta-5 w/o W during irradiation.

Body-centered-cubic materials show virtually complete recovery of mechanical properties at $\sim 0.35 T/T_m$ (ratio of absolute test temperature to absolute melting temperature).⁽¹¹⁾ Based on irradiation data on the high-temperature mechanical properties of W, Mo, and Nb, it is expected that Ta-5 w/o W will demonstrate an irradiation-induced increase of yield strength and a reduction of total elongation since the temperature range of the container in the reactor is between 0.235 and 0.30 T/T_m . The magnitude of these changes of mechanical properties after fast-neutron irradiation is under investigation.

The effect of fission product recoil damage on both Ta(W)-Co reaction layers and Ta(W)-C layers is being studied by irradiation testing in the OWR, a 5-MW thermal-flux research reactor at LASL. Because of the fission density profile obtained in such a test, the tantalum wall actually is exposed to a total fission fragment generation density that is 5–6 times the average for the fuel element. Consequently, such testing is a way of intensifying the fission recoil into the container surface and obtaining large equivalent surface effects of burnup relative to a fast reactor environment which has a flat fission density across the fuel diameter.

The first of these experiments (OWREX-1) designed to investigate wall effects has been completed. The experimental fuel element consisted of an 8-in.-long capsule containing ~ 2.2 in. of 6.2-g Pu/cc Pu-Co-Ce fuel [38.5 a/o (58.0 w/o) Pu, 24.6 a/o (9.0 w/o) Co, balance Ce]. The container was an unalloyed, deep-drawn tantalum capsule. Fuel heat was removed by a naturally convecting sodium-coolant system with the bottom of the fuel maintained at $\sim 470^\circ\text{C}$ and the top at $\sim 600^\circ\text{C}$.

The average burnup of this capsule was calculated to be 2.5×10^{19} fissions/cc or 0.15 a/o Pu burnup. Radiochemical burnup analysis has not yet been completed. The fission density at the wall was calculated to be 1.25×10^{20} fission/cc. Figure 9 shows a postirradiation radiograph of this capsule. Significant features shown in the radiograph are the exceptionally clean meniscus and the presence of fission-gas bubbles along the capsule wall (similar to those observed in LAMPRE-I fuel capsules). Metallographic examination showed no wall effects other than those expected from an equivalent thermal treatment out-of-reactor (e.g., Ta_2Co being formed at the meniscus-wall interface).

The second capsule (OWREX-2), identical to

FIG. 9. Postirradiation Radiograph of OWREX-1 Irradiation Capsule. Fuel Composition 6.2 g Pu/cc; Average Burnup; ~ 0.15 a/o Pu.

OWREX-1, was irradiated to ~ 1 a/o Pu burnup, or 1.6×10^{20} fissions/cc, with a fission density at the container wall equivalent to 6 a/o Pu burnup. A postirradiation radiograph of this capsule is shown in Fig. 10. The radiograph shows this capsule to be substantially the same as OWREX-1. Since postirradiation examination of this capsule is still in progress, no conclusions can be drawn regarding wall effects. However, although fuel enrichment is expected

FIG. 10. Postirradiation Radiograph of Fueled Region of OWREX-2 Irradiation Capsule. Fuel Composition 6.2 g Pu/cc; Average Burnup; 0.9 a/o Pu.

in the gas-phase portion of the capsule by "fuel climb," the capsule was "swipe clean" with respect to plutonium contamination, indicating that plutonium did not exhibit intergranular penetration of the capsule during the test (~2400 hr at temperature).

A further significant point is that these two capsules are loaded with the same amount of fuel and that both are the same height (to within a few mils) after irradiation. This result indicates that fission-gas release reached a steady state within the 2-week operational period of OWREX-1 and that volume percent of bubbles within the fuel remained constant after an initial transient condition.

Two experiments are being inserted in the OWR that are designed to determine the irradiation effect on Ta-5 w/o W containers with and without a carbide coating. It is anticipated these capsules will be run to at least 3% average plutonium burnup.

Conclusions

Our studies of potential core materials for fast reactors may be summarized as follows. Molten Pu-Co-Ce fuel alloys appear to be much less corrosive than the Pu-Fe eutectic alloy used in LAMPRE. Ta-5 w/o W alloy, selected as a core container, has higher yield strength by at least a factor of 5 than tantalum in the unirradiated condition, and it is expected that this increased strength will minimize or eliminate capsule distortion if the fuel is frozen. Although several major questions regarding fuel irradiation performance still must be answered (e.g., the effect of high fission products/cc on the chemical behavior of the fuel, the rate of fission-gas release at high power densities, etc.), Pu-Co-Ce alloys offer considerable potential as a practical fast reactor fuel.

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Discussion

Mr. Lawroski (ANL): From the inception of this program I recall that you had problems in acquiring tantalum of the right purity and tantalum technology. It is obvious that over the years you have made considerable progress as reported here in the ability to keep these molten alloys for long durations. Would you care to comment as to what have been some of the major improvements in the tantalum technology over and above the additions of tungsten that you mention?

Mr. Kirkbride: We have succeeded in getting the tantalum too pure. I think this is clear from the effect of carbon on the penetration. This is the old problem of establishing a maximum in your specifications on impurities and not minima, and I believe that we've got to a point now where we are actually too pure. We are routinely getting seamless drawings of tantalum tubing with 100 ppm total impurities, not just interstitials, but total impurities. This is just too pure, and now we are having to go back and add carbon in a selective manner. Of course, the cost of tantalum has come down considerably, and routinely tantalum tubing to meet the best of tubing specifications can be obtained for \$30-50/ft.

Mr. Lawroski: How about the matter of welding?

Mr. Kirkbride: Again welding is a problem. The problem is again one of purity. We are not introducing any contamination normally in our welds, even arc welds; TIG welds are coming out much softer than the base material, maybe 80 or 70 dph, for instance.

Mr. Kummerer (Karlsruhe): As regards cobalt-containing fuel, how do you consider the drawback of the high cobalt activity in the light of the reprocessing difficulties?

Mr. Kirkbride: Since it's a fast reactor we don't particularly get high cobalt activity, not as you would in a thermal reactor. Of course, in the thermal irradiations the capsules come out very warm. But for instance in LAMPRE, the stainless steels with cobalt in them don't become too active. I think it would cause a problem, just because we do have 10 w/o cobalt in the fuel, but I don't think this is an overriding economic consideration.

Mr. Kummerer: But in fast flux are not the cobalt cross sections comparatively higher than those of the other materials?

Mr. Kirkbride: Yes. You pay a penalty; there must be a balance between penalties and advantages.

Mr. Lawroski: Have you studied the addition of the uranium to these alloys?

Mr. Kirkbride: Yes. We tried for a considerable number of years to find a low-melting internal breeding fuel, a uranium-plutonium system. It turns out that the uranium-plutonium-manganese system is about the lowest melting, at 738°C. This is a pretty large price to pay in that the coolant temperature is too high if you are going to make this a molten system. Besides that, the uranium-plutonium-manganese system is really a universal solvent. It will dissolve almost anything, so we have given that up as a bad cause.

Mr. Blumenthal (ANL): To what extent will the fission gases fill the melt and restore a kind of a uniformity of the melt?

Mr. Kirkbride: In a fast system one obviously has convection because the temperature gradient that's induced across the capsule, and also the longitudinal gradient in the capsule actually causes quite a convection. However, in LAMPRE, which had a flat fission density across the fuel, we did only see bubbles along the walls. In this particular case, the longitudinal temperature gradient and the power density tend to counteract one another, and I am not sure that we are getting any convection in our irradiations in the thermal flux. However, it is quite obvious that there must be some kind of convection as bubbles break from the wall and rise. But I don't anticipate that the fission gas, in itself, is going to cause any appreciable stirring; it is going to be a thermal-convection stirring. Fission gases are growing too slowly to make much of a contribution at all.

Mr. Lawroski: In our pyrochemical studies for reprocessing we don't require the long durations for

which you would have to provide a solution if you wanted to incorporate substantial amounts of uranium along with plutonium and magnesium.

Mr. Kirkbride: That's a good point. We were successful in containing this alloy for several thousand hours, but that is about it.

Mr. Zegler (ANL): Is there any concern about the homogeneity of these liquids at temperature?

Mr. Kirkbride: As far as we have been able to determine, they are homogeneous; there are no two-phase, two-liquid regions in the system; there are no solid phases outside, except perhaps precipitated fission products. We see no indication whatsoever that these materials are not totally 100% homogeneous, as you would expect. Even though there is quite a density mismatch between components, once you get the atoms mixed, they are mixed. However, solidification can cause segregation. If that occurs, there may be considerable difficulty in getting the liquid homogeneous again, because then it can only become homogeneous by diffusion process. When we make these alloys initially, they are stirred and melted, cast, recast, and they come out very homogeneous.

Mr. Lawroski: This morning we heard considerable progress reported concerning the irradiation capability in carbide and metallic type fuels, and just now we have learned about the molten metal fuel. But we have not heard very much about any change in situation concerning irradiation for the oxide fuels. It was back in 1961, I believe, when it was first reported that oxide had been taken up to about 100,000 MWd/T, although I must admit I did not attend all the presentations. I have not heard if there has been any further progress in the irradiation of oxide fuels. There are, of course, a considerable amount reported in a better understanding of some of the factors involved with oxide. I take this opportunity to mention it now, so that those of you who have been working on oxide fuel will be prepared to make some comment on this point at the end of the session.

The Application of Experimental Data from TREAT Meltdown Studies to Reactor Accident Analysis

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During a recent study⁽¹⁾ of the coupled-reactor concept at Atomics International, it was necessary to describe the behavior of the core during very large reactivity insertions under various conditions of protective action. Revisions were made to the digital code AIREK,⁽³⁾ which was then used to treat the neutron kinetics together with an approximate thermal model of the core during reactor excursions. Because the reference core was fueled with mixed oxides and was operated at relatively high specific power levels, the magnitude of tolerable insertions was usually determined by gross fuel melting. The possible course of events subsequent to fuel melting and the manner in which changes would occur in the distribution of materials within the core was interpreted from recent AI experiments⁽²⁾ in TREAT in which ceramic-fuel meltdown in sodium had been studied.

This effort represents an initial attempt to place the models for maximum accidents on a more physically realistic basis. It should also be noted that if the mechanisms of fuel dispersion as described in this study can be more completely characterized and generalized, the dependence on the Bethe-Tait criterion can be reduced.

A. Pertinent Information from TREAT Studies

To date, the AI TREAT experiments have included examination of the transient heating behavior of relatively large-diameter (0.85-in.), hypostoichiometric, uranium carbide fuel, sodium bonded to 10- and 20-mil-wall stainless-steel tubing in a stagnant sodium environment. These TREAT transients have produced melting of the central portions of the fuel and, in one case, included gross melting of the fuel, temperature levels of about 6000°F, and substantial redistribution of materials within the test capsule. It is this latter case which has generated experimental evidence regarding the possible course of meltdown, and the results of that experiment will now be reviewed briefly.

The capsule design is shown in Figure 1. Figure 2

presents the thermal and pressure behavior during the transient, and Table 1 describes the various events which are taking place. Figure 3 shows X radiographs of the capsule prior to irradiation and following the experiment. Figure 4 is a photograph of the capsule after vertical sectioning.

For present purposes, the gross dispersion of fuel throughout the capsule is most notable. During capsule disassembly, it was observed that the sodium was not present at the bottom of the graphite crucible, but rather was frozen on the surfaces of the capsule as were substantial portions of the fuel and cladding. Some of the fuel was present in two or three larger pieces, but these together probably comprised less than half of the original fuel slug.

As a result of these observations and analysis of the thermal and pressure data, the following conclusions were drawn:

- 1) The rapid transfer of thermal energy from molten fuel and clad to coolant produced large local pressures of sodium vapor which caused the observed gross dispersal of the fuel mixture. This phenomenon should also be observed for oxide meltdown in sodium, since a large part of the energy exchange is likely to take place by radiation. The application of the present results to oxide fuel assumes energy transfer similar to the carbide behavior.

- 2) The fuel appears to have possessed considerable momentum since it was forced through fairly small openings in the system. On this basis, it is assumed that in reactor situations the disassembly can occur over distances of a few feet, and the approximate models of fuel motion incorporated this assumption.

- 3) The sodium bond generated rather modest pressures prior to cladding failure, and it appears that either film boiling occurred in the bond space (with liquid sodium adhering to the cladding) or that the bond region was completely voided.

- 4) As a result of the above observation of pressure and thermal behavior, it is believed that the presence

Fig. 1. TREAT UC Capsule Assembly.

of the sodium bond did not play a major role in the meltdown process.

5. There was a considerable time lag (approximately 1 sec) following completion of the burst and prior to cladding failure. Such a lag could be associated with the time required for the surface temperatures of the UC to reach melting, the time (probably minimal) required for collapse of the fuel in the clad, and the time to melt or react with the clad.

6) The time lag for a gas-bonded pin would be expected to be somewhat shorter, because the temperatures of the fuel surface are higher and rise more rapidly than in the case of a liquid-metal-bonded pin.

It should be noted that in the capsule experiment the energy generation within the fuel had ceased long before fuel motion began, so that the motion in a large core with residual (or high) power levels would be likely to be more extensive.

B. Considerations in Describing Meltdown for a Large Core

Fuel melting in a large core could occur in a variety of ways and for a number of reasons; loss of coolant, channel blockage, inadvertent high enrichment, of the fuel or increased system (or local) reactivity are all possible causes of fuel melting. The extrapolation from the simple conditions of the experiment to the complex situations which may arise in reactor operation cannot be made on any rigorous basis.

However, there is one case which is amenable to description, namely, the condition in which symmetric axial and radial flux gradients exist within the core and in which the temperature distribution in the fuel resembles the flux distribution (based on average fuel temperature). Under these conditions, a reactivity transient which produces no gross changes in the flux shape of the core would be likely to produce molten fuel in the center of the core, and the

region of molten fuel would assume some approximately cylindrical shape which expands both axially and radially. It is for this condition that the results of the TREAT meltdown study were applied.

The reactivity effects associated with two types of fuel motion were calculated, and these effects were incorporated into the AIREK kinetics code. This modified code will now be described, followed by a description of the method of calculating reactivity changes associated with fuel motion, and the results for the coupled core design will then be presented.

C. AIREK 3C Description

The digital reactor kinetics code AIREK 3C^(1, 3) was employed for the analyses of both moderate and severe accidents of the coupled core. This code treated four channels: the maximum and average rod in the fast region, and the average rod in each of the radial blankets. There were three axial nodes per channel. The heat transfer and flow could be varied from channel to channel.

In the transient condition, the neutron economy is time, energy, and space dependent. AIREK 3C treated the time dependence correctly, included the effects of delayed neutrons, and neglected changes in the energy spectrum and flux shape. Doppler and void reactivity feedbacks are about one order of magnitude more important than coolant and structural temperature coefficients or expansion coefficients. Consequently, for accidents in which important changes in configuration did not occur, Doppler and void were the only feedbacks considered. Fuel melting and coolant vaporization were incorporated into the analysis, while temperature dependence of thermal properties and heat capacities associated with clad and structures were neglected. Axial, radial, and local nonrandom peaking factors were employed in determining the power and temperature levels of the maxi-

a. Reactor Power

d. Fuel Temperatures

b. Energy

e. Bond Temperatures

c. Fuel-Section Pressure

f. Coolant Temperatures

FIG. 2. Data from TREAT Capsule Experiment.

mum channel. An overall fuel-coolant heat transfer coefficient was employed. Inlet coolant temperature and coolant flow rates were not varied during a non-boiling transient. In order to simulate pump-loss or pipe-rupture accidents, flow was initially set at some fraction of full flow. Flow was also changed to some new value when boiling occurred to simulate a flow reduction due to two-phase fluid pressure drop.

D. Results of Calculations for Coupled Core

Calculations were performed using a 15-group diffusion code (CAESAR) to determine the changes in reactivity in a coupled core module associated with voiding from 1 to 10% of the center of the core or fast region. Two calculational models were used to approximate the successive effects of fuel redistribution within a module:

- 1) cylindrical—cylinders of material of appropriate size removed from core;
- 2) spherical—spherical void growth in center of core with uniform redistribution of this material into remainder of core.

The composition and geometry of the reference module are given in Table 2 and Figure 5. The results of these calculations are presented in Figure 6.

TABLE 1. ANALYSIS OF TREAT DATA

Time (sec)	Description
0.45	About 50% of total energy released. Central fuel temperature was $\sim 2200^{\circ}\text{F}$.
0.5 to 0.6	Bond TC's fail (note decrease in readings to 1500°F). Small pulses (few psi—not seen on data printout) on fuel-section pressure transducer—fluctuations ceased from 0.8 to 1.7 sec.
0.7	$>90\%$ of energy released. Central fuel temperature read $\sim 4000^{\circ}\text{F}$.
1.0	Burst complete. Central fuel temperature read $\sim 5200^{\circ}\text{F}$. For ~ 0.6 sec, coolant temperatures level at $1200\text{--}1300^{\circ}\text{F}$ (these events are associated with time to melt edge of fuel, sag into clad, and heat and melt or react with clad).
1.7 to 2.0	Central fuel temperature reading dropped 1000°F . Bond temperature readings fluctuated severely. Coolant temperatures fluctuated severely. Pressure pulses up to ~ 100 psi occurred several times at ~ 0.2 -sec intervals (associated with failure of cladding and motion of molten fuel and clad into stagnant coolant, rapid energy transfer to coolant with subsequent large local coolant pressures producing fuel dispersion).

FIG. 3. X Radiographs of Capsule 1 before and after Irradiation.

To demonstrate the incorporation of fuel redistribution into the AIREK code, the reference coupled core design will be used. Consideration is given to an accident in which the control rods are ejected at a rapid rate and in which protective action does not occur so that residual power levels produce eventual melting of the gas-bonded oxide fuel in the peaked regions of the core. Based on the temperature distribution within the core and the rate of temperature rise of the fuel, the rate at which melting propagates from the center of the core was estimated. From the more conservative calculation of associated reactivity effects for the spherical model, the rate of change in reactivity due to displacement of fuel from the center of the core to the outer regions was determined. At the point of gross fuel melting, the reactivity effect was triggered. In the case under consideration, the redistribution resulted in rather large negative reactivity effects, so that propagation of meltdown would not proceed more than a few percent, at which point the core would be

FIG. 4. Disassembled Capsule after Irradiation.

TABLE 2. COMPOSITION (v/o) OF REFERENCE COUPLED CORE MODULE

	Core	Inner Blanket	Outer Blanket
U-238	10.2	22.4	22.6
Pu-239, -240, -241, -242	5.2	0.4	0.2
Oxygen	32.9	45.8	45.8
Iron	28.8	22.8	22.8
Sodium	21.9	8.6	8.6
Fission Products (moderator is 100% C)	1.0		

quite subcritical. The results of the analysis are presented in Figure 7. The model could be improved by a better understanding of the role of the coolant, the effects on the structures, and the functioning of safety

and protective mechanisms (emergency cooling systems, etc.).

E. Discussion of Parameters which Influence Melt-down Behavior

Heating rate will exert an extremely important influence on the course of meltdown, since rapid heating rates will produce high fuel temperatures and relatively minor heat transfer to other materials in the core, whereas slow heating rates could produce coolant vaporization, cladding melting, or failure prior to gross fuel melting. Thus, the initial temperature distribution and the rate of heating during the excursion will play a major role in the course of events.

a) CYLINDRICAL

b) SPHERICAL

FIG. 5. Geometry of Reference Coupled Reactor Module.

FIG. 6. Results of Coupled Reactor Reactivity Calculations for Fuel Motion.

FIG. 7. Results of AIREK Analysis of Coupled Reactor Control Rod Ejection Accident with Disassembly Model.

Similarly, the fuel-rod design, including fuel and clad material, type of bond, use of vented elements, and geometry, will also influence the sequence of events.

The interesting part of the TREAT study was the minor role of the bond and the time lag associated with cladding penetration. These results imply that heating rates which produce melting in the millisecond-to-second time interval might not produce very different results provided that maximum temperatures do not rise above the boiling point of the fuel. This contention should be checked by TREAT data on ceramic-fuel meltdown at slower and faster heating rates. Quite clearly, the extent of heating plays a major role, and it appears that for failure of ceramic-fuel elements, sufficient energy must be deposited in the fuel to produce fairly complete melting of the fuel slug. Energy levels which produce fuel temperatures well above the fuel boiling point (at the pressure in the fuel rod) are likely to result in different consequences than those observed.

Since the failure observed was a result of slow cladding penetration by molten fuel rather than clad rupture, quite different behavior could occur in highly pressurized fuel rods. However, vented fuel elements would be expected to behave in a fashion similar to that observed in the experiment.

The core design, coolant flow, and pressure distribution will affect the course of material redistribution; however, the most important parameter is the temperature distribution in the core, since it will produce appreciable differences in the rate of propagation of molten regions of the core following melting of the hottest portions.

Finally, further work on the melting process in ceramic fuels is required to study the consequence of long-term fuel irradiation, such as the foaming which has been observed to occur upon meltdown of highly irradiated metallic fuel.

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Discussion

Mr. Zebroski (GE): I think you know about the results of a long series of similar TREAT tests with oxide fuel capsules which have been extensively published. Many of these went well into the melting range and one into the high boiling range, that is, the UO_2 boiled and reached a pressure of 800 psi. Yet in none of these did we have any explosive kind of behavior in the sodium bond, nor disruption of the capsule nor even of the fuel cladding. Do you think that this might be related to the much shorter time constant of release of heat of the carbide fuel?

Mr. Stachura: The case of the oxide fuel is somewhat different from the bonded case because the gas gap produces a rather large temperature difference from the edge of the fuel to the cladding. The cladding is kept very cold throughout the course of the burst. Although central temperatures well above melting, as high as 7000°F, I believe, were reached in some cases, at no time did the edge of the fuel ever reach these temperatures. In specimen C-2-C the edge of the fuel was molten, but the melting of the fuel at the edge did not cause penetration of the clad. It may be possible that the edge was at the melting point and the slight energy of fusion to melt it partially may have been transferred to the cladding which was still cool because of being in contact with the stagnant liquid coolant. There really wasn't sufficient energy available to lead to penetration of the cladding. When the cladding transfers heat to the stagnant coolants, which transfers heat to the heat sink, it takes quite a considerable amount of energy to reach the threshold at which the cladding will fail. It is difficult to understand how energy may continually be pumped into the oxide pin to very high values and never lead to failure of the element. I think there is going to be a point at which failure will ultimately occur, and it just has not been attained.

Mr. Zebroski: I wonder if the difference might not be characterized by a difference in the peak instantaneous heat flux prior to failure. I think the oxide

rods never went much above a typical full-power heat flux or perhaps a full-power peak heat flux of perhaps around a million to a million and a half. I would suspect that with the shorter time constant in the sodium bond you probably ran heat fluxes of the order of 3 to 5 million.

Mr. Stachura: We did until the point at which the coolant temperature remained fairly constant. At that point, the boiling of the bond probably reduces the heat transfer quite considerably. By means of analog thermal models we have found that at the time the bond reaches the boiling temperature, there is imposed the resistance of sodium vapor or something corresponding to a film boiling coefficient. We rather accurately predicted thermal behavior; in fact, in the case of the large burst, we allowed the fuel to sag into the clad, thereby reducing by half the conductivity of fuel. The temperature rise took from $1\frac{1}{2}$ to 2 sec; this was reproduced in this analysis, and finally the cladding reached melting temperatures.

Mr. Zebroski: Do you know the peak heat flux?

Mr. Stachura: During the early part of the burst when the sodium bond is still at 1800°F, I imagine it is several million; during the last part of the burst, it must go down considerably.

Mr. Dalle-Donne (Karlsruhe): I don't think that from your results you can deduce that you will never have a large superheat in a sodium-cooled reactor. I don't think that the main effect is due to the smoothness of the surfaces, because unfortunately sodium, if it is in forced convection, after a certain time fills the biggest holes in the wall, if you like, so only the smallest holes remain active. This means a very high superheat.

Mr. Stachura: Yes, the experiments of Rohsenow and Marto, I think, show that there was even a hysteresis effect. But after boiling started at more modest superheats and proceeded for a while, then in order to sustain boiling at these sites rather large superheats were required. I think this definitely is related, however, to the size of the activation sites. They even found that it was related to the nature of the materials from which they were boiling. I will certainly agree that we cannot say we were really not measuring surface temperature. It was not a result that was designed to detect exactly the superheat. Quite clearly you have to have some superheat to form the vapor bubbles. The main point was to show that here was a case in which heat was transferred to the sodium to create the bubbles. I'm not sure whether the forced convection is as effective in eliminating the inert gases from the site as are the boiling processes themselves when the liquid is collapsing at these sites.

There may be some problems associated with making inactive sites that, let's say, are initially active. However, I would think that we could roughen cladding surfaces to avoid any real serious superheat problem.

Mr. Dalle-Donne: That method was tried, but it did not reduce the superheat very much. One thing which could be effective would be the presence of an impurity in sodium that modified the surface tension; maybe that is the reason that you didn't find any superheat.

Mr. Noyes (AI): On the question of superheat, I think there is one factor that we haven't mentioned in this discussion, namely, surface heat flux. Rohsenow and Marto's experiment showed, among other things, that the superheat phenomenon was heat-flux-dependent, and the Oak Ridge experiments were mostly done at low heat flux. At high heat fluxes, the transition is something around 1 or 2×10^5 heat flux. Now in the reactor and in these experiments, the heat fluxes were of the order of 10^6 . You're much less likely to experience superheating under these high heat fluxes than you would expect under the low heat fluxes at which the Oak Ridge experiments were done. Here is a factor which needs to be kept in mind in the consideration of superheat questions.

Mr. Stachura: Was their system closed so that the gradient through the sodium could have been small enough that inert gas pressure could have actually resulted in some pressurization of the system to also inhibit boiling formation of bubbles?

Mr. Dalle-Donne: I think the important factor here is not heat flux. It is the gradient of temperature perpendicular to the wall that is important, because from that depends how the bubble can grow. I think the smaller this gradient is, the easier it is to superheat. You can always think of an accident in a sodium-cooled reactor in which the temperature of the sodium in a channel or in a subassembly is practically

three-phase mixtures, etc. I don't think any energetic reactions were involved. I believe the pressure pulses were due primarily to the large local temperatures of the sodium that were generated upon passage of energy from the fuel to the sodium. These local pres-

ures transmitted the pulse, which was damped quite a bit since it had to go through some vapor space; nevertheless they were the cause for these pulses. The only materials in the capsules were fuel, stainless steel, tantalum, and thermocouple materials.

Mixed Oxide Fuel Movement at Various Power Levels and in Transients*

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I. Introduction

A fast reactor fuel typically is not deployed in its most reactive configuration. Consequently, fast reactor cores are subject to possible positive reactivity changes due to fuel movement,⁽¹⁾ the sensitivity being greater in small, high-leakage cores. Movement of fuel could also affect its exposure lifetime. In this paper we will summarize our experimental observations on the movements of mixed oxide† fuel within the cladding in various fuel-temperature regimes as they relate to (a) fuel-cladding interactions and (b) reactivity changes.

II. Fuel-cladding Mechanical Interactions

The basic question of interest from the standpoint of the operating life of a fuel element is whether the differential expansion of the fuel relative to that of its cladding will generate fuel-cladding mechanical interactions which, in the extreme, could lead to distortion or failure of the cladding. It is normal to provide sufficient as-fabricated void, in particular a diametral gap, such that the differential thermal expansion is accommodated within the fuel and stresses are not introduced in the cladding. Were movement of the fuel to alter this initial design condition, a potential for stressing the cladding could develop.

In-pile experiments in which the fuel density, radial gap and/or axial gap have been varied indicate that the differential thermal expansion can be accommodated within the range of values of interest in an FCR. For example, mixed oxide fuel pins irradiated for short term in EBR-II at power levels up to 16 kW/ft exhibited no cladding distortion.⁽²⁾ This dimensional stability is exhibited despite thermal-stress fracture of the fuel due to temperature gradients, which is typical in oxide fuels. The consequences of fuel fracture appear to be alleviated by the facts that: (a) the size into which oxide fuel fragments is limited,

(b) crack healing occurs during heating of the fuel. In support of these observations, power cycling of oxide fuel pins which employed a self-supporting cladding has revealed no evidence of strain cycling.^(3, 6, 7) Data have been reported⁽⁸⁾ which suggests that mixed oxide fuel exhibits better resistance to thermal fracture than UO_2 . We have irradiated the two materials under identical conditions and found no significant difference in the fracture behavior (see Fig. 1).

Radial migration of fuel away from the center is commonly observed at central fuel temperatures above about 1800°C, but below melting. A central void forms in a matter of minutes, effectively reducing the central temperature. The primary transport mechanism appears to be vaporization from the hot side of a pore and condensation on the cold side. The columnar grains and central void in the inner half of the fast-flux-irradiated, mixed oxide fuel in Fig. 2 are the result of this type of movement.

Mixed-oxide irradiations at equivalent power levels of the same Pu/U ratio but different stoichiometries have revealed⁽⁹⁾ less redistribution in fuel with a slight oxygen deficiency as compared to the fuel with excess oxygen. This is evident from the relative size of the central void (see Fig. 3). In addition, irradiation of an instrumented capsule containing stoichiometric UO_2 and hypostoichiometric ($\text{O/M} = 1.97$) mixed oxide pins in tandem at equal rod power indicates⁽¹⁰⁾ that radial migration in the $\text{PuO}_2\text{-UO}_2$ is less despite operation at somewhat higher temperatures (see Fig. 4).

Radial relocation of the fuel does not appear to influence fuel-cladding interactions in the steady state. The accessibility of the void to the fuel and plasticity of the mixed oxide are believed to be contributing factors to the absence of cladding strain despite the radial movement. Circumferential restraint on the fuel column by the cladding and by the outer, cooler fuel regions is substantial even under FCR conditions. The plastic portion of the fuel can flow and occupy the available void volume rather than stretch the cladding.

* This work was performed under USAEC Contract AT(04-3)-189, Project Agreement No. 10.

† Mixed oxide as used in this paper refers to a solid solution of PuO_2 and UO_2 , a typical composition being 20 w/o PuO_2 and 80 w/o UO_2 .

MIXED OXIDE

UO₂

10X

FIG. 1. Similarity of Fracture Pattern in Irradiated Mixed Oxide and UO₂.

100X

FIG. 2. Diametral Composite of Mixed Oxide Irradiated in a Fast Flux for Short Term at 15 kW/ft.

Another source of fuel expansion at lower powers and high burnups is fuel swelling due to the combined expansion effects of solid fission product agglomeration and retained fission gas pressure. The reported swelling rates for UO₂ and PuO₂ cover a broad range, viz., 0.6 to 1.7% ΔV per a/o burnup.^(11, 12) The same studies indicated that the swelling rate is inversely related to the original void volume and that an irradiation-induced plasticity occurs. Thus one can speculate that fuel swelling could be accommodated by provision of appropriate void within the fuel pin.

In investigation of fuel swelling, measurable fission-gas porosity (see Fig. 5) was observed⁽⁹⁾ in the cooler portion of a high-burnup (70,000 MWD/MT), high-shear-density (92% of theoretical density), mixed oxide fuel pin. The power level in this section was approximately 10 kW/ft, and the porosity occupied about 15% of the cross section. Additional evidence of fuel movement in this region was the disappearance of the as-fabricated radial gap. Nevertheless, the diameter in this region of the fuel pin exhibited only a slight increase, which suggests that the swell-

HYPO STOICHIOMETRIC

HYPER STOICHIOMETRIC

70,000 MWD/T

FIG. 3 Effect of Stoichiometry on Radial Fuel Movement in Mixed Oxide Fuel

AS ETCHED

20 W/O PuO₂ 80 W/O UO₂ (o/m 1.97)

50X

AS ETCHED

UO₂ (o/m 2.00)

50X

FIG. 4 Comparison of Radial Migration in UO₂ and Mixed Oxide Irradiated at Equal Rod Powers

ing was largely accommodated in this high-density fuel pin. More precise experiments to evaluate the extent of swelling and its accommodation under representative FCR conditions are in progress, both in fast and thermal spectra.

When the mixed oxide fuel is operated above the solidus temperature, an additional expansion is contributed by the change in volume on melting. Indirect in-pile measurements⁽¹⁰⁻¹³⁾ of the mixed oxide indicate that the molten volumetric expansion is of the order of 8 to 12%, which is comparable to the 7 to 10% ΔV experienced by UO₂. A significant result

from steady-state high-power tests⁽¹³⁾ is the demonstration that the molten expansion *per se* can be accommodated in axially unrestrained, short fuel segments if sufficient void space is available. This same response has been displayed during transient irradiations in which up to 75% of the fuel volume became molten very rapidly.⁽¹⁴⁾

Fuel movement and fission-gas generation during steady-state irradiation may indirectly bear upon the subsequent transient fuel performance. Transient testing of a non-pre-irradiated and a pre-irradiated (7900 MWD/MT at high power) specimen under dupli-

cate conditions, during which three-quarters of the fuel was molten, revealed that cladding deformation occurred only in the latter pin.⁽¹⁵⁾ It was surmised that fission gases released during the transient were trapped in the fuel, could not escape into the gas plenum, and thus exerted an appreciable pressure against the cladding. Further transient testing of fuel pre-irradiated to higher burnups is in progress. It should be noted that pre-irradiating and transient irradiating in a thermal flux represents a more severe

test of the fuel than irradiation in a fast-flux environment due to the flux-depression effects in the thermal spectrum.

III. Reactivity Effects

Potential sources of Δk due to fuel movement in a fast reactor include:

1. subassembly bowing;
2. Pu-U separation;
3. thermal expansion of the fuel column;
4. axial redistribution of fuel.

Subassembly bowing is beyond the scope of this paper. Differential migration of plutonium and its effect on the Doppler coefficient are covered in a separate paper.⁽¹⁶⁾ In a large reactor the negative reactivity effect from thermal expansion is relatively small due to the inherently low neutron leakage. Furthermore, the axial expansion of fuel under irradiation is not fully predictable. A conservative approach thus takes no credit for this mode of fuel movement.

We will confine our attention to the item of fuel redistribution. Reactivity insertion rates due to fuel redistribution would be of concern in the event that both the magnitude of the movement toward the reactor midplane were large and such movement were rapid. A generalized assessment of the effects of fuel transport on reactor physics and control is not practical, principally because the consequences are a function of the specific reactor design (core size, shape, etc.). For example, a comparative analysis⁽¹⁷⁾ of four ceramic-fueled reactor concepts indicates that the reactivity effects from axial translation of fuel differ notably and would be minimized by a pancake-

As Polished

250×

FIG. 5. Fission-gas Bubbles in High-burnup Mixed Oxide Fuel.

TABLE 1. AXIAL DISTRIBUTION OF IRRADIATED MIXED OXIDE FUEL

Specimen Number	Fuel Length (in.)	Smear Density (% Theoretical)	Peak Rating (kW/ft)	Burnup (MWD/T)	Max Molten Area (%)	Postirradiation Smear Density (% Theoretical)		
						Top Segment	Middle Segment	Bottom Segment
F1E	14.2	87	15	2,000	0		No change	
B4A	5.1	93	23	16,000	0		No change	
E1A	4.0	76 ^(a)	23	7,000	0		No change	
B2A	2.5	93	25	52	— ^(c)	90	93	95
B6A	2.2	93	28	Nil	5		No change	
C3A	6.0	93	29	9,000	8	89	94	96 ^(d)
B6B	2.2	93	32	Nil	11	91	94	96
C2B	6.0	91	Tran ^(b)	Nil	75	85	90	96 ^(d)
C3B	6.0	93	25 Tran ^(b)	7,900 Nil	75	92	Undetermined 89	95 ^(d)

^(a) Annular pellets (95% theoretical density).

^(b) Tran = Transient irradiated

^(c) Metallographic preparation may have missed coring.

^(d) Some fuel cast into hollow insulator pellet at end of fuel column.

FIG. 6. Gross Gamma Scan of Reference FCR Fuel Irradiated in EBR-II Illustrating Axial Stability of Mixed Oxide.

FIG 7 Upward Transport of Pre irradiated Mixed Oxide Fuel Subsequently Melted during a Transient Test.

shaped core. Theoretical consequences concerning the relative importance of fuel movement in a hypothetical accident are discussed elsewhere;⁽¹⁸⁾ we will stress the empirical observations made to date on the mixed oxide.

Numerous specimens irradiated under a variety of conditions have been examined for axial fuel movement. Gross gamma scans were performed to determine whether the radioactivity profile departs from

the integrated flux curve. Multichannel analyses plus single-channel scans for volatile and nonvolatile nuclides were used to distinguish variations due to fuel redistribution vs. those due to fission product migration. After the fuel pin was sectioned transversely, the fuel length was divided into segments and the density estimated from the void distribution in each segment. Typical results from these examinations are summarized in Table 1.

FIG 8 Solidification Pattern of Mixed Oxide Fuel Following Transient Irradiation

As shown in the tabulation, no evidence has been observed in the FCR program of any significant axial redistribution in elements where the mixed oxide fuel has operated below center melting. Similar conclusions relative to axial fuel movement may be drawn for UO_2 operated under BWR conditions.⁽¹⁹⁾ The axial stability of reference FCR fuel irradiated in a fast flux at 15 kW/ft can further be inferred from Fig. 6, which shows that the activity curve matches the flux profile and is quite smooth. Contrary to some postulations, fragments of fuel have not been observed to fall into the central void when one has formed.

When the linear power is sufficiently high to cause fuel melting, fluid flow becomes a potential source of fuel redistribution. Axial relocation of the mixed oxide has occurred in most pins in which a portion of the fuel was operated in the molten regime. Table 1 summarizes the results in terms of axial density change.

Liquefaction does not necessarily result in slumping, i.e., downward movement. Gravity is not the only force to which molten fuel is subject. The ultimate disposition of the once-molten fuel in the pin is subject to variation depending upon its condition prior to melting and can be affected by at least five phenomena:

- a. volume expansion on melting;
- b. slumping of molten fuel by gravity;
- c. boiling or gas evolution from the liquid;
- d. expulsion of liquid through cracks;
- e. shrinkage on freezing.

Items a, c, and d can lead to expansion of the fuel.

Upward transport of pre-irradiated mixed oxide fuel subsequently melted during a transient is illustrated in Fig. 7. The upward movement is attributed to pressure from the sudden release of internal fission gas and perhaps vaporization of the fuel. This tendency of the fuel to separate axially during a power surge would be helpful, of course, in limiting an accidental excursion. A similar phenomenon has been noted in transient tests of sodium-bonded uranium carbide fuel in which vaporization of the bond generated high local pressures and caused fuel relocation of this nature.⁽²⁰⁾

The downward movement of once-molten fuel follows an irregular pattern. It appears that the central portion of the fuel pin is completely filled with molten fuel at the time the maximum fuel temperature occurs, and that much of the downward movement occurs during solidification; this is evidenced by the conical shrinkage cavity which forms (see Fig. 8), in much the same manner as in a casting poured into a mold without an external reservoir of liquid.

In the current 1000-MWe design⁽²¹⁾ fuel design limits have been selected to preclude axial fuel movement due to melting, i.e., all fuel is kept below

TABLE 2. SUMMARY OF EXPERIMENTAL WORK ON MOVEMENT OF MIXED OXIDE FUEL

1. Differential fuel-cladding thermal expansion can be accommodated under representative FCR conditions.
2. Radial movement occurs, as in UO_2 , but introduces no objectionable fuel-cladding interactions.
3. High-burnup swelling tests in thermal and fast fluxes are in progress.
4. No evidence of significant axial redistribution exists in mixed oxide operated below center melting.
5. Both upward and downward movement of fuel are noted as a result of transient melting.
6. Molten expansion in an overpower condition can be accommodated in short fuel segments provided sufficient void volume is available.

incipient melting in the steady state and no fuel rises above the liquidus during a transient.

IV. Conclusions

The principle conclusions relative to experimental observations on movement of mixed oxide fuel are summarized in Table 2.

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Discussion

Mr. Okrent (ANL): You said that the smear density is about 87% for the oxide. I recall that the Russians, who have had a reasonable amount of experience with oxide, at Detroit said that they would go down to about 75% or 80% in order to get high burnup. So I find an implication that to achieve 100,000 MWd/T, there is some feeling that lower densities than your 87% are needed. Would you care to comment to this point?

Mr. Rabin: I don't have all the details on the Russian work, although I know they have looked at UO₂ and PuO₂ individually. I'm not completely familiar on what basis they have come up with their design; were they extrapolating PuO₂ data or just how did this come about? The reported swelling rates would not indicate that a low density is needed.

Mr. Okrent: Have you achieved 100,000 MWd/T or more with an 87% density fuel?

Mr. Rabin: No.

Mr. Okrent: What I am trying to see is: at 87% have you built in a compromise for safety which you will give up later? In order to get 100,000 MWd/T, will you indeed have to go to 80% density or 75%, as the Russians have suggested?

Mr. Rabin: Well . . .

Mr. Okrent: You mentioned the bright side of how fuel might move in an oxide, mainly first that it doesn't escape the jacket during the transient, at least you haven't been able to make it do this, and sec-

ondly, you think it will move from the center in both directions. But I think one can equally well visualize that fission gas might be primarily released near the bottom, where more is trapped in the fuel since this is a colder region. The molten part of the fuel might be more towards the top. You might have a general motion all in one direction, perhaps upward, but with a net compaction of many pins, adding reactivity. Is it clear that fuel always moves in such a way as to lose reactivity?

Mr. Rabin: No. First, the profile would be symmetrical so it will be presumably as cold at the top as at the bottom.

Mr. Okrent: Which profile? There are various ways of getting to the melting point. You may have an overpower incident or a partial loss of flow.

Mr. Rabin: My remarks were not intended to convey the impression that it will always be favorable. In general, the presumption has been that it will always be unfavorable. The point that I was trying to make was this is not necessarily so. In other words, it has been frequently assumed that compaction will occur. These results show that this is not necessarily so. We are not at this time intending to use this upward movement as a design basis for inclusion in our safety factors.

Mr. Stachura (AI): In capsule C-2-B was there not a cladding failure?

Mr. Rabin: No, that was C-2-C, which ran at higher power than C-2-B.

Mr. Stachura: Did C-2-C produce a penetration of the cladding?

Mr. Rabin: Yes.

Mr. Bump (ANL): You say that you are designing so that during the transient the fuel does not melt. What transients are these at which the fuel does not melt? A second related question is this: General Electric apparently does not use the statistical-type approach in combining the hot channel factors or uncertainty factors. This puts you at a disadvantage for otherwise you could presumably go to higher power without melting.

Mr. Rabin: I would like to refer the answer to this question to Mr. Sherer or Mr. O'Neill.

Mr. Sherer (GE): We do use the statistical approach for those factors when we believe it is proper to do so. We don't believe it is applicable to those factors that are of a systematic nature; unless you have a large number of these uncertainties, we don't believe that you could properly apply a statistical approach. We do apply it to fabrication tolerances for example.

Mr. Bump: What transients will not put the fuel above melting?

Mr. O'Neill (GE): We design for the normal re-

actor operating transients, for example, the overpower, the normal power overshoot, and that sort of transient, to stay below melting, but we do not consider it in our accident transients, of course.

Mr. Kummerer (Karlsruhe): My question is not related directly to the fuel movement but to the cladding behavior in this transient test. Did you ob-

serve any longitudinal strain onto the cladding of the specimens which were pre-irradiated before the transient test?

Mr. Rabin: The particular specimen was not measured. A specimen pre-irradiated under comparable conditions which was examined and not transient tested showed no axial expansion.

Status of Recent Meltdown Studies

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I. Introduction

Analysis of the consequences of a "meltdown-type" incident in a fast reactor requires knowledge of the phenomena governing the movement of core materials—coolant, as well as fuel and structure—resulting from abnormal operating conditions. Information needed includes modes of fuel failure and the conditions which produce the various types of failure, types and velocities of fuel movement, and coolant effects. Pre-failure movements of coolant or fuel may also be involved. Given these data, then, and a hypothetical accident, the next step consists of determining types of movements, the various velocities, degrees of coherence of movement over the core, and phasing these results into kinetics calculations. At this stage, there are various possibilities: the incident may be shown to have terminated; secondary movements may be found to be possible, requiring additional analysis; or shutdown due to internal pressures may have to be calculated. Even in the case of the most widely studied system, EBR-II, meltdown-related data are incomplete. It is the purpose of this paper to review the current status of meltdown studies, emphasizing recent work. Because separate papers relate results obtained by Atomics International and General Electric experimenters, this report will be restricted to the Argonne National Laboratory program.

II. Failure Phase

A. Coherence of Failure Study

At the last October Fast Reactor Conference, we gave a preliminary report⁽¹⁾ on calculations of the coherence in fuel failure for hypothetical accidents in an EBR-II-like reactor, based on the experimental data then available for the Mark-I fuel. Except for a particularly incredible case, degree of coherence was sufficiently large to play a significant role in limiting possible rates of reactivity addition resulting from failure.⁽²⁾ Those calculations can now be shown to have a wider range of validity than could have been demonstrated then.

(1) *Effects of Prior Irradiation.* Although localized,

preliminary cladding failures have been observed, massive failure and gross fuel movement still appear (for burnups in the range of interest of EBR-II Mark I) to be characterized by the same thresholds, delays, and mechanisms as unirradiated samples.⁽³⁾ One later sample run just past the failure threshold, with axially shaped power profile, showed failure at the hottest region and some fuel movement from ends to the center. Expulsion of fuel from the cladding was not complete.

FIG. 1. Comparison of Experimental Linear Expansion of Irradiated Sample with Expansion Calculated Using Data for Unirradiated U-5 w/o Fs Alloy. Sample Heating Rate: 1000°C/sec.

FIG. 2. Comparison of Experimental Linear Expansion of Irradiated Sample with Expansion Calculated Using Data for Unirradiated U-5 w/o Fs Alloy. Sample Heating Rate: 80°C/sec.

FIG. 3. Post experiment Condition of 7 pin Clusters of EBR-II Pins, Run in Inert Gas.

For short transients in the time scale of TREAT excursions, excessive swelling of EBR-II Mark-I fuel alloy appears to occur at temperatures well above the normal operating range.⁽⁴⁾ In Fig. 1 are compared measured expansions of unrestrained irradiated EBR-II Mark-I fuel alloys with those expected on the basis of data for unirradiated material given similar heat treatment. Massive swelling is associated with the melting range of 1000–1080°C, in the middle of which the sample collapsed.⁽⁴⁾ The specimen was heated at the rate of approximately 1000°C/sec.

Because of calibration uncertainties (thermocouples could not be attached directly to the sample; hence temperatures were calculated from reactor transient data, power calibrations, and enthalpy data), a band is shown for the calculated expansion. Figure 2 is a similar comparison for a sample heated at about 80°C/sec. Both samples had burnup of about 1.3 a/o.

(2) *Capabilities of Subassembly Containment.* Some subassembly containment is necessary to prevent excessive propagation of failure. Figure 3 shows four small clusters, opened up after being run in the

absence of sodium coolant, in subassembly mockups of one-mm-thick 304 stainless steel, and with a shaped axial power profile. Three were run to mild failure, and one (second from bottom) to extensive failure. In the last case, failure appears to have occurred in the hottest region, with expulsion of fuel there, and subsequent movement of molten fuel-cladding eutectic to the subassembly bottom, where it solidified without attacking the cladding. This result is quite consistent with out-of-pile penetration studies.⁽⁵⁾

(3) *Effects of Coolant.* Results from recent loop experiments with single pins in the center of a ring of six dummy pins⁽⁶⁾ are consistent with thresholds and failure mechanisms discovered earlier in tests with single pins in dry and stagnant sodium capsules, and used in the calculations of coherence of failure.⁽²⁾

B. Mechanisms of Failure

Competition between cladding failure due to internal pressure and dissolution by molten fuel has been reviewed for the sodium-bonded EBR-II fuel pin.⁽¹⁾ Details of the cladding dissolution must be

FIG. 4. Solidified Fuel-Cladding Eutectic in Surface Crack of EBR-II Fuel Alloy of Specimen Carried Almost to Failure in Integral Sodium Loop.

cladding, following exposure in sodium just short of the failure threshold,⁽⁶⁾ in a TREAT package sodium loop.⁽⁷⁾ A surface crack, filled with uranium-steel eutectic, is clearly visible. This phenomenon of propagation of molten material through a cracked surface shell may also be important for brittle ceramics.

Thermal expansion may also be a source of failure. Figure 5 shows a Nb alloy-clad UC pin which ruptured in a TREAT transient as a result of resistance to axial expansion: the ceramic fuel fragmented and, acting like a hydraulic medium, transmitted the axial loading into a radial loading in the central region of the cladding, producing a pressure failure of the niobium tubing. Because of the nature of the pressure loading, nearly all of the fuel remained inside the cladding after the cladding rupture.

III. Movement Phase

As illustrated in Fig. 3, fuel movement can be a multistage process, in which the initial movement associated with failure is followed by secondary motion(s).

The role of internal pressure in producing fuel movement upon failure has been surveyed over a range of conditions. However, the metallurgical conditions, geometry, and temperature distributions of

FIG. 5. Appearance of Nb Alloy-clad UC Element after Failure Produced by Transient Mechanical Loading of Ceramic Fuel.

specified, however, since actual attack of the cladding may result from high-temperature molten material moving from the interior *via* small cracks in the cooler fuel surface. This is demonstrated in Fig. 4, which shows an EBR-II fuel pin after removal of

the fuel pins at the time of failure must be known if this phenomenon is to be analyzed properly.

As an example, Fig. 6 is an enlargement of a recent metallographic photo of uranium "fibers" in the thorium matrix of an EBR-II-type Th-U fuel pin which

FIG. 6. Fiber Structure of Th U Fuel Pin after TREAT Transient with Maximum Cladding Temperature of 1360°C.

was run in a TREAT transient. Although maximum cladding temperatures were $\sim 200^{\circ}\text{C}$ above the solidus temperature and the uranium phase of the alloy was molten, the material retained the fibrous structure typical of its fabrication by extrusion.⁽⁸⁾ This relative metallurgical stability at high temperatures explains why samples of this fuel type did not display fuel movement resulting from pressure of bond sodium when the steel cladding failed.⁽⁹⁾ Because of the competing phenomena involved, prior irradiation and a sodium coolant may change this behavior significantly.

Effects of temperature distributions are illustrated in Fig. 7, which was used in the analyses of a melt-

down experiment with an EBR-II-type pin run to extensive failure in a TREAT sodium loop.⁽⁶⁾ Calculated cladding bursting pressure—calculated pressure differential required to burst the tubing plus ambient pressure in the loop—is shown as a function of cladding temperature. In this nonisothermal case, “cladding temperature” is taken to be that of the central node point in the cladding region. Also shown is the internal pressure due to argon cover gas inside the cladding and vapor pressure of the sodium bond, displayed as a function of center cladding temperatures, for the transient temperatures calculated up to time of peak power using the ARGUS code.⁽¹⁰⁾ The

FIG. 7 Calculated Internal Pressures and Cladding Bursting Pressures of EBR II Pin in Sodium Tr 855

FIG. 8 Still Frames Showing High temperature Failure of Ta clad UO_2 Sample

analyses led to a prediction of a cladding failure due to internal gas pressure at about 187 sec. Experimentally, a sharp pressure spike of about 3 atm was recorded at the inlet to the test section at 1.86 sec, and the sample postmortem showed extensive failure along the length of the pin, with pressure rather than cladding dissolution as the dominant mode of failure.

By contrast, internal pressures calculated under the assumption that the bond sodium vapor pressure was that of sodium at the cladding temperatures (i.e., no radial temperature gradient) were much lower.

Although fuel movement due to gas pressure inside the cladding is often considered to be a unique characteristic of sodium-bonded metallic fuels, it can be realized for gas-bonded ceramic pins. Figure 8 shows still prints taken from the color motion picture film record of a high-temperature failure of a Ta-clad UO_2 specimen run in an inert gas environment.⁽¹¹⁾ The figure traces the course of the test from the heating of the cladding through failure and expulsion of molten oxide. Meltdown fragment velocities measured from the films ranged from 106 to 290 cm/sec.

The secondary movements are not well-characterized yet, particularly with regard to the effects of coolant. These effects depend, among other things, on the relative timing of failure and coolant expulsion. Thus, it is possible to predict that coolant boiling could cause fuel dispersal, or that, conversely, the coolant could be expelled prior to failure and then rush back in and compact fuel material. There is evidence that secondary movements may not be as important in the case of irradiated metal fuel, because of the large volume change observed upon meltdown of metallic fuel.⁽¹²⁾ Frothing phenomena may also be significant for irradiated ceramic fuel; this point has yet to be established experimentally.

IV. Plans

It is planned to expand the experimental program with metallic uranium fuel to include studies of the effects of clustering fuel pins in a sodium environment, with relatively realistic axial and radial power profiles. Behavior of irradiated specimens in sodium and in clusters is also to be investigated.

A stock of irradiated oxide pins is being accumulated for experiments with inert gas and sodium environments. Effects of sodium bonding and environment on unirradiated oxide pins will also be studied. The program of experiments on carbide specimens will be expanded as more samples become available. Work on multicomponent fuels (like the Th-U matrix) will continue, in pace with their development.

As the behavior of the uranium fuels becomes better characterized, it will become appropriate to phase in study of plutonium fuels. Procurement has been initiated on a range of promising plutonium fuel samples—both ceramic and metal.

V. Acknowledgments

It is a pleasure to acknowledge the contributions of R. Brittan, for helpful discussions and guidance; E.

Sowa, J. Heap, and G. Golden, for development of experimental facilities; J. Boland, TREAT Manager, and his efficient reactor staff.

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Safety and Economic Aspects of Vented Fuel in Sodium-cooled Reactors*

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1. Introduction

One of the major advantages of fast ceramic reactors over other power reactor systems is their potential for achieving low fuel-cycle cost. Fulfillment of this potential requires high fuel performance as well as the good neutron economy inherent in fast reactors. In particular, high burnup and high power density are required in order to minimize the fabrication, recovery, and fuel-inventory components of fuel-cycle costs.

2. Fission-gas Accommodation

The release of considerable fractions of generated fission gas from high-burnup, high-power-density FCR fuel is well-known. Thus, the accommodation of these fission gases is a major design problem for the FCR fuel and system. Various ways for accommodating fission gases in fuel pins are shown in Table 1. The broad objective of all these designs is to relieve the pressure buildup due to fission gas without permitting release of daughter activity or ingress of sodium into the fuel-blanket region. If the gas is retained in the core region, pressure buildup inside the fuel element requires restraining cladding thicknesses which are generally precluded by considerations of both thermal stress and core composition. Alternatively, very low-density fuel, which allows space for fission gas, also leads to a poorer neutron economy.

If the gas is transferred out of the core region, as appears necessary, it may be retained in a plenum chamber integral with the fuel cladding, but remote from the core, or it may be released into the coolant and cover gas of the primary system. Three leading concepts are shown schematically in Figure 1.

The vented-to-plenum concept, basically an extension of fuel cladding into a region above or below the core, appears the most attractive pressure-vessel design. The concept offers the potential for removal of the pressure stress from the cladding as well as a

potential improvement of behavior of fuel-pin failure, especially as regards gas blanketing and autocatalytic failure of adjacent pins.

The restraining clad appears the least likely, due to high thermal stresses and poor neutron economy.

The diving-bell design was picked as the simplest pressure-relieved example, since it does not extend to cover gas nor does it rely on moving parts and special material properties. The vent is shown schematically, in greater detail, in Figure 2. The volume within the bell, and thus the length of the vent, is determined by the criterion that sodium cannot be forced into the fuel region on shutdown.

The potential disadvantages of vented-to-coolant systems might include additional requirements as regards shielding, cover-gas systems, fuel-handling system, and maintenance due to the release of the fission product gases and subsequent buildup of daughter activity.

Most of these disadvantages can be neutralized by retention of the fission gases inside the fuel until the fission gases decay and the fission-gas daughters (Cs, Sr, Ba, and La) are deposited inside the fuel pin. If one examines the fission product chains, we find that all potentially troublesome daughter products can be eliminated by an adequate delay time within the fuel.

3. Vented-to-coolant Test

A preliminary demonstration test of vented-to-coolant fuel has been successfully performed and previously reported.⁽²⁾ This test was designed to identify the fission products released, to measure their release fraction, and to determine the delay time experienced by the gases prior to their escape.

The test fuel rod was comprised of 95% dense (90% overall average including the fuel-cladding gap), 20 w/o PuO₂-80 w/o UO₂ pelleted fuel, 0.25 in. in diameter by 5 in. long with a 2-in.-long UO₂ blanket. The fuel operated at linear powers of 20 to 24 kW/ft in a thermal flux which approached conditions for central melting. Total exposure was 16,500 MWD/T.

* Work performed under Contract AT(04-3)-189, Project Agreement 10, between the U. S. Atomic Energy Commission and General Electric Company.

Results of this experiment are given in Table 2. Release of the long-lived and stable fission gases agreed closely with a calculated release of 40–50% for these operating conditions.

In Table 2 note the dependence of gaseous release fraction on half-life. By plotting these data as in Fig. 3, with the release fraction normalized to that of stable gases plotted against half-life on semilogarithmic paper, the slope gives the hold-up time. The hold-up time in this test turned out to be near 5 days. This explains the lack of daughter activity that escaped into the simulated coolant and cover gas.

The only fission-gas daughter released in significant quantity was Cs-133, the daughter of 5.3-day Xe-133. It turned out that neutron activation of Cs-133 to Cs-134 produced the majority of activity present outside the fuel, excluding that due to Na-24. A summary of this test is given below:

1. Some 44% of the noble fission gas was released, consistent with the 40–50% predicted by calculation.

2. The effective gas-delay time was estimated to be about 5 days based on isotopic analysis of krypton and xenon released.

3. The predominant activity in the sodium, after the decay of Na-24, was Cs-134, an activation product of Cs-133, which in turn is a daughter of Xe-133.

4. The release fractions of all other nuclides (including fissile) (Sr-89, Sr-90, Y-91, Zr-95, Ru-103, I-131, Cs-137, Ba-140, and Ce-144) were at or near the detection limit in the range from 10^{-6} to 10^{-10} .

5. No deleterious effects to the fuel were observed due to possible exposure to sodium vapor. Traces of sodium were observed at the top of the "blanket," which may have been responsible for retention of radio-iodine in this test.

Thus, the fissile elements and the nongaseous fission products and daughter elements do not distill out of the fuel (and blanket), and the gaseous fission products are sufficiently delayed in their release so that Cs-134 seems the major concern to vented-to-coolant system design.

We are continuing this work to back up these conclusions and to date have found nothing to refute them.

TABLE 1. FISSION-GAS ACCOMMODATION

Pressurized Pins:
In-core, thick clad
In-core, low density
Vented-to-plenum
Pressure-relieved Pins:
Capillary tube to cover gas
Check valve
Semipermeable porous plug
Diving bell

4. Implications to FCR Design

A technical and economic analysis⁽¹⁾ has been carried out comparing a large reactor plant using vented-to-coolant fuel with one having vented-to-plenum fuel. As a basis for comparison, a 1000-MWe FCR design with a submerged tank-type primary system was utilized and the test results described above extrapolated. This system is described in another paper.³ Figure 4 is a schematic view of this type of system. The cover-gas system, shielding, fuel-handling, maintenance and decontamination, pumping power, reactor vessel, reactor building, fuel costs, and safety features were considered. Cost incentives or penalties for vented-to-coolant as compared to vented-to-plenum fuel were identified and their magnitudes were estimated. For purposes of the comparison, one percent failed fuel was assumed present in either case, a basis which presumably favors the vented-to-plenum case in view of its greater failure potential due to the pressure stress.

Before such estimates were possible, questions such as (a) the effects of Cs-134 activity, (b) the cover-gas activity, (c) the gaseous decay tank system design, etc., were analyzed. Examples of the type of analysis performed are given below.

The vented-to-coolant cover-gas activity is illustrated in Table 3 as a function of fuel-pin holdup time. Note the total activity in the system at 5 days, the basis of this analysis.

The Cs-134 activity in the system, assuming 100% of the released Cs-133 found its way into the sodium coolant, is shown in Fig. 5. Since this activity is within an order of magnitude of the unavoidable Na-24 activity, the problem should be a second-order one. Note also the effect of one percent fuel failures. The Cs-137 activity from one percent failures is near the Na-24 level, i.e., the plant must be designed for about this level of activity in any event. Thus, no plant design changes were chargeable to vented-to-coolant in this regard.

a. Economic Analyses

Without giving the details of each analysis, let us skip to the results of the economic analysis. The reader is referred to GEAP-4770⁽¹⁾ for these details. As a result of these analyses, it appears that where changes in capital costs can be identified, they are small, and no significant net penalty or gain is apparent (see Table 4). It does seem that there is a small net gain, however. Accordingly, this capital cost analysis provides no basis for choice between the designs. Likewise cost differences resulting from fuel handling, shipping, pumping power—right down the list—were also found to be relatively small.

FIG. 1. Schematic of Fission-gas Accommodation Concepts.

Should vented-to-coolant fuel result in a gain in burnup, the cost advantage is about 0.01 mill/kW-hr per 10,000 MWD/T difference. It turns out that the significant differences lie in the area of safety.

b. Safety Considerations

Consider further the question of safety. When the cladding need not perform as a pressure vessel, less demands need be placed on the fuel-cladding properties, which are subject to possible deterioration over the life of the fuel in the fast flux and sodium environment. Therefore, the vented-to-coolant fuel may be expected to be less subject to fuel-pin failures due to this cause, although a quantitative prediction of the improvement is not possible.

Secondly, the probability of failure propagation should be reduced by a low internal pressure. The possibility that fission gas under high pressure could be injected into the coolant stream through a minor defect, causing vapor blanketing of adjacent pins and possibly choking off of coolant flow is effectively eliminated. An elementary analysis of this situation showed that other fuel pins, surrounding a vented-to-

plenum pin which failed, would probably be gas blanketed long enough for cladding failure.

Although some preventative fuel-pin designs come to mind, such that an impedance is placed between the gas plenum and fuel, there will always be a larger gas inventory available for injection into the coolant from vented-to-plenum designs. In this sense the vented-to-coolant design is again superior.

Even if vented-to-coolant designs do not decrease the incidence of failures, i.e., failures turn out to be a function of something other than pressure, a decrease in damage potential is apparent from a failure for any reason. The reduced pressure of the contained fission gas could make many failures less dramatic. For example, solid or molten fuel is less subject to expulsion through the break-threatening blockage of a fuel channel or sodium voiding.

Additionally, some second-order safety characteristics are apparent during fuel handling and shipping. Handling of the unpressurized, irradiated fuel is obviously less hazardous than handling of pressurized ones. Canning during shipment of vented-to-

coolant seems straightforward and probably required, in any event, for defective fuel.

A possible disadvantage of vented fuel, which was considered, was the concern that fission-gas bubbles might increase the tendency for sodium voiding in the core. Sodium velocities in the primary tank were found low enough to allow adequate de-entrainment of the gas. The gas remaining in the coolant was calculated to be $<0.001\%$ of the total produced at 100,000 MWD/T, which should be sufficient to guarantee that bubbles do not coagulate and clog a fuel channel. These calculations indicated that a bubble large enough to travel >0.5 ft/sec vertically will be released in the low-velocity, baffled region provided in the sodium tank. This corresponds to a bubble size of about 0.02 in., whereas the spacing between fuel pins is several times this value. Thus, even if a bubble did not escape, it would probably pass through the core unnoticed, and could even serve to eliminate superheat by providing nucleation sites on a continuous basis.

c. Detection System for Failed Fuel

In reviewing the possible systems for detection of fuel failure in a sodium-cooled reactor with vented-

TABLE 2. FISSION PRODUCTS RELEASED TO COOLANT OR COVER GAS

Nuclide	Half-life	Release Fraction
Gases:		
Kr-85	10.6 yr	0.44
Xe-131m	12 d	0.30
Xe-133	5.3 d	0.27
Xe-133m	2.3 d	0.05
Xe-135	9.2 h	0.0003
Solids:		
Ru-103	40 d	4×10^{-10}
I-131	8 d	1×10^{-8}
Cs-137	29 yr	1×10^{-6}
Other (Sr, Y, Zr, Ba, Ce)		10^{-6} to 10^{-8}

FIG. 3. Fuel-pin Holdup Time.

FIG. 2. "Diving Bell" Vent Device.

to-coolant fuel, the two main objectives of leak detection and leak location must be kept in mind. Several methods have been suggested, and it is not our purpose here to review the candidates. However, all rely on sampling of either the reactor coolant or cover gas, and then analyzing the sample for material or activity which would indicate the presence of defective fuel.

Most available methods are applicable to the detection of failed fuel. Discrimination between previously

FIG. 4. Tank and Cover-gas System.

released activity and incremental fresh activity will be a problem which must be solved in either vented-to-coolant or vented-to-plenum fuel systems because leakers probably will always be present in the core. Discrimination between aged (vented) fission gases and fresh (failed) fission gases seems possible in principle, and delayed-neutron detectors should also continue to work. The delayed-neutron emitters present in a vented fuel system will signal fuel failure just as in a nonvented fuel system because the short-lived halides do not reach the coolant in either design except through defects.

Also, experimental evidence indicates that the heavy elements do not leave vented fuel except through defects, so no interference with activity monitors or with operation and maintenance is inferred. In fact, the frequency of fuel-pin failures should be decreased by application of vented fuel so the amount of both delayed-neutron emitters and fissile material in the coolant should be decreased.

Location of failed fuel, if it can be done, is a much more difficult problem. If the origin of the activity is

to be sufficiently localized, it probably requires sodium sampling from each subassembly, regardless of fuel design. The difference between vented-to-coolant and vented-to-plenum fuel would be only that entrained fission gases could not be used as indicators of failure for the vented-to-coolant fuel.

5. Conclusions

In summary, the vented-to-coolant fuel has been shown to be a primary contender as a ceramic fuel for a fast reactor. In some respects the vented design is superior to nonvented designs.

In the area of economics, vented-to-coolant designs show no significant increase in costs and possibly a small net reduction.

The value of improved propagative-failure behavior of vented fuel pins is difficult to assess, but appears beneficial and important. However, the long-term capability for withstanding the sodium and sodium vapor environment remains to be demonstrated.

Finally, an important advantage is that the vented-

TABLE 3. EQUILIBRIUM COVER-GAS ACTIVITY (Ci) FOR VARIOUS HOLD-UP TIMES* FOR VENTED-TO-COOLANT FUEL

ISOTOPE	t 1/2	0	10 m	1 h	5 h	15 h	1 d	5 d	20 d	60 d
Kr-89	3.2 m	3.54×10^7	4.06×10^6	3.54×10^1						
Xe-137	3.8 m	4.52×10^7	7.18×10^6	6.8×10^2						
Xe-135m	15 m	1.38×10^7	8.84×10^6	8.6×10^2	1.38×10^{-1}					
Xe-138	17 m	4.20×10^7	2.82×10^7	3.77×10^6	2.1×10^2					
Kr-87	1.3 h	2.08×10^7	1.91×10^7	1.22×10^7	1.04×10^6	7.3×10^3	5.72×10^1			
Kr-83m	1.86 h	3.70×10^6	3.45×10^6	2.56×10^6	5.55×10^4	1.45×10^4	1.37×10^2			
Kr-88	2.8 h	2.84×10^7	2.73×10^7	2.22×10^7	8.5×10^6	7.1×10^5	7.4×10^4			
Kr-85m	4.4 m	1.15×10^7	1.12×10^7	9.8×10^6	5.2×10^6	1.06×10^6	2.63×10^5			
Xe-135	9.2 h	4.90×10^7	4.75×10^7	4.52×10^7	3.34×10^7	1.45×10^7	8.04×10^6	5.23×10^3		
Xe-133m	2.3 d	1.30×10^6	1.30×10^6	1.28×10^6	1.22×10^6	1.08×10^6	9.62×10^5	2.86×10^5	3.25×10^3	
Xe-133	5.3 d	5.32×10^7	5.32×10^7	5.32×10^7	5.15×10^7	4.9×10^7	4.66×10^7	2.76×10^7	3.72×10^6	2.12×10^4
Xe-131m	12 d	1.54×10^5	1.54×10^5	1.54×10^5	1.54×10^5	1.48×10^5	1.45×10^5	1.15×10^5	5.4×10^4	9.8×10^3
Kr-85	10.6 y	7.5×10^2	7.5×10^2	7.5×10^2	7.5×10^2	7.5×10^2	7.5×10^2	7.5×10^2	7.5×10^2	7.5×10^2
TOTAL		2.76×10^8	2.11×10^8	1.51×10^8	1.01×10^8	6.67×10^7	5.6×10^7	2.8×10^7	3.79×10^6	2.67×10^4

*ASSUME 35% RELEASE FRACTION FROM THE FUEL

FIG. 5. Activity in the Coolant System.

TABLE 4. TECHNICAL AND ECONOMIC COMPARISON

Basis:

1000-MWe, FCR, tank-type reactor, demonstration test results.

Conclusion:

Slight capital cost advantage to vented-to-coolant fuel.
 Slight handling and shipping advantage to vented-to-plenum fuel.
 Slight fuel-performance advantage to vented-to-coolant fuel.
 Slight pumping-power advantage to vented-to-coolant fuel.
 Slight decontamination and maintenance advantage to vented-to-plenum fuel.

to-coolant concept places less pressure-stress resistance on cladding properties in the fast flux and sodium environment.

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Discussion

Mr. Campise (AI): I think there is one obvious economic incentive to vented elements that has been overlooked thus far. It is not at all obvious that the plenum design is going to make the grade as far as being able to provide us reliable 100,000-MWd/T performance, whereas the vented concept just may increase the probability that if one looks at fuel-cycle costs you will find maybe 0.3–0.5 mill/kWh, and this indeed is an economical incentive.

Mr. O'Neill: Yes, we looked at the fuel costs and fuel-performance advantages, and I certainly agree with you that there is one. We find, however, that it is more like 0.01 mill/kWh per 10,000 MWd per ton difference. I agree with you that this is a potential benefit of venting to coolant, and I strongly believe that the vented-to-coolant design will have less failure potential than the vented-to-plenum design. Of course, this is part of the reason why we have selected it for the design which Dr. Cohen discussed yesterday.

Mr. Hall (LASL): What is the fate of the cesium once it gets into the sodium?

Mr. O'Neill: The best I have seen are the papers on BR-5 by the Russians. We don't really have good data on the fate of the fission products after they get into the coolant, although we are addressing ourselves to that problem. The Russians seem to report a deposition in the vapor phase, which was out of proportion with the amount of sodium there is, as if there was some sort of preferential deposition of cesium, which we find very interesting.

Mr. Kummerer (Karlsruhe): Under normal operation conditions, I find this proposal a very attractive one. But for transient or overpower condition the delay time for the fission gases in this fuel goes down to about zero and, as we can pick up from your table, the activity of the material is raised by some orders of magnitude. What is your comment?

Mr. O'Neill: I suspect you mean normal transients. The particular test data that I showed were the result of some 20 startups and shutdowns, including startup and shutdown transients for example. We did not find any effect due to this although we were looking for one. For example, when we took the capsule out of the reactor we rushed it to the hot cell and got immediate data to look for short-lived fission products; we did not find any. I don't think there will be enough transients in the steady-state operation of the reactor to cause a normal steady-state problem; I should hope not.

Mr. Kummerer: Excuse me, I must correct myself. I did not mean a normal transient; I meant mainly an overpowered condition coming out from a little accident or so. Then you have to some extent a swift release of all the fission gases or most of the fission gases to the sodium. The sodium activity may increase by a factor of 10 or 100, and this is a problem as regards the safety record of this reactor.

Mr. O'Neill: I don't really see what that has to do with the difference between vented-to-coolant and any other kind of fuel.

Mr. Kunkel (AI): With regard to the question on cesium that was asked, we have some data that indicate that the cesium is preferentially deposited in the cold trap. We will talk more about this tomorrow.

Mr. Runnalls (AECL): I understand from Mr. Comprelli's presentation that there is expected to be a fuel-clad interaction which might be as thick as about 3 mils, and there is also likely to be a ferritic layer produced on the outside of the reference 316 SS, which is perhaps 3 mils thick. In addition, if I remember the numbers correctly, there is some evidence of intergranular sodium penetration to about 7 mils. Now, Dr. Cohen yesterday indicated that the reference design is 10-mil 316 SS. How do you hope to avoid venting the fission gas through the sheet?

Mr. O'Neill: Dr. Zebroski, would you like to answer that?

Mr. Zebroski (GE): I think Mr. Comprelli mentioned that these phenomena on both the inside and outside of the cladding are statistically very rare; you don't see them on every pin, and you see them only in occasional spots in the pins where they are found. I think the assumption that you simultaneously have maximum penetration on the outer surface and the maximum penetration on the inner surface facing each other is one of the cases where we will use the statistical approach and say that's rare enough to neglect.

Mr. O'Neill: Let me tell you now how we would design today; it is the way that Dr. Cohen described it yesterday. We assume there is a 100% safety factor in the mass-transfer data which we have today, including the ferrite layer, and it came to, I believe, something around 3.5 mils. We then decided what the minimum end-of-life cladding thickness should be, selected 7 mils, but made the cladding 10 mils thick. I might point out that it is not particularly significant to the vented-fuel cause to have 10 mils of cladding as compared to 15. It's again one of those insignificant economic gains. It is beneficial, however.

Mr. Okrent (ANL): Is it clear that one can design to vent and at the same time not get sodium back in or not flood under the various conditions of starting up

reactors and having temperatures somewhat different from what one designed for, etc.? Are you very optimistic that this is a solved problem?

Mr. O'Neill: Actually, we have only the single test, which was very successful; we are very proud of it. We are continuing the work.

Mr. Okrent: It used to appear that the oxide is likely to achieve high burnup by releasing its fission product gases, thereby not swelling like metals and carbides. It was on this basis that it gained the reputation as a 100,000-MWd/T fuel. Now, however, there seems to be some question as to whether the released fission product gases can be held within a fuel element that goes to high burnup. Secondly, one is quite concerned about the sudden release of these fission product gases and their possible adverse effects on safety. And, hence, serious looks are being given to a new design concept whereby the fission product gases get into the sodium. Am I correct? Is there a strong reservation concerning the overall desirability and likelihood of success for the vented-to-plenum oxide concept at burnups of 100,000 MWd/T or more?

Mr. O'Neill: I don't mean to degrade the vented-to-plenum concept by advocating vented-to-coolant. I certainly believe that we can design vented-to-plenum fuel pins that can make it to 100,000 MWd/T. We have no evidence to indicate that we can't. However, we have precious little evidence to indicate that we can, too.

Mr. Okrent: I believe you said you're concerned about the escape of the released fission product gases, and I think the conference has been concerned about the consequences of boiling which might follow this escape.

Mr. O'Neill: I also indicated that we had several ideas of how we might put impedance between the plenum and the failure and help the problem along for the vented-to-plenum design, as well. We haven't set to work on that problem, if it is a problem. We're just in the process of analyzing it, and we have quite an improvement in our technique to make a right comparison. If it is a problem I am sure we will come up with a design of each type that can handle the problem.

Mr. Kelman (ANL): Can you tell me how much allowance you make in your design for the swelling of the fuel due to retained fission gases and solid fission products?

Mr. O'Neill: We do not make a direct allowance for swelling in our fuel design at this time. We designed the fuel in the way I described as I started the paper, namely, by combination of the fabrication tolerances at the hot spot with the overpower. In the cooler region of the core, where the swelling is likely

to be the biggest problem, there will be a gap and porosity existing in the fuel, which should accommodate quite a bit of swelling. There are no data existing on the swelling of mixed oxide fuel pins that you can use to set down a criterion that would be based on swelling alone. We will be ready and willing to change our criteria when such data come along.

Mr. Morrison (Battelle-Columbus): Concerning the vented-to-coolant concept, it seems to me that the principle of operation involves sorption of the fission products on the surface, either of your capillary or your diving bell. I wonder if you could envision at high burnup the possibility of saturating the selective surface sites for sorption and cutting down your delay time for the release.

Mr. O'Neill: Yes. Not only that, the cesium deposits in the blanket on the way to the venting devices, I'm sure. In fact, we analyzed this experiment destructively and found that the delay time had occurred for the most part before it ever got out of the fuel and blanket region. But to get to your question: does it cause a change or decrease in the delay time? I really doubt that, because it's a diffusion phenomenon for the most part.

Mr. Morrison: It seems to me it's a diffusion phenomenon to get it there, but it's a sorption phenomenon once it stays there. If you may envision a saturation of the surface sites or if you get enough fission products plated out there, you might get an increase in temperature in this region and get a desorption.

Mr. O'Neill: Yes, I guess I should point out that a complementary part of this vented-to-fuel development program is a comprehensive study of the potential for fission product deposition (we call it plugging) in the blanket region. We have seen some slight evidence that it occurs. We haven't seen any evidence that it is a real serious problem.

Mr. Zebroski: May I add to your answer on the swelling. As I mentioned in the paper this morning, we have observed swelling in the colder regions of the fuel, but it's very difficult to distinguish this from the redistribution of the porosity because it is of the same order of magnitude as the fabrication tolerances that you can ordinarily achieve. We see porosity whose total volume is of the same order as the 1-mil gap on the outside, so the reason that we have to give an indeterminate answer is simply that this is a very hard quantity to measure quantitatively. We do have fairly strong indications that it is small. We have in progress a series of four experiments, effectively dilatometer experiments, which are specifically designed for this purpose. The fuel has no place to go; it starts out at very high density with extremely high restraint, and we expect to be able to measure con-

tinuously, that is, nondestructively, the expansion of the fuel up to about 150,000 MWd/T at about four different temperatures. We will get this low-temperature effect and hopefully find a threshold temperature

at which gas release reduces the swelling rate. The old Bettis data which give swelling rates for thin wafer fuel at fairly low temperature, I think, give an upper limit to the probable effect that we would design for.

General Discussion

Mr. Lawroski (ANL): I would like to thank the speakers for their excellent presentations and the audience for the stimulating comments and questions that they have provided. The meeting is now open for general questions.

Mr. Stachura (AI): There was one comment which Dr. Dickerman made which I thought I agreed with at first, but on reflection, I find I disagree. This was the comment about the fact that while fuel could melt into sodium and become dispersed, in similar fashion a channel could void and the sodium could fall into it and compact the fuel. At first I thought this might fall into the category where you do have a voiding-first condition. In the case of ceramic fuel, however, suppose the coolant did come back down into contact with, let's say, molten fuel. Since it hasn't solidified into cooler portions and is mobile, the vapor pressures would be much larger than any hydrodynamic pressures of simple sodium coming back into the core by gravity. I think in that case we would still have an effective dispersion effect, although as I mentioned earlier a previously voided channel requires an experiment where we look at meltdown into a voided region.

Mr. Lawroski: I would like to ask now for someone to comment on the question: what are some of the modes of failures that have been reported, at least in part, of oxide fuels that have been planned to go to rather high irradiations, but failed? Obviously some of the comments that we have had in connection with vented fuels here raise this question as well.

Mr. Leitz (GE): Perhaps you have reference to some of the failures that we have experienced in capsule tests which have been conducted since the first series of 40 successful tests. We have had a few failures with these irradiations. One, I believe, was associated with an overambitious rod power that we exposed a pin to, prior to planned transient irradiation. As a result of exposures, I think in excess of 30 kW/ft, and end flux peaking, we lost the end of the pin. The other failures have been attributable to the difficulties associated with sodium bonding fuel capsules for these irradiation tests; the development of gas bubbles in the bonds, not associated particularly with fission gases, but introduced in the course of fabrication and unfortunately not removed thereafter, led to cladding burnout. I believe the French had some similar un-

fortunate experience and possibly others who have tried high-performance fuel tests with sodium bonds.

Mr. Lawroski: Have you had failures that you can't account for in this way, failures that have occurred prior to when you would have expected it, based on the early irradiations that went to 100,000 MWd?

Mr. Leitz: I don't recall any.

Mr. Dickerman (ANL): I would like to point out that if the coolant rushes back in, and comes into contact with high-temperature fuel, the pressures will be generated at that location; presumably the majority of the fuel will be nearer the center of the core, so you do have a mechanism whereby pressure can be generated in a place that will drive the fuel, for example, towards the center of the core. I believe this is the sort of thing which has been used to describe one of the GE irradiations in TREAT.

Mr. Leitz: For one of the irradiations in TREAT in which there was a failure, reconstruction suggests that the pressures which were generated in the coolant tended to force the material back into the region inside the cladding.

Mr. Rabin (GE): The aluminum heat sink which was around the pin filled the central core such that when we examined it, we found that the aluminum heat sink had solidified inside the central core and was combined with some of the NaK that was in the annulus around the pin; evidently the differential pressure pushed it in. This was a non-preirradiated transient specimen.

Mr. Stachura: I believe that the GE capsule was pressurized on the outside to about 10 atm. I think the failure that did occur was that the oxide did penetrate the clad, but apparently the dispersion that we noticed was not observed. Perhaps the energy was insufficient, so that the contact of fuel with the cladding was enough to bring the temperatures closer to failure temperatures, yet not get the pressures much above 10 atm. However, I will certainly agree that the motion of molten fuels into regions other than the peak flux regions could produce pressures that may drive it again randomly. We need to look at some experiments in which there is a longer range of travel and resolidification of the fuel in the core.

Mr. Boland (ANL): I would like to urge caution in interpreting the single-pin experiments in TREAT too literally. We have yet to run clusters of fuel ele-

ments and look to see what happens. Do we get the significant sodium pressures that we think might be possible due to superheating, with an appreciable mass loading of the subassembly caused by the effect of accelerating what would normally be a sodium head on any power reactor system? I think these single-pin experiments, although interesting, might lead us down the wrong path if we put too much faith in them.

Mr. Kummerer (Karlsruhe): It is clear that if we consider the maximum hypothetical or credible accident, then for both the vented and nonvented fuel concepts, the accident is the same. If the reactor explodes there is no difference. But there are different stages of safety considerations. If we have only some overpower with a nonvented fuel, then we have no accident; we have only an irregularity. But if we have a vented fuel then we have an accident, because the activity of the sodium goes up by a factor of 100 or so, and we need to shut down the reactor to clean the sodium, etc.

Mr. Campise (AI): I think one point that has to be considered is that when we finally get around to engineering a sodium-cooled fast reactor which is designed for vented elements, more than likely there will be auxiliary systems to scrub the sodium as well as to handle the normal venting of fission gases. We have demonstrated in the past that the normal traps in a primary system will trap solid fission products, and the gaseous fission products will be handled by the auxiliary system. It is highly probable that in an overpower situation most of this material will be trapped in the system employed in the design of the vented element reactor.

Mr. Lawroski: How much evidence do you have of this, other than empirical?

Mr. Campise: As I recall, we dumped something like 10,000 Ci of fission products into the SRE, which were predominantly removed in cold traps.

Mr. Kunkel (AI): Maybe I could shed a little light on this. We did find that during the accident at the SRE, when there was a considerable quantity of fission product activity dumped into the sodium, that the level of the activity decreased markedly over what one would anticipate due to the normal radioactive decay, and that the majority of this material was deposited in the cold trap in the piping and in other areas of the system.

Mr. Lawroski: This was of the metallic fuel, was it not?

Mr. Cohen (GE): I would like to say that those of us who are working with oxide fuel are not satisfied with the amount of evidence which we have in support of these fuels. I don't mean that we are having bad results. What's happening now is that we are getting

results few and far between. If a conclusion of this discussion is that we should redouble our efforts to have in-fast-reactor-flux irradiation of oxide fuels and perhaps a bundle, which would not have the misleading results that you get from sodium bonding in EBR-II, for example, I think we would be a lot further along in satisfying the chairman and other people that we have enough evidence.

Mr. Zebroski (GE): I would like to try to also give a speculative comment on Dr. Kummerer's question of the gas release in a transient. I think you have to understand that the instantaneous inventory in the fuel is relatively small, and that in addition to the total rise and absolute temperature of the fuel volume plus the delay time there still is some plenum. The total rise in absolute temperature is less than a factor of two. Thus, on the average, the instantaneous dump-out in the limited class of transients which you must be talking about (that is, the ones which are big enough to be considered an incident, and yet small enough so that you don't damage the fuel), doesn't lead to melting or damaging of the fuel. If you consider this limited class of accidents, you wouldn't be dumping kilocuries into the coolant, only smaller quantities and probably of the same order as the steady-state quantity would be involved.

Mr. Wensch (AEC Washington): It was quite a few years ago that we initiated the work on the vented-fuel-element program. I think I would like to state the objectives of that program so we can see if we have fulfilled the mission. The first objective was to determine the effect of leakers and know what the effect would be in terms of short-range and long-range consequences. If the results indicated that the consequences were tolerable, then we would go to vented fuel elements. It seems to me that great caution must be exercised in considering vented fuel elements for any fast reactor in the future in order to assure ourselves that we can live with any long-term damage which could result to the primary system. I see no evidence that a program has been laid out to the Commission or to this audience which showed that these end objectives will be reached at the time of, say, the planned prototype for either the Germans or ourselves.

Mr. Lawroski: We must bear in mind that the Russians and BR-5 were able to deal with a large amount of what amounts to vented fuel and clean up the reactor and enable themselves to reload it with another fuel.

Mr. Cohen: I would like to repeat to Dr. Wensch the assurance that I gave him earlier that he will see that program.

Session VI-B
October 13, 1965

Mostly Reactivity Coefficients

Chairman: F. W. THALGOTT
Argonne National Laboratory

Secretary: R. A. Lewis
Argonne National Laboratory

Doppler Coefficient Measurements in Zebra Core 5

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(PRESENTED BY R. C. WHEELER)

Introduction

In order to measure the Doppler coefficient associated with a zone of fissile material, the ZEBRA reactor was modified in October 1964 so as to accommodate a central hot loop. A series of measurements was completed during the following ten months with various assemblies of fissile and nonfissile pins within the loop, with neutron spectra approximating to that expected in a typical power fast reactor. The core (Core 5 in the Zebra series) was arranged in two regions, an outer concentrated region of U^{235} and graphite 'driving' an inner region of more dilute mixed plutonium and natural uranium oxide. The inner region included the test zone (inside the loop) and a cold buffer zone, whose composition matched that of the test zone in the case of the four main assemblies of mixed oxides. In these assemblies it was found that the measured Doppler coefficients were slightly more negative than was predicted by the calculations. Other measurements indicated an apparent small negative coefficient for plutonium (i.e., in the absence of uranium) and a small negative coefficient for steel.

The loop had the following advantages over Doppler experiments with small fuel samples:

- (a) the amount of fuel was large enough to give significant reactivity changes;
- (b) the heated zone was nearly large enough to establish its own neutron spectrum (i.e., even without the matched buffer zone);
- (c) it was possible to include correct reactor constituents including sodium;
- (d) the effect of the zone boundary was comparatively small.

Details of the Loop

The main features of the loop are shown in Fig. 1. Inside the reactor, the loop consisted of an insulating structure made up of four square concentric steel tubes, which occupied the central 26 by 26-cm hole in the core lattice structure, a hole normally filled by 25 fuel sheaths. Inside this structure a 16 by 16 array of pins, supported on a pin base plate symmetrically about the core centre-plane, could be accommodated on a square

pitch of 1.13 cm (see Fig. 2). The temperature of the pins was controlled by circulating nitrogen gas through the gaps between the pins. In addition, an electrical heating tape of 6-kW capacity was wound round the innermost steel wall and was operated in such a manner as to achieve an approximately constant radial temperature distribution in the pin zone. The space between the first and third steel walls of the insulating structure, and between the third and fourth steel walls, was filled with Santocell insulant (mainly low-density SiO_2). In the space between the second and third steel walls, a flow of air at the ambient temperature of the buffer zone was blown upwards by means of a second circulator in the so-called 'guard-ring' circuit. The two insulating walls and the flow of air in the guard-ring circuit together restricted the heat flow from the inner test region to the outer core to a maximum of about 25 W.

The walls of the inner loop were supported by means of four trunnions which connected to the outer steel wall at the core centre-plane. Because the temperature variations of the outer wall were small (e.g., less than $1^\circ C$ when the loop temperatures were cycled), the vertical movements of the trunnions due to thermal expansions were negligible. This particular method of supporting the loop was chosen to minimise the reactivity effects of thermal expansion and to simplify their calculations (see below). The loop structure was shown to expand as expected by a series of tests using Invar push-rods and external dial gauges.

Outside the biological shielding of the reactor were situated the heaters, cooler, and circulators associated with the three circuits of the experiment. The main circuit heater (40 kW) and cooler (a nitrogen-to-containment-air heat exchanger of about the same capacity) were operated by an automatic control system such that any desired pin zone temperature in the range from 100 to $550^\circ C$ could be reached and maintained. The temperatures in the loop were monitored by resistance thermometers situated in the gas stream, and also at six positions inside three temperature probes which occupied positions in the assembly of pins. A temperature change over the complete range could be accomplished in about 2 hr. Another, smaller circulator and

heater were associated with the guard-ring circuit; a third blower was associated with heat-savenger circuits which were arranged to remove heat escaping from the lagged pipes of the main circuit.

A small proportion of the main circuit was bypassed through a filter paper in a subsidiary loop, and an α -scintillation counter was fixed close to the filter so that an automatic warning of the release of PuO_2 particles (i.e., in the event of fuel-pin failure) would have been received in the control room. Other alarms were associated with high-temperature sensors in the test zone (and in the outer zones), with low gas flows in the three circuits, and with sodium leakage. Two detectors were used for observing leakage of sodium. One, at the lowest point of the main circuit, measured the resistance of a gas gap that would have been submerged in sodium in the event of any considerable leak. The other, situated further downstream, measured the attenuation of a beam from a sodium vapour lamp in passing across the pipe of the main circuit.

The Pins Used in the Loop

Three types of fuel pins and four types of nonfissile pins were used in the various arrangements in the test zone (see Table 1). The fuel pins (Fig. 3) were canned in 0.25-mm-thick stainless steel tubes, of 11-mm outside diameter. The top and bottom end pieces were

FIG. 1. Zebra Doppler Experiment.

FIG. 2. Sectional Plan View through Doppler Loop.

TABLE 1. NUMBERS OF PINS LOADED IN TEST ZONE

Assembly	A	B	D	Fe	Void	Na	Al	Temperature Probes	Buffer Zone Composition
A	128	—	—	128	—	—	—	—	Matched A
B	—	128	—	125	—	—	—	3	"
C	—	128	—	—	125	—	—	3	Matched D
D	128	—	—	—	125	—	—	3	"
E	—	—	—	128	125	—	—	3	"
F	—	—	—	—	—	—	—	3	"
G	—	—	—	—	—	—	—	3	Matched H
H	128	—	—	—	—	125	—	3	"
I	—	—	—	—	—	—	—	3	Matched J
J	96	—	—	32	—	125	—	3	"
J(1)	54	—	—	18	56	125	—	3	"
J(2)	24	—	—	8	96	125	—	3	"
J(3)	6	—	—	2	120	125	—	3	"
J(4)	—	—	—	—	128	125	—	3	"
K	—	—	16	112	—	125	—	3	"
L	—	—	—	253	—	—	—	3	"
M	—	—	—	128	125	—	—	3	"
N	125	—	—	128	—	—	—	3	"
O	—	—	—	—	—	—	253	3	"

welded to the tubes, both edge and seam welds being used at each end. The fuel was in the form of sintered pellets of 10-mm diameter and 6- or 10-mm height. Above each fuel column (69 cm high) was a nimonic spring which ensured contact between the faces of the pellets during thermal cycling. Each fuel element was thermally cycled after manufacture and then tested for leak tightness before use in the loop. The three fuel types were as follows:

Type A pins each containing 555 g of 16% PuO_2 and 84% depleted UO_2 (mixture by weight);

Type B pins each containing 427 g with the same PuO_2 content as in type A, the balance made up of boron steel and steel;

Type D pins each containing 505 g PuO_2 .

The 'A' pins were specified to represent the composition of a typical power fast reactor fuel (but not with the typical diameter of a power-producing fuel pin). The 'B' and 'D' pins were designed to help separate the contribution of plutonium to the Doppler effect. The 'D' pins were filled, like the 'A' pins, with a column of homogeneous pellets. However, the 'B' pins were loaded with three types of pellets in a regular sequence, as follows:

first pellet: 100% PuO_2 ; 3.2-g mass and 6-mm height;
second pellet: boron steel; 8.7-g mass and 14-mm height;

third pellet: steel; 3.7-g mass and 5-mm height;

The total length was 25 mm. Each 'B' pin contained 27 such cells of 3 pellets.

The steel was designed to achieve approximately the

same total scattering cross section per plutonium atom as in the 'A' pins, and the boron content (see Table 2) was chosen so as to match the neutron spectra of the two assemblies with 'B' pins to the corresponding assemblies with 'A' pins. Calculations show that the spectra were in fact comparable (see Table 7 below).

The Core Arrangement Outside the Loop

The arrangement of the outer core zones is shown in Figures 4 and 5, and some dimensions are given in Table 3. Immediately outside the insulator zone of the loop, a buffer zone (16 cm, or $2\frac{1}{2}$ scattering mean free paths thick) was loaded with standard Zebra fuel sheaths and fuel pieces. The composition (but not the heterogeneity) of this zone (see Table 2) was made close to that of the test zone in the cases of assemblies A, D, H, and J, the mixed oxide assemblies investigated. (Carbon was used to mockup part of the oxygen content.)

Thus the neutron spectra in the pin zones of these

FIG. 3. Schematic of Pins Used in Tests.

TABLE 2. ZONE COMPOSITIONS (atoms/cm³ × 10⁻²²)

Zone	U ²³⁵	U ²³⁸	Pu ²³⁹	Pu ²⁴⁰	O	C	Fe + Cr + Ni	Natural B	Na	Al
<i>Driver</i>	0.431	0.239	—	—	—	5.591	0.540	—	—	—
<i>Filter</i>	—	—	—	—	—	—	7.691	—	—	—
<i>Buffer A, B</i>	0.004	0.597	0.114	0.006	1.203	0.440	3.360	—	—	0.325
Test A (and N)	0.003	0.583	0.105	0.006	1.461	—	3.42	—	—	—
Test B	—	—	0.101	0.006	0.214	—	5.45	0.253	—	—
<i>Buffer C, D, E, F</i>	0.004	0.565	0.108	0.006	1.137	0.416	0.982	—	—	0.550
Test C	—	—	0.101	0.006	0.214	—	2.600	0.253	—	—
Test D	0.003	0.583	0.105	0.006	1.461	—	0.570	—	—	—
Test E (and M)	—	—	—	—	—	—	3.420	—	—	—
<i>Buffer G, H</i>	0.004	0.556	0.108	0.006	1.120	0.410	0.974	—	0.668	0.421
Test H	0.003	0.583	0.105	0.006	1.461	—	0.570	—	0.838	—
<i>Buffer I to O</i>	0.003	0.415	0.080	0.004	0.836	.305	1.655	—	0.754	—
Test J	0.002	0.437	0.079	0.004	1.096	—	1.282	—	0.838	—
Test K	—	—	0.085	0.005	0.180	—	3.054	—	0.838	—
Test L	—	—	—	—	—	—	6.26	—	—	—
Test O	—	—	—	—	—	—	0.675	—	—	2.200
Test F, G, I	—	—	—	—	—	—	—	—	—	—
<i>Insulator</i>	—	—	—	—	0.086	—	1.965	—	—	0.05

FIG. 4. Schematic Diagram of Core 5 (Not to Scale).

assemblies were close to those which would apply in infinite zones of the same compositions. The same matching of compositions of zones did not apply to assemblies B, C, and K, because here the purpose was

to separate the plutonium Doppler contribution in spectra appropriate to assemblies A, D, and J, respectively, and therefore the buffer compositions of the latter were retained (see Table 1). Outside the buffer

FIG. 5. Core-5A Loading Plan

zone were loaded, in order, a filter zone, a driver zone, and a radial blanket zone. The filter zone consisted of approximately one row of fuel sheaths loaded with iron plates. The object of the filter zone was to use the inelastic scattering and absorption of the iron to reduce the proportion of high-energy and very low-energy neutrons, respectively, coming from the driver zone, and so obtain a better spectrum match between it and the buffer zone. The driver zone consisted of 93 % U^{235} plates, 37 % U^{235} plates, and graphite plates. The control rods, which had a fuel section and a B^{10} section, were loaded in this zone. Outside the driver zone (and above and below the driver and buffer zones) was a 30-cm-thick natural uranium blanket.

A series of subsidiary experiments with chambers, which measured the reaction rate ratio $B^{10}(n,\alpha)/U^{235}(n,f)$, indicated that the spectrum radially across the assembly was constant from the centre to near the outer boundary of the buffer. Similar measurements in the vertical direction through the centre showed only small variations along the height of the pins.

Experimental Details

The reactivity at each test zone temperature was measured by the movements of a calibrated control rod at a constant reactor power (usually about 5 W). The calibration was carried out by the reactor-period

TABLE 3. ZONE BOUNDARIES USED IN CYLINDRICAL MODEL OF CORE AND FISSILE LOADINGS

Zone	Outer Radius (cm)	Height of Zone (cm)	Fissile Content (kg)
Test	10.75	68.6	9.5 $Pu^{239(a)}$
Insulation	15.35	81.3	—
Buffer	31.9	50.5	108 $Pu^{239(a)}$
Filter	38.2	50.5	—
Driver	48.0 ^a	50.5	426 $U^{235(a)}$
Radial Blanket	78.1	81.3	—

(a) Assembly 5A.

method, using calculated values of the total effective delayed-neutron fraction (0.0058 in 'A'). Typically, reactivity changes could be measured to an accuracy of about 7×10^{-7} . The reactivity changes had to be corrected for the effect of thermal expansions of the pins and structure (see below), and for background-reactivity drift. Reactivity drift occurred because of nuclear heating and because of heat loss from the loop to the outer core. The rate of temperature increase in the driver elements at the core centre-plane when the power was 5 W was about 0.05°C/hr , and this gave rise to a linear reactivity drift of about $-2 \times 10^{-6}/\text{hr}$. The method of measuring the reactivity of the test zone at a temperature T in the presence of drift was to balance

Fig. 6. Measured Temperature Coefficients (Uncorrected).

the reactor successively at 350, T, 350°C, and to interpolate linearly with time between the 350°C balances so as to obtain the effective background reactivity at the time of the balance point at T. Thus reactivity changes were all related for convenience to one test-zone temperature (350°C). The temperature coefficients measured for each assembly are shown in Fig. 6, and (after correction for expansion effects) in Fig. 8.

Heat flow from the test zone to the buffer zone varied with test-zone temperature. In a typical low-temperature cycle (e.g., 350-120-350°C) the temperature inside an inner-row buffer element measured by a resistance thermometer at the core centre-plane varied by about 0.1°C, and in a typical high-temperature cycle (e.g., 350-550-350) by up to 0.6°C. (The time response of the inner buffer to test-zone changes was about 1 hr.) Calculations showed that these temperature changes made negligible corrections to the measured reactivity changes. Subsidiary experiments were carried out to check this by running the guard-ring circuit at a series of different temperatures, it being shown that the results were insensitive to buffer zone changes of this order.

The mean temperature in the test zone could be measured by means of six resistance thermometers to an accuracy of about 0.5°C. The temperature variations along the length of the temperature probes in contact with the pins never varied by more than about 2°C when the system was balanced, even at 550°C. Varia-

tions radially across the pin zone were also of the order of 2°C.

Allowance for Pin Expansions

The temperature coefficient C_1 associated with the thermal expansion of the column of material in a can (for constant pin base-plate position) was allowed for by use of the expression

$$C_1 = \alpha N \left[-W + l_1 \left(\frac{dW}{dl} \right)_1 \right], \quad (1)$$

where α is the linear thermal expansion coefficient, N is the number of pins loaded of the type being considered, W is the reactivity worth of the column of material at the reactor ambient temperature (50°C), l_1 is the length of the column, and $(dW/dl)_1$ is the reactivity worth per unit length at the top of the column. The expression was evaluated for each type of pin in turn.

Published values of α were used for steel, sodium (i.e., liquid sodium in steel), and aluminum; but for the pellets of the A, B, and D pins, α was measured in the laboratory by use of an apparatus with silica push rods and dial gauges. Values of W and dW/dl were measured in the reactor in each assembly. It was found that, in general, W for fissile pins was nearly constant across the test zone, and it was sufficiently accurate to measure it in one or two positions only. For nonfissile pins, for which the radial variations were more important, values of W were frequently obtained by averaging the effect obtained with 9 or 16 pins distributed over the zone.

Table 4 gives typical values in assembly J. The accuracies of the estimates were, however, dependent mainly on the values of $(dW/dl)_1$, which were obtained in two ways. Measurements of small-sample reactivity worth were obtained for the important materials at a series of heights through the centre of the core, and these were used to deduce dW/dl . Secondly, for the case of sodium, steel, and aluminum, the reactivity worths of special pins with an increased length of material (2.5, 5, and 7.5 cm extra for sodium; 2.5 cm extra for steel and aluminum) were measured. By comparing the reactivity worths of these special pins with the worths (W) of the normal pins, values of dW/dl were again obtained. The two methods were in agreement in the majority of assemblies to within the errors. An additional refinement in the case of some steel pins involved increasing the length by 2.9 cm and at the same time cutting a flat onto the side of the pin such that the total mass was equal to the mass of a normal steel pin. The special pin was thus a reasonable geometric model of a normal pin at 400°C, and the difference in reactivity worths between the pins was then directly related to the temperature coefficient C_1 for steel pins.

TABLE 4. EXAMPLE OF INPUT DATA FOR AXIAL EXPANSION CORRECTIONS (ASSEMBLY J)

	'A' Fuel	Steel	Steel Can	Sodium	Units
α	10.3	18.5	220	18.5	$10^{-6} \text{ }^{\circ}\text{C}$
l_1	68.6	72.4	68.6 ^(a)	72.4	cm
l_3	34.3	34.3	34.3	34.3	cm
W	$+6.9 \pm 0.1$	-0.87 ± 0.04	$+0.135 \pm 0.004$	-0.09 ± 0.01	$\times 10^{-5}$
dW/dl	$+7.3 \pm 1$	$+2.7 \pm 0.3$	$+2 \pm 0.2$	$+0.3 \pm 0.03$	$\times 10^{-7} \text{ cm}$
d^2W/dl^2	—	$+2.0 \pm 0.2$	—	—	$\times 10^{-8}/\text{cm}^2$
dw/dl	—	$+2.7 \pm 0.3$	—	—	$\times 10^{-9}/\text{g}/\text{cm}$

l_2 : 36.1 cm (base-plate, bottom end fittings)
39.3 cm (top end fittings)

R : 10.9 cm

M : 2.4×10^3 gm (base-plate)
12.4 g (bottom end fitting per pin)
17.8 g (top end fitting per pin)

W_2 : $1.25 \pm 0.2 \times 10^{-4}$

dW_2/dR : $2.8 \pm 1.5 \times 10^{-6}/\text{cm}$

(a) i.e., at ambient.

FIG. 7. Estimated Correction of Temperature Coefficient for Assemblies with Sodium Pins.

Measurements with small samples indicated that dW/dl and hence C_1 were constant for expansions over the range of 50–550°C for fuel, steel, and aluminum. But for sodium, a column of which expanded by about 7.5 cm over this temperature range, this was not true, and C_1 was evaluated as a function of temperature (see Fig. 7). The temperature coefficients after correction for expansion effects are shown in Fig. 8.

The reactivity effect of the steel tubes of the cans was evaluated in the same way as for the solid steel pins. The effects of the top and bottom end pieces (C_2), and of the steel pin base-plate (C_3) were each evaluated by means of the expression

$$C_2 \text{ or } C_3 = \alpha l_2 M (dw/dl)_2, \quad (2)$$

where α is the linear expansion coefficient for steel, l_2 is

the distance of the end fitting, or steel plate, from the core centre-plane, M is the mass of steel, and $(dw/dl)_2$ is the gradient of the reactivity worth of steel per g (each part of the steel structure expanded away from the core centre-plane because of the trunnions holding the inner loop at this position were connected to the cold outer structure).

The movement of the base-plate also contributed a reactivity effect by moving the pins, an effect not included in expression (1). This was evaluated for each type of pin by the expression

$$C_4 = \alpha l_3 [(dW/dl)_{l_3} - (dW/dl)_{l_1-l_3}], \quad (3)$$

where α and (dW/dl) are as above, l_3 is the distance of the bottom of the column of material from the core centre-plane, and $l_1 - l_3$ is the distance of the top of

FIG. 8. Measured Doppler Coefficients as a Function of Temperature.

the column from the core centre-plane. For fuel pins where the column was symmetrical about the centre-plane, C_4 was zero. For steel and aluminum pins (and for a sodium column near ambient) the columns were nearly symmetric about the centre-plane, and the expression (3) could be simplified to

$$C_4 = \alpha l_3(l_1 - 2l_3) \left(\frac{d^2 W}{dl^2} \right)_{l_3}. \quad (4)$$

In general the values of C_4 were much less than those found for C_1 , C_2 , and C_3 .

The expansion effect of the inner walls of the loop in contact with the pins was allowed for by use of the expressions

$$C_5 = -\alpha k_1 W_2; \quad (5)$$

$$C_6 = -\alpha k_2 R (dW_2/dr)_R, \quad (6)$$

where C_5 and C_6 represent the axial and radial contributions, respectively. Here W_2 is the worth of a long steel sample (0.32 by 17.5 by 150 cm) which was measured close to the inner wall of the loop (of 0.34-cm thickness), with the lower edge at the core centre-plane.

A scaling factor k_1 was found which allowed for the difference in mass of the sample and the innermost wall of the loop, and allowed also for the change in geometry of the walls in the region beneath the pin base-plate. A device for moving the steel sample radially enabled

values for (dW/dr) to be measured. The effective radius of the test zone is given by R , and k_2 is a scaling factor similar to k_1 . A small correction term was also applied to both C_5 and C_6 to allow for the expansion of the second steel wall, which had a mass about one-half of the first wall, and ran at about one-quarter of the temperature rise of the first wall.

Originally, it had been supposed that because the core had been designed so that both the spectrum and flux were approximately constant radially across the loop and inner buffer, the contribution C_6 due to radial movement of the walls of the loop would be small or negligible. In fact it was found that C_6 was an important correction term. Several experiments were carried out to understand the mechanism of this reactivity change, which is now believed to be due to changes in the number of neutrons streaming up the loop. Accurate determination of the magnitude of C_6 proved to be difficult, and except for the sodium expansion term, this effect was the largest source of error in the corrections and thus in the Doppler coefficients.

Small coefficients were also estimated for the expansion effects of the Santocell insulant and of the nitrogen gas. The reactivity worth of a large sample (4 kg) of the former was measured in the pin zone, and a reactivity coefficient estimated assuming the effective thermal expansion coefficient to be that of steel. The coefficient associated with the nitrogen was estimated from meas-

TABLE 5. BREAKDOWN OF EXPANSION CORRECTIONS ($\times 10^{-7}/^{\circ}\text{C}$)

Assembly	Fissile Pin Expansions $C_1 + C_2$	Nonfissile Pin Expansions at 170°C $C_1 + C_2$	Effect of Pin Base-plate $C_3 + C_4$	Test-zone-wall Axial Expansion C_5	Wall Radial Expansion C_6	Santocell Wall Expansion	Nitrogen Expansion at 170°C	Total Correction
A	-0.09 ± 0.1	$+0.5 \pm 0.1$	$+0.15 \pm 0.06$	-0.3 ± 0.15	-0.4 ± 0.2	-0.1 ± 0.05	-0.1 ± 0.03	-0.4 ± 0.3
B	-0.18 ± 0.04	$+0.88 \pm 0.09$	$+0.14 \pm 0.03$	-0.3 ± 0.15	-0.4 ± 0.2	-0.1 ± 0.05	-0.1 ± 0.03	-0.1 ± 0.3
C	$+0.02 \pm 0.02$	$+0.21 \pm 0.05$	$+0.2 \pm 0.04$	-0.3 ± 0.15	-0.6 ± 0.3	-0.15 ± 0.07	-0.1 ± 0.03	-0.7 ± 0.4
D	-0.26 ± 0.13	$+0.85 \pm 0.2$	$+0.3 \pm 0.1$	-0.3 ± 0.15	-0.6 ± 0.3	-0.15 ± 0.07	-0.1 ± 0.03	-0.3 ± 0.4
E		$+0.96 \pm 0.2$	$+0.1 \pm 0.04$	-0.3 ± 0.15	-0.6 ± 0.3	-0.15 ± 0.07	-0.1 ± 0.03	-0.1 ± 0.45
F		--	$+0.05 \pm 0.03$	-0.45 ± 0.2	-0.8 ± 0.4	-0.2 ± 0.1	-0.1 ± 0.03	-1.5 ± 0.5
G		--	$+0.08 \pm 0.02$	-0.45 ± 0.2	-0.8 ± 0.4	-0.2 ± 0.1	-0.1 ± 0.03	-1.4 ± 0.5
H	-0.02 ± 0.01	$+2.9 \pm 0.4$	$+0.2 \pm 0.1$	-0.3 ± 0.15	-0.4 ± 0.2	-0.1 ± 0.05	-0.1 ± 0.03	$+2.2 \pm 0.5$
I	--	--	$+0.1 \pm 0.02$	-0.4 ± 0.07	-0.7 ± 0.2	-0.2 ± 0.1	-0.1 ± 0.03	-1.3 ± 0.3
J	-0.08 ± 0.05	$+2.7 \pm 0.4$	$+0.11 \pm 0.01$	-0.2 ± 0.05	-0.3 ± 0.2	-0.1 ± 0.05	-0.1 ± 0.03	$+2.1 \pm 0.5$
J ₁	-0.04 ± 0.03	$+2.6 \pm 0.4$	$+0.11 \pm 0.01$	-0.25 ± 0.1	-0.3 ± 0.2	-0.1 ± 0.05	-0.1 ± 0.03	$+2.0 \pm 0.5$
J ₂	-0.02 ± 0.01	$+2.6 \pm 0.4$	$+0.11 \pm 0.01$	-0.25 ± 0.1	-0.3 ± 0.2	-0.1 ± 0.05	-0.1 ± 0.03	$+1.9 \pm 0.5$
J ₃	-0.005 ± 0.005	$+2.6 \pm 0.4$	$+0.11 \pm 0.01$	-0.25 ± 0.1	-0.5 ± 0.3	-0.1 ± 0.05	-0.1 ± 0.03	$+1.8 \pm 0.5$
J ₄	--	$+2.5 \pm 0.4$	$+0.11 \pm 0.01$	-0.25 ± 0.1	-0.5 ± 0.3	-0.1 ± 0.05	-0.1 ± 0.03	$+1.7 \pm 0.5$
K	-0.31 ± 0.03	$+3.5 \pm 1$	$+0.08 \pm 0.01$	-0.25 ± 0.1	-0.3 ± 0.2	-0.1 ± 0.05	-0.1 ± 0.03	$+2.6 \pm 1.0$
L	--	$+0.8 \pm 0.1$	$+0.1 \pm 0.05$	-0.22 ± 0.04	-0.3 ± 0.1	-0.1 ± 0.05	-0.1 ± 0.03	$+0.2 \pm 0.15$
M	--	$+0.7 \pm 0.1$	$+0.1 \pm 0.05$	-0.24 ± 0.04	-0.5 ± 0.15	-0.1 ± 0.05	-0.1 ± 0.03	-0.1 ± 0.15
O	--	$+0.9 \pm 0.1$	$+0.1 \pm 0.05$	-0.26 ± 0.05	-0.5 ± 0.15	-0.1 ± 0.05	-0.1 ± 0.03	0 ± 0.25

urements at varying loop pressure at constant temperature, and also from a series of reactivity worth measurements with a NaN_3 sample.

These corrections are summarised in Table 5.

Effect of Varying Test-zone Area

A number of variants of assembly J were loaded in order to determine whether the measured Doppler coefficients were dependent on the width of the low-density insulating zone between the buffer and test zones. The sodium pins were left in the alternate positions of J, but the numbers of fuel and steel pins were replaced in stages by void pins, two rows at a time, starting with the outer rows first. At each stage until the final one, there was an inner pin region of reduced area whose composition was the same as in J. Finally, in J₄, the test zone contained only sodium and void pins. After correction for expansion effects, the Doppler coefficient was found to be an approximately linear function of the mass of fuel in the zone (see Fig. 9 below). It was concluded that the width of the gap between the test and buffer zones was not important.

Discussion of Results

The measured temperature coefficients together with the corrected Doppler coefficients are shown in Table 6. The corrections are relatively small for the cases of the two assemblies with U^{238} and without sodium (A and D), but they are important for all other assemblies. In particular, there is a large expansion correction which is necessary for each assembly containing sodium, which arises because of the high thermal expansion coefficient

of sodium. In each case the error of the Doppler coefficient is attributed mainly to the errors inherent in the derivation of the corrections, rather than to the errors associated with the measurements of the temperature coefficients (see Fig. 6).

The results with empty cores (F, G, and I) show small residual negative coefficients which are only slightly larger than the errors. It is concluded that the method of deriving the corrections for structure expansions is at least approximately correct. It is, however, possible that there are unexplained expansion mechanisms in the loop (not accounted for above) whose effects are of the order of $-0.5 \times 10^{-7}/^{\circ}\text{C}$, and this must be taken into account when discussing the results of the fueled assemblies. (A comprehensive search was made for fuel-element movements and temperature rises in the buffer and driver zones, but nothing was found which might account for such a coefficient.) With the test zone loaded mainly with sodium [J(4)] or aluminum (O), barely significant small residual negative coefficients were observed. With steel, however, the residual coefficients become more significantly negative (E, L, and M), and the results show some evidence of a small negative Doppler coefficient for steel. (These residual coefficients are plotted as a function of steel loading in Fig. 10).

The positive coefficients calculated for the plutonium assemblies B, C, and K were not observed. The evidence in B and C is for a near-zero or even a slightly negative plutonium contribution. The result from K is anomalous and shows a significant negative effect, even allowing for the large errors associated with the sodium

FIG. 9. Dependence of Doppler Effect on Test Zone Area.

TABLE 6. COMPARISON OF RESULTS ($\times 10^{-7}/^{\circ}\text{C}$)

Assembly	Brief Description of Assembly	Measured Temperature Coefficient at 170°C (Uncorrected)	Corrected Temperature Coefficient (Doppler Effect)	Calculated Doppler Coefficients		
				Hansen and Roach	FD1	FD2
(1) Assemblies with Mixed Oxide (A) Pins (16% PuO ₂ , 84% U ²³⁸ O ₂)						
A	A pins + Fe	-7.7 ± 0.07	-8.1 ± 0.3	-6.3	-3.5	-6.6
D	“ + Void	-6.0 ± 0.1	-5.7 ± 0.4	-5.0	-2.8	-4.6
H	“ + Na	-5.3 ± 0.1	-7.5 ± 0.6	-5.7	-4.3	-6.3
J	“ + Na + Fe	-4.0 ± 0.1	-6.1 ± 0.5	-4.5	-3.7	-5.0
(2) Assemblies with PuO ₂ without U ²³⁸						
B	B pins + Fe	-0.9 ± 0.1	-0.8 ± 0.3	+1.5	+1.5	+2.8
C	“ + Void	-1.5 ± 0.1	-0.8 ± 0.4	+1.1	+1.0	+1.8
K	D pins + Na + Fe	-0.3 ± 0.05	-2.9 ± 1.0	+1.4	—	+3.0
(3) Assemblies with Nonfissile Pins Only						
E	half loading of Fe	-1.4 ± 0.05	-1.3 ± 0.5	0	0	0
L	full “ “ “	-1.5 ± 0.1	-1.7 ± 0.2	0	0	0
M	half “ “ “	-1.1 ± 0.1	-1.0 ± 0.2	0	0	0
J(4)	half “ “ Na	+1.2 ± 0.1	-0.5 ± 0.5	0	0	0
O	full “ “ Al	-0.3 ± 0.1	-0.3 ± 0.3	0	0	0
(4) Assemblies without Pins						
F	—	-2.35 ± 0.05	-0.8 ± 0.6	0	0	0
G	—	-2.3 ± 0.05	-0.9 ± 0.6	0	0	0
I	—	-1.75 ± 0.05	-0.4 ± 0.3	0	0	0

expansions. The reason for this result is not yet understood.

Three calculations are included in the comparison of

TABLE 7. CALCULATED CENTRAL NEUTRON SPECTRA^(a)

Group	E_L , keV	Assembly						
		A	B	C	D	H	J	K
1	3000	279	281	364	353	338	294	299
2	1400	727	745	938	896	847	760	775
3	900	599	688	742	645	586	566	633
4	400	1809	1943	1961	1857	1671	1645	1707
5	100	3312	3280	3182	3248	3244	3265	3191
6	17	2184	2125	1962	2031	2136	2195	2119
7	3	741	648	623	695	795	828	776
8	0.55	284	238	199	234	316	357	376
9	0.10	55	41	26	36	58	76	90
10	0.03	10	10	4	4	8	13	33
11-16	—	0	0	0	0	0	0	1
Totals		10000	10000	10000	10000	10000	10000	10000

^(a) Using Hansen and Roach cross-section data (LAMS 2543) and the CRAM diffusion-theory programme.

results. These are based, respectively, on fluxes and adjoint fluxes calculated using the Hansen and Roach set; the FD1 set, which is a Winfrith extension to the YOM set; and the FD2 set, which is based on the UKAEA nuclear-data library. The calculations were carried out in one dimension by use of the CRAM diffusion-theory programme. The method of calculating the Doppler coefficient using perturbation theory and allowing for the heterogeneity of the pins was described by Smith and Rowlands,⁽¹⁾ and more details are given in another paper at this conference.⁽²⁾

The calculations were carried out for the range 300–600°K. In order to compare them with experiment, the Doppler coefficients are quoted in Table 6 at 170°C, this being the weighted mean temperature for this range if it is assumed that the Doppler coefficients are inversely proportional to temperature. The three calculations tend to underestimate the negative coefficients measured for the mixed oxide assemblies A, D, H, and J by between 15% and 50%. However, the results show that the method of estimating the U^{238} contribution to the Doppler effect is soundly based.

FIG. 10. Residual Temperature Coefficient as a Function of Mass of Heated Steel in Pin Region.

FIG. 11. Temperature Dependence of Measured Doppler Coefficients.

The results also confirm that the Doppler effect varies roughly with inverse temperature, which is the usual assumption made when extrapolating the effect up to the temperature range of a power reactor. The Doppler coefficients when plotted logarithmically against the absolute temperature (see Fig. 11) can be seen to vary approximately as T to the power 'a' (T^a), where for

assembly A, $a = -1.0 \pm 0.2$;
 for assembly H, $a = -1.0 \pm 0.1$;
 and for assembly J, $a = -0.9 \pm 0.1$

over the range of temperature considered.

Acknowledgments

The authors would like to thank all members of the Zebra team who operated the reactor or assisted with the experiments; and, in particular, Mr. R. M. Absalom who participated in the experimental work of the Doppler experiment; Mr. C. R. Patterson who performed the computer calculations; Mr. G. Ingram who performed the perturbation experiments upon which the expansion corrections are based; and Mr. A. S. Belcher who assisted with the processing of the data. They would also like to thank Mr. J. Codd and Mr. J. L. Rowlands for advice on the theory of the Doppler effect, and Mr. G. Gunnill and his team who were responsible for the engineering design of the loop.

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2. J. L. Rowlands and D. Wardleworth, *The interpretation of Doppler and sodium reactivity worth measurements in zero power reactors*, see next paper.

Discussion

Mr. Kato (ANL): Do you plan to continue this series of heated-zone experiments in which you might vary the surrounding core compositions and also use different test elements?

Mr. Wheeler: We haven't any definite plans at the moment, but we can put this loop back at fairly short notice. Although we are not doing any loop experiments in the present assembly, we may very well do so in the future.

Mr. Engelmann (Karlsruhe): It seems to me that you introduce quite an uncertainty due to the expansion of sodium. Did you even consider the use of sodium in a compound? We are planning such experiments at Karlsruhe, and we were thinking of using a lead-sodium alloy which has a higher melting point and a relatively low expansion at these temperatures.

Mr. Wheeler: It is quite true that we lost a lot of accuracy in the experiments with sodium, and we might well consider something as you suggest for another occasion. But the advantages of having sodium in was, I think, that we showed there was nothing special about the presence of sodium which made the calculations worse.

Mr. Yiftah (Israel AEC): Has any attempt been made to analyze the negatively measured and positively

calculated Doppler effect of plutonium, a phenomenon somewhat similar to the negative Doppler effect of uranium-235 measured at Atomics International a couple of years ago?

Mr. Wheeler: I think possibly Mr. Codd may be able to say something about this in the next paper.

Mr. Pitterle (APDA): George Collin at Los Alamos has been advocating a slightly different distribution of resonance parameters for Pu^{239} . His belief is that the $J = 0$ sequence is a sequence of very broad resonances with average fission width between 1 or perhaps 2 eV, and the $J = 1$ sequence for S waves being much smaller fission widths as well as following the $\nu = 1$ distribution of fission widths. We have made some Doppler calculations for plutonium under these conditions, and we find our results less by about a factor of 2 than what we calculated for the test cases of Okrent. The α values are very strongly dependent in this case because 80% or more of the Doppler effect is from the $J = 1$ sequence. If these were high, as some people indicated, although trying to fit parameters to these α values does not seem reasonable, it could well be negative.

Mr. Fischer (ANL): I would like to ask what fission widths were used for the $J = 1$ and 0 states.

Mr. Pitterle: We have tried a number of different combinations. We assume the average fission width for the $J = 0$ sequence to be about 1 eV. Then we attempt to fit group-dependent σ_f and α to obtain both the neutron widths and the fission width for the $J = 1$ sequence. At the moment the values for σ_f and α that we are using are the UK values above a keV and the Schmidt values below. The energy range is from 10

keV to 100 eV with α values varying uniformly from about 0.5 to 0.75 over this energy range. This fitting procedure yields $J = 1$ fission widths varying from 0.06 to 0.09 eV and strength functions varying from 0.8 to 1×10^{-4} . We have also carried out another case which is in agreement with the feelings of George Cowan and Hans Bethe. That is to assume that most of the low-energy resonances that have been resolved are $J = 1$ resonances and that many of the very broad $J = 0$ resonances have not been resolved. If we take the recommended values in the recent BNL supplement and throw out the one plutonium resonance which has a very broad width, we obtain an average fission width of about 70 millivolts for the $J = 1$ sequence. We then assumed a strength function of 1×10^{-4} and extrapolated the low-energy fission width of 70 millivolts exponentially with an increase of a factor of e in 100 keV. This procedure yields a Doppler coefficient within less than 20% of that obtained by the fitting procedure, although the parameters and resulting σ_f and α values are significantly different. The $J = 0$ sequence is dominantly a fission contribution, and this contribution is sensitive to the strength function. An increase in the strength function requires a smaller fission width for the $J = 1$ sequence to obtain a given α value. It is significant that the contribution of the $J = 0$ sequence to the Doppler effect is not negligible. For example, at 300°K this sequence contributes about 10% of the Doppler effect, while at 1500°K it contributes about 20% of the Doppler effect. This is because the Doppler coefficient of this sequence does not decrease as rapidly with temperature as the $J = 1$ sequence.

The Interpretation of Doppler and Sodium Reactivity Worth Measurements in Zero Power Reactors, with Particular Reference to the Zebra Doppler Loop Measurements

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1. Introduction

The kinetic behaviour of large sodium-cooled fast reactors is dominated by the Doppler and sodium reactivity coefficients. Unfortunately, the accurate calculation of these coefficients is one of the most difficult problems in the neutron physics of fast reactors, and many uncertainties in both the data and the methods of calculation remain, in particular, in the methods of interpreting measurements in zero-power reactors.

In order to reduce the uncertainty in the Doppler effect, a series of measurements was made recently in the Zebra Zero Power Reactor by heating the central part of the core to 550°C.⁽¹⁾ The extrapolation of the results obtained from this experiment involves the treatment of the heterogeneity of the test zone and the effect of its finite size. The methods and approximations used in analysing the results are discussed below.

The nuclear data for plutonium are still of less than the desired accuracy, and in this paper a study is made of the effect on the Doppler coefficient from Pu²³⁹ of the present uncertainties in the data from 0.1 keV to 10 keV, and, in particular, in the resonance parameter data in the unresolved resonance region.

Useful experimental data on the sodium coefficient has been reported from a series of experiments with ZPR-VI Assemblies 2 and 3. Calculations have been made to allow a comparison with these measurements using the standard methods of calculation, and some of the approximations involved in these methods are examined.

2. Interpretation of Doppler Effect Measurements

2.1 The Effect of Data Uncertainties on the Doppler Coefficient of Pu²³⁹

Recent measurements at Harwell⁽²⁾ and Saclay⁽³⁾ of Pu²³⁹ cross sections in the resonance region have shown the existence of very broad resonances, with widths of up to about 6 eV. The present measurements cover only part of the resonance region from which the Doppler effect arises in fast reactors, and the way in

which the resonance data are to be extrapolated into the unresolved region is uncertain. However, it has been thought worthwhile to make Doppler coefficient calculations for Pu²³⁹ on the basis of the recent Harwell measurements to see how broad resonances affect the coefficient.

Uttley⁽²⁾ has made a single-level analysis of the resonances in the total cross section of Pu²³⁹ from 100 to 300 eV and has obtained values of the resonance energy, the total width Γ , $\sigma_0\Gamma$, and $g\Gamma_n$ (where g is the statistical spin factor and σ_0 the peak cross section). He also finds a background cross section of 3.6 b in the interval from 100 to 200 eV, and of 7 b in the interval from 200 to 300 eV, which is unaccounted for in the single-level analysis of the data and which it is presumed corresponds to very broad levels.

Calculations have been made using Uttley's resonance parameters by assuming the resonances to form a single sequence with $g = 1/2$. A constant capture width of $\Gamma_c = 40$ meV was assumed, which is the mean obtained by Story⁽⁴⁾ from an analysis of Bollinger's data.⁽⁵⁾ For extrapolation of the fission width distribution above 300 eV the resonances were divided into six approximately equal groups and the mean value of Γ_f for each group found and rounded off. The six values of Γ_f used were 2,520, 300, 100, 45, 25, and 10 meV, the mean value of Γ_f being 500 meV. In each energy group in the unresolved region the cross section was represented by the 24 resonances formed by taking these six values of Γ_f and four values of Γ_n (sampled from a Porter-Thomas distribution), calculated at the mid-lethargy of the group. The strength function assumed was 2.3×10^{-4} (twice the usual value, because of the assumption of a single resonance sequence with $g = 1/2$). The background cross section was allowed for by increasing the fission cross section calculated from the resonance parameters by one third. (The mean value of the observed background, 5 b, is about a quarter of the mean fission cross section.) The potential scattering cross section was taken to be 10.3 b.

Effective group cross sections have been calculated

for seven half-lethargy groups spanning the range from 101 eV to 3.36 keV by means of the code ERIC 2,⁽⁶⁾ which treats the resonances as isolated. The infinite-dilution fission and capture cross sections and the cross sections at 300°K and 600°K for environment background cross sections of 10, 100 and 400 b have been calculated and are given in Tables 1 and 2.

The infinite-dilution cross sections are given in Table 1. The fission cross section in group 16 is about twice the value recommended in the evaluation of Douglas and Barry,⁽⁷⁾ but the value in group 22 is not incon-

sistent with current data. The values of α in all groups are about 30–40% lower than the values recommended by Froelich, Ott, and Schmidt.⁽⁸⁾ Uttley⁽⁹⁾ has made an analysis based on preliminary results of James' Pu²³⁹ fission cross section and his own total-cross-section measurements which gives an α of about 1.2 in the range from 600 eV to 2 keV. This corresponds with a dip in the mean fission cross section, and so the compound elastic scattering is uncertain. The total absorption cross section is not inconsistent with Uttley's estimates when a curve which varies monotonically with energy is drawn through his mean values in 100-eV and 1-keV intervals. (The average values in some intervals deviate by about a factor of two from this monotonic curve.) Neglecting the constant background fission cross section would increase α by a factor 4/3 and so make it more consistent with the data of Froelich, Ott, and Schmidt.

In Table 2 the fission and capture cross sections at 300°K and the difference between the fission and capture cross sections at 600°K and 300°K, $\delta\sigma_f$ and $\delta\sigma_c$, are tabulated. The values are given for three σ_B values (the environment background cross section per atom of the resonant absorber): $\sigma_B = 400$ b, a value which is typical of a dilute fast reactor; $\sigma_B = 100$ b, and $\sigma_B = 10$ b, a value appropriate to a sample of Pu²³⁹ metal of about 1-cm

TABLE 1. Pu²³⁹ CROSS SECTIONS CALCULATED FROM THE ASSUMED RESONANCE PARAMETERS (INFINITE DILUTION VALUES)

Group	Lower Energy, eV	σ_f , b	σ_c , b	α	σ_a , b
16	3360				
17	2040	5.47	1.80	0.329	7.27
18	1230	7.39	2.56	0.346	9.95
19	748	9.93	3.60	0.363	13.53
20	454	13.24	5.00	0.378	18.24
21	275	17.61	7.30	0.415	24.91
22	167	18.96	7.95	0.419	26.91
22	101	24.97	12.61	0.505	37.58

TABLE 2. Pu²³⁹ CROSS SECTIONS AT 300°K AND THE DIFFERENCE BETWEEN CROSS SECTIONS AT 600°K AND 300°K

Group	σ_f , b	σ_c , b	α	σ_a , b	$\delta\sigma_f$, b	$\delta\sigma_c$, b	α'	α'/α	F
A. $\sigma_B = 400$ b									
16	5.40	1.75	0.32	7.15	0.03	0.02	0.67	2.07	0.26
17	7.22	2.43	0.34	9.65	0.06	0.04	0.73	2.17	0.29
18	9.54	3.31	0.35	12.85	0.11	0.09	0.78	2.25	0.32
19	12.44	4.37	0.35	16.81	0.20	0.17	0.83	2.36	0.36
20	15.95	5.91	0.37	21.86	0.34	0.32	0.95	2.57	0.42
21	17.18	6.28	0.37	23.46	0.37	0.38	1.03	3.06	0.48
22	21.37	8.60	0.40	29.97	0.58	0.67	1.14	2.83	0.53
B. $\sigma_B = 100$ b									
16	5.22	1.64	0.31	3.72	0.09	0.06	0.65	2.08	0.26
17	6.86	2.19	0.32	9.05	0.16	0.11	0.70	2.20	0.29
18	8.86	2.83	0.32	11.69	0.27	0.20	0.75	2.34	0.32
19	11.26	3.50	0.31	14.76	0.40	0.32	0.79	2.53	0.36
20	14.00	4.40	0.31	18.40	0.56	0.49	0.87	2.76	0.42
21	15.07	4.54	0.30	19.60	0.60	0.56	0.93	3.09	0.48
22	18.23	5.75	0.32	23.98	0.72	0.72	1.00	3.16	0.52
C. $\sigma_B = 10$ b									
16	4.71	1.32	0.28		0.26	0.15	0.60	2.14	0.25
17	4.99	1.63	0.27		0.37	0.23	0.63	2.29	0.28
18	7.52	1.93	0.26		0.49	0.32	0.65	2.53	0.31
19	9.38	2.23	0.24		0.59	0.39	0.66	2.79	0.34
20	11.61	2.66	0.23		0.71	0.49	0.69	3.00	0.37
21	12.16	2.76	0.23		0.75	0.54	0.72	3.15	0.40
22	14.65	3.44	0.24		0.75	0.55	0.74	3.13	0.40

TABLE 3. RATIO OF THE U^{238} CAPTURE AND B^{10} ABSORPTION CROSS SECTIONS TO THE Pu^{239} ABSORPTION CROSS SECTION AT 300°K FOR $\sigma_B = 400$ b

Group	$\sigma_c^{U^{238}(a)}$, b	$\sigma_a^{B^{10}}$, b	$\sigma_c^{U^{238}}/\sigma_a^{Pu^{239}}$	$\sigma_a^{B^{10}}/\sigma_a^{Pu^{239}}$
16	0.6	12.2	0.084	1.7
17	0.9	15.7	0.094	1.6
18	1.0	20.3	0.078	1.6
20	1.0	33.0	0.046	1.5
21	1.9	42.3	0.081	1.8
22	1.7	54.3	0.057	1.8

(a) These values are the shielded values appropriate to a dilute fast reactor.

radius. Also tabulated are the values of α and $\alpha' = \delta\sigma_c/\delta\sigma_f$. For a narrow distribution of fission widths α'/α is close to unity, increasing as the distribution of fission widths increases relative to the distribution of capture widths (here assumed to be constant). For the distribution of fission widths used in these calculations α'/α lies approximately in the range from 2 to 3. Even if the constant background fission were neglected, α'/α would lie in the range from 1.5 to 2.3.

2.2 Compositions in which the Doppler Coefficient of Pu^{239} Might be Negative

Some guidance about the compositions of Pu^{239} which might have a negative Doppler effect can be obtained by the following simple considerations. The Doppler effect for Pu^{239} is proportional to

$$\nu\delta\sigma_f - (\delta\sigma_f + \delta\sigma_c)\phi^* \quad (2.1)$$

$$= \nu\delta\sigma_f[1 - (1 + \alpha')(\phi^*/\nu)],$$

where ϕ^* is the importance of low-energy neutrons relative to the importance of fission neutrons. This importance is the probability of a neutron causing fission, times ν . If we neglect the variation of ϕ^* with energy at low energies, this is

$$\phi^* = \frac{N_9\nu\sigma_f}{N_9(\sigma_f + \sigma_c) + \Sigma a_{\text{rem}} + B^2D} \quad (2.2)$$

$$= \frac{\nu}{(1 + \alpha)(1 + Y)},$$

where

$$Y = \frac{\Sigma a_{\text{rem}} + B^2D}{N_9(\sigma_f + \sigma_c)} = \frac{\sigma_{\text{rem}}}{\sigma_f + \sigma_c}$$

is the ratio of the absorption in the remaining constituents plus the leakage, to the absorption in Pu^{239} (in the resonance region).

The Doppler effect is therefore proportional to

$$(1 + Y) - \frac{1 + \alpha'}{1 + \alpha} = Y - \frac{\alpha' - \alpha}{1 + \alpha} = Y - F. \quad (2.3)$$

The quantity $F = (\alpha' - \alpha)/(1 + \alpha)$ is also tabulated in Table 2. The Doppler coefficient is negative when F is larger than Y (or, alternatively, when α'/α is larger than $\sigma_{\text{rem}}/\sigma_c$).

If the present representation of the resonances gives a correct estimate of F , then the Doppler coefficient of Pu^{239} will be negative when the ratio of the leakage plus absorption in other constituents to the absorption in Pu^{239} is less than about 0.35, on the average, through the Doppler energy region (100 to 2,000 eV).

In Table 3 the U^{238} capture cross section (shielded appropriately to a dilute fast reactor) and the B^{10} absorption cross section are given. Also given are the ratios of these to the calculated Pu^{239} absorption cross section. For a core in which the leakage and absorption in the Doppler region in isotopes other than Pu^{239} and U^{238} are small, the Pu^{239} Doppler coefficient would be zero for a ratio of U^{238} to Pu^{239} of about 5, according to the above analysis. This is a typical value for a large dilute fast reactor. It would be negative for smaller ratios of U^{238} to Pu^{239} and positive for larger values. In the Zebra Doppler Loop U-PuO₂ pins (A), the ratio is about 6, and so the above analysis would be expected to give a small positive Pu^{239} Doppler coefficient for these. The Zebra Doppler Loop Pins (B) contain a ratio of B^{10} to Pu^{239} of 0.5, and so these would be expected to have a positive Pu^{239} coefficient. The PuO₂ pins in Core 5K are diluted with steel and sodium, and so the Pu^{239} coefficient would be expected to be negative.

The variation of neutron importance with energy also contributes to the Doppler effect; a neutron importance which increases towards lower energies makes a positive contribution to the Doppler coefficient because the increase in the absorption cross section with temperature moves the mean absorption to a higher neutron energy. An α value which increases towards lower energies tends to make the Doppler coefficient slightly more negative than the above simple treatment would suggest, but the variation of α with energy in the Doppler region is uncertain.

It seems possible for the Doppler effect of a small sample of Pu^{239} to be positive in a core which has a negative Pu^{239} coefficient. This is because α' is smaller for the small value of σ_B appropriate to a sample of metal or oxide. The F value calculated from the α' value corresponding to $\sigma_B = 10$ b and the α value corresponding to $\sigma_B = 400$ b is about 0.23, compared with the value of 0.35 obtained with the α' for $\sigma_B = 400$ b. However, the sample may perturb the neutron importance, and first-flight collisions in the sample may increase the importance of fission neutrons.

Since the extrapolation of Uttley's resonance data is inconsistent with estimates of α , the sign of the Pu^{239} Doppler coefficient in fast reactors must be regarded

as uncertain. If the distribution of fission widths is approximately the same as that assumed here, the ratio α'/α would remain approximately the same and F would increase with increase in α , leading to less positive Pu^{239} Doppler coefficients. If, however, the higher values of α correspond to a reduction in the constant background fission and the number of broad resonances, F will decrease.

2.3 Pu^{239} Doppler Coefficients in a Sodium-cooled Reactor and in the Doppler Loop

These cross sections have been used in reactivity calculations for a large sodium-cooled fast reactor and for two of the Zebra Doppler Loop compositions. The Pu^{239} Doppler coefficient obtained for the large fast reactor was $+7 \times 10^{-7} \delta k/^\circ\text{C}$ (the mean value for the range from 300°K to 600°K) compared with the value $+7 \times 10^{-6} \delta k/^\circ\text{C}$ obtained using a mean fission width of $\Gamma_f = 78 \text{ meV}$ and a narrow distribution. The reactor was a two-zone core with a $\text{U}^{238}/\text{Pu}^{239}$ ratio of 6.03 in the inner core, and of 4.21 in the outer core, the plutonium containing 17% Pu^{240} .

For the U-Pu oxide pins in the Zebra Doppler Loop the absorption in other isotopes, relative to that in Pu^{239} , is least in Core 5D, which contains no diluent pins. The mean Pu^{239} coefficient obtained using the cross sections in Table 2A ($\sigma_B = 400 \text{ b}$) was $2.4 \times 10^{-8} \delta k/^\circ\text{C}$ compared with the value $12 \times 10^{-8} \delta k/^\circ\text{C}$ obtained using the narrow fission width distribution and $\sigma_s \simeq 200 \text{ b}$, and the value $-5.8 \times 10^{-8} \delta k/^\circ\text{C}$ obtained for the U^{238} coefficient. Assuming a $1/\sigma_s$ dependence for the Pu^{239} coefficient in this range ($\sigma_s = \sigma_B + \sigma_p$, and $\sigma_p = 10.3 \text{ b}$) gives for a σ_s value of 200 b , $5 \times 10^{-8} \delta k/^\circ\text{C}$ for the cross sections in Table 2, compared with $12 \times 10^{-8} \delta k/^\circ\text{C}$ for the narrow width distribution.

For the plutonium pins, the value was also $2.4 \times 10^{-8} \delta k/^\circ\text{C}$ (for $\sigma_B = 400 \text{ b}$) compared with the value $18 \times 10^{-8} \delta k/^\circ\text{C}$ (for $\sigma_s \simeq 200 \text{ b}$) for the narrow fission width distribution. Allowing for the differences in σ_s this is a reduction to one quarter.

2.4 The Effect of Heterogeneity and Sample Size on the Doppler Effect

For the interpretation of the Zebra Doppler Loop experiment, and of measurements of the Doppler effect of small samples, the effects of heterogeneity and sample size have been allowed for by using effective cross sections for equivalent homogeneous media. To obtain this equivalence the space-averaged flux in the homogeneous region containing the heated resonant absorber must be expressed in the form

$$\begin{aligned}\bar{\phi}_0(E) &= 1/[S + \Sigma_0(E)] \\ &= 1/N[\sigma_s + \sigma_{\text{res}}(E)],\end{aligned}\quad (2.4)$$

where $\Sigma_0(E)$ is the total macroscopic cross section in this region, S is a constant, N is the atomic density of the resonant absorber in the region, σ_s the effective background cross section, and σ_{res} the resonant cross section.

The effective cross section for the resonant absorber averaged over the energy dependence of the flux in the region is then the same as that for an infinite homogeneous medium which has a macroscopic cross section (for scattering out of the resonances) larger than that of the material in the region by an amount S . Greebler and Goldman⁽¹⁰⁾ have called this increase in the effective background cross section, $N\sigma_{\text{escape}}$.

Two methods have been used to obtain σ_{escape} . The first is to obtain the constant S from a rational fit to the collision probability which is appropriate to a cross section in the wings of a resonance. In doing this the methods of Bell⁽¹¹⁾ and Rothenstein⁽¹²⁾ are followed. The second is to obtain the mean flux in the material for two values of the resonant cross section and to assume a linear variation of the reciprocal of the flux. These methods are not as accurate as those of Otter⁽¹³⁾ and Kelber,⁽¹⁴⁾ who average the parameters through resonances, but they are expected to give values of σ_{escape} which are accurate to about $\pm 50\%$. The main uncertainty probably arises from approximating the complex geometry by two uniform homogeneous regions.

The effect of the heterogeneity on the group-averaged flux has not been treated in the Zebra calculations, the effective cross section for the resonant region being taken to be the value appropriate to the cell-averaged flux.

2.5 Derivation of σ_{escape} for an Isolated Specimen and a Regular Lattice

In the case of a two-homogeneous-region problem, where region 0 contains the resonant absorber and region 1 contains nonresonant material, the mean flux in region 0 can be written in the form (2.4) if the collision probability P_{00} can be written in the form

$$1/(1 - P_{00}) = 1 + K\Sigma_0, \quad (2.5)$$

where K is approximately independent of Σ_0 . (Here $P_{i,j}$ is the probability that a neutron originating in region i will make its next collision in region j .) It then follows that $\sigma_{\text{escape}} = 1/NK$. It is well known that P_{00} can be approximated in this form for isolated regions of resonant material and for regular lattices. For an isolated region $K = Cl_0$ where C is of the order of unity, l_0 being the mean chord in the region $4V_0/S$, and so

concerned, the insulation to a large extent isolates the test zone and buffer region spectra. At the top and the bottom of the zone the insulation will be greater and so it is a reasonable approximation to take $\sigma_{\text{escape}} \simeq 1/l_z N$. This expression has been derived for a homogeneous zone. In the heterogeneous case, the effect of the finite zone size is to increase G (the probability that a neutron leaving the fuel region will make its next collision in the moderator without first entering a fuel region).

The probability of a neutron entering a fuel cell is reduced by a constant amount (on average), depending on the size of the zone. This reduction can be treated by increasing the effective size of the fuel cell, by increasing $l_1 \Sigma_1$ so that the correct value for σ_{escape} is obtained in the limit as l_0 and $l_1 \rightarrow 0$:

$$\sigma_{\text{escape}} = \frac{l_1 \Sigma_1 + (l_0/l_z f)}{N_0 l_0 [1 + E(l_1 \Sigma_1) + (l_0/l_z f)]} \quad (2.16)$$

$$= \frac{\sigma_m + \sigma_z}{1 + E(l_1 \Sigma_1 + N_0 l_0 \sigma_z)}, \quad (2.17)$$

where f is the fuel volume fraction and $\sigma_z = 1/N_0 l_z$.

The fuel pins in the test zone have a height of 18.56 cm and the cross section is a square of width 18.2 cm; thus $l_z = 16$ cm. The finite size of the test zone is equivalent in the calculation of the effective background cross section σ_s , to increasing the mean macroscopic cross section by $1/l_z = 0.0625 \text{ cm}^{-1}$. This is 40% of the mean macroscopic cross section corresponding to the U-Pu oxide pins (Set A) and 20% of the value for the Pu pins (Set B) (in the absence of diluent pins).

2.8 Values of the Effective Background Cross Section, σ_s , for U^{238} and Pu^{239} in the Zebra Doppler Loop

We may summarize the results of the previous two sections. Approximate values for the effective background cross sections appropriate for computing the resonance shielding factors and Doppler coefficients for U^{238} and Pu^{239} in the Zebra Doppler Loop are given in Table 7. In deriving these, the background cross sections have been assumed to be independent of energy,

TABLE 7. APPROXIMATE VALUES OF σ_s , THE EFFECTIVE BACKGROUND CROSS SECTION (in barns)

Pins	Component	Homogeneous	Heterogeneous	Finite Zone Size	Total
A	U^{238}	26.6	26.3	9.5	36
	Pu^{239}	148	147	51	198
A + SS	U^{238}	85.8	61.1	6.5	68
	Pu^{239}	477	339	35	374
B	Pu^{239}	297	162	29	191
	Pu^{239}	637	220	19	239

a cross section of 11 being assumed for steel atoms and of 3.7 b for oxygen, with a potential scattering cross section of 10 b for uranium and plutonium isotopes.

A single, energy-independent value cannot be given for the case of sodium diluent pins because of the 3-keV sodium resonance. The effect of heterogeneity and zone size are largest for U^{238} in the case of the U-Pu pins in the absence of diluent pins, when σ_s is about 35% higher than the homogeneous value.

The effects of heterogeneity and zone size are largest for Pu^{239} in the case of the plutonium pins with the steel diluent pins, when the σ_s value is only 37% of the homogeneous value and has a large uncertainty because of the simplifications made in the calculation of the heterogeneity effect. The Pu^{239} Doppler coefficient is about thrice the homogeneous value, with an uncertainty of a factor of about two arising because of the simplifications made in treating the heterogeneity.

2.9 The Doppler Effect of a Small Specimen in a Core Containing the Same Resonant Absorber

A way of obtaining a value for σ_s in this case is to calculate the mean flux in the specimen, $\bar{\phi}_0$, corresponding to the resonant absorber cross section between resonances, ϕ_n , and for a value within a resonance, ϕ_r , and to assume a linear variation of $1/\bar{\phi}_0$ with the resonant absorber cross section between these two values. This gives

$$\sigma_s = \frac{\phi_r - \sigma_r}{\phi_n - \phi_r}, \quad (2.18)$$

σ_r , being the value of the resonant cross section for which ϕ_r was calculated.

Taking the flux in the specimen at energies between resonances to be the same as that in the core and assuming the narrow-resonance approximation gives

$$\sigma_{\text{escape}} = 1/C_0 l_0 N \quad (2.19)$$

for a specimen in a core containing no resonant absorber. The value of C_0 is the one corresponding to the specimen cross section within the resonance.

As an example of the estimation of σ_s for a specimen in a homogeneous core containing the same resonant absorber as the specimen, a U^{238} metal specimen of density N_0 is considered in a homogeneous core containing U^{238} at density N_1 . The remaining core constituents are assumed to be nonresonant and to have a macroscopic cross section Σ_m . The mean flux in the specimen is given by

$$\Sigma_0 \phi_0 = S_0 P_{00} + S_1 P_{01}, \quad (2.20)$$

where S_0 is the neutron source density in the specimen and S_1 the source density in the core (these source

densities being assumed to be spatially constant). Eliminating P_{01} by using the reciprocity relationship

$$\Sigma_1 P_{01} = \Sigma_0 (1 - P_{00}) \quad (2.21)$$

and writing

$$\Sigma_0 = N_0 (\sigma_p + \sigma_{res})$$

and

$$\Sigma_1 = \Sigma_m + N_1 (\sigma_p + \sigma_{res}),$$

where σ_p is the potential scattering cross section of U^{238} and σ_{res} the resonant cross section of U^{238} , the mean flux becomes

$$\bar{\sigma}_0 = \frac{S_0 P_{00}}{N_0 (\sigma_p + \sigma_{res})} + \frac{S_1 (1 - P_{00})}{\Sigma_m + N_1 (\sigma_p + \sigma_{res})}. \quad (2.22)$$

If the source densities in the resonances are taken to be the values corresponding to scattering from between resonances by a uniform flux in core and specimen, then

$$S_0 = N_0 \sigma_p \phi_n; \quad S_1 = (\Sigma_m + N_1 \sigma_p) \phi_n \quad (2.23)$$

and

$$\frac{\phi_0}{\phi_n} = \frac{P_{00}}{1 + \frac{\sigma_{res}}{\sigma_p}} + \frac{1 - P_{00}}{1 + \frac{\sigma_{res}}{\sigma_m^*}}, \quad (2.24)$$

where

$$\sigma_m^* = \frac{\Sigma_m}{N_1} + \sigma_p. \quad (2.25)$$

Taking the values $\sigma_p = 10$ b and $\sigma_m^* = 40$ b, a U^{238} density in the specimen of $N_0 = 0.05 \times 10^{-24}$ atoms/cm³, and a cylindrical rod of 1-cm diameter gives $\sigma_s = 17$ b (for $\sigma_r = 40$ b). The value given by this method for a U^{238} specimen in a core which does not contain U^{238} is $\sigma_s = 36$ b. The presence of the U^{238} in the core has a significant effect on the effective background cross section for U^{238} in the specimen.

Calculations of the Pu^{239} coefficient based on Uttley's resonance measurements in the energy range from 100 to 300 eV still predict positive values for the U-PuO₂ pins and the Pu-boron pins, but the values are reduced to a third or a quarter of the values given in Table 8. It is expected that these data will predict a negative Pu^{239} coefficient for core 5K, which contained no U^{238} or boron.

Calculations have also been made with use of the Hansen and Roach cross-section set⁽²¹⁾ and the FD1 set. Some calculations were made with a two-dimensional model, and the heterogeneous values of σ_s were used in the Hansen and Roach calculations. These are reported in the paper by Baker and Wheeler.⁽¹⁾

TABLE 8. COMPARISON OF MEASURED AND CALCULATED DOPPLER COEFFICIENTS

Assembly	Composition	Measured	Calculated		
			U ²³⁸	Pu ²³⁹	Total
(1) Assemblies with Mixed Oxide Pins: 16% PuO ₂ , and 84% _c UO ₂ (Set A)					
A	A pins + Fe	-8.1 ± 0.3	-8.3	+1.7	-6.6
D	“ + Void	-5.7 ± 0.4	-5.8	+1.2	-4.6
H	“ + Na	-7.5 ± 0.6	-8.7	+2.4	-6.3
J	“ + Na + Fe	-6.1 ± 0.5	-6.5	+1.5	-5.0
(2) Assemblies with PuO ₂ without U ²³⁸ (Sets B and D)					
B	B pins + Fe	-0.8 ± 0.3		+2.8	+2.8
C	“ + Void	-0.8 ± 0.4		+1.8	+1.8
K	D pins + Na + Fe	-2.9 ± 0.8		+3.0	+3.0

2.10 Comparison of Doppler Coefficients Calculated Using the FD2 Set with the Values Measured in the Zebra Doppler Loop

Calculations have been made with the FD2 cross-section set⁽²⁰⁾ for a homogeneous model of the Zebra Doppler Loop. Diffusion-theory calculations were made for an infinite cylinder with an axial buckling, and the calculated Doppler coefficient was reduced by 15% to allow for the length of the fuel pins being less than the height of the core. Heterogeneity and test-zone size were allowed for by multiplying the calculated Doppler coefficients by a factor. This factor was the inverse ratio of the σ_s values for the homogeneous and heterogeneous models for the Pu^{239} coefficient and the change was one-fifth of the percentage difference between these σ_s values for the U^{238} coefficient. The resonance data used in the Doppler calculations were those described in the report on the FD1 set.⁽²²⁾ The coefficient is the average value for the range from 300°K to 600°K.

The Pu^{239} coefficient was calculated to be positive, whereas the measured value is estimated to be negative. If the Pu^{239} coefficient had been taken to be zero for the U-Pu oxide pins, the calculated coefficient would be closer to the measured value, being a slight overestimate, whereas the calculated net coefficient is an underestimate.

3. The Sodium Coefficient

3.1 Introduction

The methods used at present in the UKAEA at Winfrith for the calculation of sodium loss and temperature coefficients for fast reactor design studies are

based on diffusion theory and the multigroup cross-section set FD2.⁽²⁰⁾ The basic nuclear data used for the FD2 compilation are largely those in the UKAEA Nuclear Data Files,⁽²⁸⁾ with the following two main exceptions:

1. The sodium cross sections and the elastic removal cross sections for the main light diluents have been evaluated by means of the ANL program ELMOE.⁽²⁶⁾
2. Heavy-nuclide self-shielding factors are evalu-

ated for each mixture by methods that were used for the cross-section set FD1.⁽²²⁾

The first systematic set of measurements of the sodium coefficient in a zero-power facility were made in ZPR-VI. This section of the paper is a description of a series of calculations performed to check the bases of the Winfrith methods against the experimental measurements made in ZPR-VI Assembly 2.

3.2 The ZPR-VI Assembly 2 Sodium Coefficient Measurements—Two-dimensional (RZ) Calculations

ZPR-VI Assembly 2 was a cylindrical, U^{235} -fuelled assembly, with a core 36 in. in both height and diameter. The composition is given in Table 9; sodium is one of the main diluents.

Measurements of the sodium coefficient were made by removing sodium from various regions of the core and determining the ensuing reactivity change. A description of the core and of the experiments can be found in the ANL progress reports for 1964 (the sodium

TABLE 9. COMPOSITION OF ZPR-VI ASSEMBLY 2

	Core	Blanket
Stainless Steel { Fe 71% Cr 19% Ni 10%	0.01460	0.006344
Sodium	0.007908	—
Carbon	0.01290	—
U^{238}	0.009850	0.03993
U^{235}	0.002284	8.104×10^{-5}

FIG. 1. Layout of Zones A, B, C, and D.

coefficient measurements are reported in ANL-6904⁽²³⁾ and in a paper by Grifoni *et al.*⁽²⁴⁾ Some modifications of the experimental results are made in this latter paper, which also reports one-dimensional slab and cylindrical diffusion-theory calculations.

Because of the importance of the sodium coefficient in fast reactor design and the suspected large error in the calculation of this parameter, it is of obvious value to check the present calculation methods and cross-section data against this set of experiments. Two-dimensional (RZ) diffusion-theory calculations (using the program CRAM⁽²⁵⁾) were performed, using the Winfrith data sets FD1 and FD2, to obtain a normal and adjoint flux. The sodium coefficients for the experimental configurations were calculated by use of first-order perturbation theory.

The layout of the experimental regions and their relationship to the calculation model is shown in Figs. 1, 2, and 3. The nomenclature for the regions is that of ANL-6904, not that of Grifoni *et al.* The mesh spacing is given in Table 10; an even mesh was used between

the points in the table. The adequacy of the mesh was checked by a series of 3-group calculations, doubling and redoubling the standard 24 x 24 mesh.

Table 11 compares the coefficients from the 24 x 24, 48 x 48, and 96 x 96 calculations. The largest change in decreasing the mesh spacing by a factor of 4 is $0.02 \times 10^{-4} \delta k/k$ per kg. In the standard calculation the first 24 groups of FD1 and FD2 were used, that is, the energy representation ceased at 37 eV, which is certainly adequate in this case. The normal flux at the centre in the 24th group is smaller by about six orders of magnitude than the peak flux in any group. A homogeneous model was used.

In FD1 and FD2 the sodium cross sections are defined as the change in system macroscopic cross sections per atom of sodium removed. This change was evaluated by use of ELMOE, and therefore the changes in the group cross sections for the light nuclides on removal of sodium are included. The sodium cross sections in the FD2 set were evaluated for a typical plutonium-fuelled fast power reactor and are therefore not entirely

FIG. 2. Layout of Zones E and F.

FIG. 3. Layout of Zones G through J.

TABLE 10. THE 24 X 24 MESH

Radial		Axial	
Mesh Point	Radius (cm)	Mesh Point	Height above Centre-line (cm)
4	9.15	18	45.72
6	14.03	24	75.72
8	19.76		
10	24.70		
14	34.80		
18	46.15		
24	76.15		

applicable to this assembly. The corresponding cross sections for this assembly have been evaluated but have not yet been used in a two-dimensional calculation.

The FD1 and FD2 libraries in CRAM include a facility for automatic heavy-nuclide self-shielding according to a homogeneous model. The change in the

TABLE 11. THE EFFECT OF DECREASED MESH SPACING: 3 GROUPS ($\delta k/k$ per kg $\times 10^4$)

	24 x 24	48 x 48	96 x 96
A	-0.08	-0.07	-0.07
B	0.05	0.07	0.07
C	0.13	0.14	0.14
D	0.89	0.90	0.90
E	0.55	0.56	0.56
F	0.44	0.46	0.46
G _A	0.01	0.02	0.02
G _B	0.46	0.48	0.48
G _C	1.06	1.06	1.06
H _A	0.38	0.40	0.40
H _B	0.65	0.66	0.66
H _C	0.97	0.97	0.97
I _A	0.82	0.83	0.83
I _B	0.85	0.86	0.86
I _C	0.83	0.83	0.83
J _A	1.16	1.16	1.16
J _B	0.98	0.98	0.98
J _C	0.68	0.68	0.68

TABLE 12. THE TWO-DIMENSIONAL (RZ) RESULTS
(δ (1/k) per kg $\times 10^4$)

	FD1	FD2	Experiment
A	0.33	0.46	0.36 ± 0.07
B	0.48	0.61	0.46 ± 0.02
C	0.67	0.80	0.66 ± 0.02
D	1.31	1.42	1.41 ± 0.02
E	0.97	1.09	1.09 ± 0.02
F	0.86	0.98	1.00
G _A	0.43	0.56	0.43 ± 0.06
G _B	0.90	1.03	0.92 ± 0.06
G _C	1.44	1.54	1.72 ± 0.06
H _A	0.84	0.97	1.02 ± 0.06
H _B	1.07	1.20	0.98 ± 0.06
H _C	1.30	1.37	1.26 ± 0.06
I _A	1.26	1.39	1.37 ± 0.04
I _B	1.22	1.32	1.27 ± 0.04
I _C	1.07	1.11	1.19 ± 0.04
J _A	1.50	1.59	1.65 ± 0.06
J _B	1.24	1.30	1.21 ± 0.06
J _C	0.82	0.83	0.77 ± 0.03

TABLE 13. DISCREPANCY BETWEEN CALCULATION
AND EXPERIMENT (%)

	A		B		C	
	FD1	FD2	FD1	FD2	FD1	FD2
G	0	+29	-3	+12	-17	-12
H	-22	-5	-10	+22	+3	+9
I	-9	+2	-4	+4	-11	-7
J	-10	-4	+3	+7	+7	+7

uranium cross sections with removal of sodium evaluated in this way was included in the perturbation calculations. No heterogeneity correction was made, and this will therefore not be a very accurate estimate of the effect.

The calculated sodium coefficients for FD1 and FD2 are compared with experiment in Table 12. The coefficients are $\delta(1/k)$ per kilogram, and the experimental values were derived from the reported values, which were in 1h/kg, using the reported relation $451 \text{ 1h} = 1\% \delta k/k$. Experimental uncertainties were reported only for regions A, B, C, D, and E. The experimental errors for the other regions were guessed as 1 inhour for reactivity changes greater than 20 1h, $\frac{1}{2}$ 1h for reactivity changes less than 20 1h. They are in the table only to give an estimate of the kind of uncertainty that is thought to exist, and it is acknowledged that they might be disputed.

A brief description of the layout of the regions is as follows: A, B, and C were central cylinders, with a length-to-diameter ratio (L/D) of about one, increas-

ing in size alphabetically from A to C; D was a cylinder at the top of the core; E and F were cylinders of different diameter, decreasing alphabetically, from the top of the core to the centreline; the regions G to J were cylinders (G) and rings (H to J) mapping the sodium coefficient through the core, going out from the centre alphabetically from G to J, and upwards from the centre alphabetically in the subscript from A to C; G_A is therefore a central cylinder and J_C a ring at a top corner.

Table 13 is in the form of a map of the percentage discrepancy between each of the cross-section sets and the experimental measurements, laid out as a quarter axial section through the core for regions G_A to J_C. Column C and row J are next to the blanket and row H contains the control rods.

3.3 One-dimensional (Spherical) Calculations

A number of one-dimensional calculations were also performed in spherical geometry. Their aims were to check in a limited way:

1. the adequacy of first-order perturbation theory;
2. the effect of sodium data from ELMOE derived specifically for ZPR-VI Assembly 2.

The regions A, B and C at the centre of the core lend themselves to a simple spherical model. The sodium coefficient was calculated:

1. directly, as the difference between two k calculations using CRAM;
2. by first-order perturbation theory.

The calculations used FD1, FD2, and FD2*, where FD2* contains light-nuclide cross sections from ELMOE derived for this assembly. The results are in Table 14 as $\delta k/k$. The results indicate that:

1. the difference between direct calculation and

TABLE 14. THE ONE-DIMENSIONAL RESULTS
($\delta k/k \times 10^4$)

	Experiment	Direct 1d.	1st-order 1d.	1st-order 2d.
FD1				
A	0.44	0.44	0.446	0.401
B	2.62	3.01	3.08	2.72
C	9.98	10.13	10.61	10.06
FD2				
A	0.44	0.57	0.596	0.552
B	2.62	3.52	3.84	3.45
C	9.98	11.40	12.60	12.06
FD2*				
A	0.44	0.46	0.486	
B	2.62	3.04	3.34	
C	9.98	10.30	11.45	

TABLE 15. SODIUM COEFFICIENT COMPONENTS
FOR REGIONS A, B, AND C

	FD1		FD2	
	1d	2d	1d	2d
<i>Leakage</i>				
A	0.113	0.108	0.121	0.116
B	0.301	0.276	0.322	0.295
C	0.503	0.496	0.538	0.530
<i>Moderation</i>				
A	0.496	0.455	0.758	0.719
B	0.471	0.420	0.722	0.661
C	0.404	0.369	0.623	0.585
<i>Absorption</i>				
A	-0.087	-0.083	-0.172	-0.165
B	-0.083	-0.079	-0.164	-0.156
C	-0.073	-0.071	-0.143	-0.140
<i>Self-Shielding</i>				
A	-0.153	-0.149	-0.214	-0.215
B	-0.147	-0.139	-0.204	-0.192
C	-0.129	-0.124	-0.179	-0.172

first-order perturbation theory is small, but not negligible for the larger reactivity changes. The results here are somewhat ambiguous. For case C, FD1 shows a 5% difference whereas FD2 and FD2* show a difference of 11%;

2. FD2* improves the agreement with experiment over FD2.

Table 15 lists the sodium coefficient components for FD1 and FD2 in 1d and 2d. One point to note in this table is that the self-shielding term (that part of the coefficient resulting from the change in heavy-nuclide self-shielding with loss of sodium) is of the same order as the absorption term (due to the loss of the capture in sodium).

3.4 An Interpretation of the Calculations for ZPR-VI Assembly 2

The shape, in the energy dimension, of the adjoint function in a uranium-fuelled system is significantly different from that in a plutonium-fuelled system. Sodium coefficient measurements in uranium-fuelled systems, such as ZPR-VI Assembly 2, are therefore of no

FIG. 4. The Normal Spectrum along the Central Axis.

direct relevance to plutonium-fuelled reactor design in so far as the moderation term is concerned. (The moderation term is that part of the sodium coefficient which derives from the change in moderation with loss of sodium.) An accurate calculation of the leakage term (which derives from the change in leakage with loss of sodium) depends on an accurate calculation of the normal flux spectrum, of the spatial variation of the flux, and on an accurate knowledge of the transport cross sections for sodium and for the system. The spectrum will depend strongly on the amount and type of moderator in the system rather than on the fuel, and the diluent composition of ZPR-VI Assembly 2 is to some extent typical of most sodium-cooled reactors at present under consideration. In ZPR-VI Assembly 2 the main contributor to the system transport cross section is U^{238} . This again is a typical situation. The accuracy with which the leakage component is being calculated here is, therefore, of immediate relevance to plutonium-fuelled as well as uranium-fuelled reactor design.

The discrepancy map (see Table 13) indicates an overestimate of the sodium coefficient for all excepting the outer regions by FD2. It is of some interest to see whether these discrepancies can be explained by assuming a constant error in the moderation term. If we assume that the spatial variation of the flux is being calculated correctly, whether the error in a given term of the coefficient is constant will depend on whether the spectrum is constant over the core. Figures 4 and 5 show the variation of the spectra along the central axis. The spectra are plotted at the centre and towards the edge. It can be seen that the normal spectrum varies hardly at all, and the adjoint does not change until the experimental region G_c is reached. This stability of the spectra is reflected in the group-by-group breakdown of the terms shown in Figs. 6 and 7. In Fig. 6 the fraction of the total leakage term from each group is plotted for the most extreme cases, G_A at the centre and J_C at the top corner. There is very little change. In Fig. 7 the group breakdown of the moderation term is plotted (normalised to the group 1

FIG. 5. The Adjoint Spectrum along the Central Axis.

FIG. 6. Fraction of Total Leakage Component (FD2).

contribution) for G_A , G_B , and G_C . The graphs for G_A and G_B are very similar. There should therefore be some constant discrepancy for the terms separately away from the core-blanket interface.

Table 16 shows the ratios between the terms for those regions away from the core edge. The self-shielding, absorption, and moderation terms depend on the spatial variation of the flux in a similar way. The leakage term depends on the spatial variation of the current. Any errors in the calculation of the former

three terms cannot easily be separated, and they can most simply be considered together.

In Table 17 the effect of reducing the calculated sum of the moderation, absorption and self-shielding terms by 40% is shown. The agreement with the experimental measurements is considerably improved except for the regions H_A and H_B . These regions contain the control rods, which may introduce some significant perturbation to the spectrum and therefore introduce an effect not included in the calculation. That

FIG. 7. The Moderation Component (FD2) (Normalised to Group 1 Contribution = -1).

TABLE 16. RATIO BETWEEN TERMS (FD2)

	Self-shielding	Moderation	Leakage
	Absorption	Absorption	Moderation
<i>A</i>	1.30	-4.4	0.2
<i>B</i>	1.23	-4.2	0.4
<i>C</i>	1.22	-4.2	0.9
<i>G_A</i>	1.22	-4.2	0.4
<i>G_B</i>	1.20	-4.0	1.8
<i>H_A</i>	1.23	-4.1	1.4
<i>H_B</i>	1.22	-3.8	3.0
<i>I_A</i>	1.22	-3.3	4.7
<i>I_B</i>	1.22	-3.1	6.9

TABLE 17. MODIFIED CALCULATION RESULTS (FD2)

	Leakage	Other Terms	Modified Other Terms	Total	Modified Total	Experiment
<i>A</i>	0.12	0.34	0.20	0.46	0.32	0.36 ± 0.07
<i>B</i>	0.29	0.31	0.19	0.60	0.48	0.46 ± 0.02
<i>C</i>	0.53	0.27	0.16	0.80	0.69	0.66 ± 0.02
<i>G_A</i>	0.24	0.32	0.19	0.56	0.43	0.43 ± 0.06
<i>G_B</i>	0.83	0.20	0.12	1.03	0.95	0.92 ± 0.06
<i>H_A</i>	0.74	0.23	0.14	0.97	0.88	1.02 ± 0.06
<i>H_B</i>	1.05	0.15	0.09	1.20	1.14	0.98 ± 0.06
<i>I_A</i>	1.30	0.09	0.05	1.39	1.35	1.37 ± 0.04
<i>I_B</i>	1.26	0.06	0.04	1.32	1.30	1.27 ± 0.04

there is something atypical about the coefficients in the cylindrical shell *H* can be seen from the distortion in the discrepancy map (see Table 13) in this shell. Also in Table 12 it can be seen that the measured coefficient in *H_A* is greater than that in *H_B*, whereas the calculated coefficient is significantly less. This seems to indicate that the simple homogeneous model assumed is inadequate for these regions.

Conclusions

Calculations of the Pu^{239} Doppler coefficient based on an extrapolation of the resonance parameters obtained from measurements of the Pu^{239} total cross

section made recently at Harwell result in smaller values than those calculated previously. The measurements showed the existence of some very broad resonances. A Pu^{239} Doppler coefficient smaller by an order of magnitude than the value obtained using a narrow fission width distribution has been obtained for a large dilute fast reactor, and negative values could be expected in cores with less absorption relative to the Pu^{239} absorption in the resonance region (for example, with a $\text{U}^{238}/\text{Pu}^{239}$ ratio of less than about 5). However, the α values obtained in this extrapolation are about 35% smaller than the usually accepted values, and so calculations of the Pu^{239} coefficient based on currently

available resonance data must be regarded as uncertain, even in sign.

The approximate methods used to treat the heterogeneity of the Zebra Doppler test zone, and its finite size, have been described. The unit cell is a complicated one; the simple treatment is expected to give the heterogeneity corrections only to an accuracy of about $\pm 50\%$. The heterogeneity correction to the Pu^{239} coefficient is a factor of about three for the plutonium pins when there are diluent pins present. Methods of allowing for the sample size of a small Doppler sample in a core which contains the same material as the sample are also described.

The Doppler coefficients calculated using the FD2 set and the narrow fission width resonance data for Pu^{239} give positive values for the Pu^{239} coefficients in the Zebra Doppler Loop, whereas the measured values are negative. The recent Pu^{239} data would lead to values smaller by a factor of 3 or 4 and probably to a negative value in core 5K. The U^{238} coefficients are in good agreement with the Doppler coefficients measured for the mixed U-Pu oxide pins when the Pu^{239} coefficients are taken to be zero.

Calculations of sodium reactivity worth have been made to compare with the measurements made in ZPR-VI Assembly 2. The calculations were for a homogeneous model, and used diffusion theory and first order perturbation theory.

The results suggest that the FD2 cross-section set overestimates the sum of the moderation, self-shielding and absorption components of the sodium coefficient in ZPR-VI Assembly 2, but there is no evidence of a systematic error in the calculation of the leakage component. Reducing the sum of moderation, self-shielding, and absorption components by 40% brings the calculated values into reasonable agreement with the measured values.

A possible explanation of the discrepancy between calculation and experiment may be the inadequacy of the homogeneous model. Measurements with ZPR-VI Assembly 2⁽²⁹⁾ and Assembly 3⁽³⁰⁾ indicate large heterogeneity effects. These could introduce the kind of constant discrepancy found. If this is so, a discrepancy of a similar magnitude would not be expected in an assembly with a different lattice structure.

Acknowledgment

The Authors would like to thank Mr. E. Begg for making available the results of his calculations of the Zebra Doppler Loop Doppler coefficients using the FD1 and FD2 cross-section sets and Miss Linda Grimshaw for help with the computations.

Addendum

Changes in the values of Tables 12 through 17, and in some of the conclusions, were made by the authors since the time of the Conference.

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Discussion

Mr. Greebler (GE): Did I understand you to say that the 25% and 40% adjustment of the two components was done empirically as a recipe to get better agreement between the measured and calculated results, or was it based on some specific factors in the calculation in the cross sections and so on?

Mr. Codd: It has no theoretical justification; on the other hand, the leakage terms are more important in the outer regions of the reactor, and that gives a good idea of the 25%. Likewise, at the center the moderation is relatively more important. They are not just guesses.

Mr. Peterson (Battelle-Northwest): I understood that you made a 15% correction to the one-dimensional diffusion calculations for the fact that the heated region was not completely filling the axial length. Could you explain how you arrived at the 15%?

Mr. Codd: I think all I can say there is that this was based on various two-dimensional studies in which one tried to simulate the geometry and compensation of the regions above the loop more accurately. That is all.

Mr. Segal (APDA): I would just like to ask if your calculations of the sodium worth in Fermi, also dominated by the leakage effect, have shown a similar discrepancy. I would like to remind you that when I did similar calculations I found a 25% discrepancy but in the opposite direction of the leakage term. I am of the opinion that the discrepancies are due to data principally and to hope for 25% in heterogeneity effects is perhaps too optimistic.

Mr. Codd: You referred to Fermi calculations. I don't think the authors would feel able to say very much about Fermi work at the present without the comparisons with Fermi data.

Mr. Nims (APDA): I noticed in your map of the calculated sodium worth that in the central axial

position the use of FD2 overestimated the worth by about 18%, which is inconsistent with the author's statement about underestimating by the order of 1%.

Mr. Codd: That correction factor of 40% refers only to the FD2 data set, and therefore the reduction in the moderation component would make that 18% come close to zero. One would require different corrections for the other data-set values where the sign of the error was opposite.

Mr. Chernick (BNL): You say Uttley and Company are serious about these low values of α for plutonium. Is this simply *ad hoc* for the moment?

Mr. Codd: I doubt if Dr. Uttley knows about these low values; our calculations are very preliminary.

Mr. Chernick: But he took a big leap forward in assuming that these resonances were all fission. Is that your understanding of what he has done about the broad resonances?

Mr. Codd: That is what he has done, I think, on the basis of Lynn's recent theory. The low α values are purely the responsibility of the authors, no one else.

Mr. Engelmann (Karlsruhe): In the conference here two years ago, there was a paper of Storrer on the effect of a hot sample in a cold surrounding. In your loop, of course, the center was very much larger; have you tried to calculate the correction due to the effect of the cold reactor and hot sample?

Mr. Codd: That is discussed in the paper. But in between the main reactor and the loop, there is an insulating zone, and the author suggests, in fact, that this essentially disconnects the two regions insofar as the resonance steps in the flux are concerned.

Mr. Engelmann: If this is the case, then the Doppler effect as you calculated it should not agree with the Doppler effect which you measured, because in your calculation, you assume that the reactor is homogeneous and at the same temperature. I got it that this surrounding layer between the loop and the core zone did, in the resonance region, decouple the cold reactor and heated center. Is this correct?

Mr. Codd: Yes.

Mr. Engelmann: Then in your calculation the Doppler effect is calculated for the whole reactor.

Mr. Codd: No, only for the heated loop region.

Mr. Pitterle (APDA): Just a comment on those α values which they had for this broad sequence. As I have mentioned before, I find that the α values are very sensitive to the ν values assumed for the fission distribution. If one neglects this, assuming certain sets of parameters, one very easily gets very low α values; but if one assumes a $\nu = 1$ distribution for the $j = 0$ sequence, for instance, this drops the average $\Gamma_f(\Gamma)/\Gamma$ term for the infinite-dilution cross section, and this

begins to raise the α value. I think it's significant as well for the $j = 0$ sequence where we had been assuming a ν of 5 or 10. And here again, it's surprising how much difference this makes in the α value.

Mr. Codd: Thank you. The authors in fact don't

assume a theoretical law for the fission width distribution. They group Uttley's parameters into six groups, each having a representative mean width, and assume that this can be extrapolated and that these groups apply to all energies.

Accuracy of Power Reactor Void Coefficients Extrapolated from Measurements in Critical Assemblies

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1. INTRODUCTION

It is generally agreed that we cannot undertake construction of a large, fast power reactor for commercial use until large-scale critical experiments have been conducted to confirm the reliability of crucial design calculations for such a reactor. Although complete and unambiguous information could be obtained if the critical assembly mocked up the reference power reactor design in every detail, a more flexible and less expensive approach is preferable. In this paper we consider how accurately the crucial parameters for nuclear design can be derived from what might be termed a "practical" critical assembly that may differ from the reference power reactor in such respects as size, temperature, and isotopic composition of plutonium. The parameter used to illustrate the approach, the coolant void coefficient, was selected because of its great importance and particular sensitivity to these differences. (A similar approach could be taken for other important nuclear parameters, such as the Doppler coefficient and the breeding gain.)

The reference power reactor is the MARK-XV design for a 1000-MWe supercritical, steam-cooled, fast breeder reactor (SCBR).⁽¹⁾ The annular core is 18.8 in. thick by 62.5 in. high, and has a volume of approximately 4560 liters; the annulus is blanketed on the inside, outside, and at the top end. The fuel is a mixture of plutonium and depleted uranium oxide in nickel-based alloy tubes approximately $\frac{1}{4}$ in. in diameter, and the coolant is supercritical steam. Under operating conditions, the average fuel temperature is 2485°F, and the average steam density is 6.6 lb/ft³ (3500 psi, 850°F). At the beginning of core life, the isotopic composition of plutonium is an "equilibrium mixture" that contains 68 % Pu-239, 28 % Pu-240, and 4 % Pu-241. The atomic ratio of fissionable isotopes (Pu-239 + Pu-241) to total plutonium plus uranium is approximately 0.16, and the critical mass is approximately 2700 kg plutonium. Core volume fractions are 43 % fuel, 40 % coolant, 8 % cladding, 5 % structure, and 4 % B₄C (enriched) control rods.

It is apparent from this description of the reference power reactor that a critical assembly would be considerably simplified if absolute duplication of the reference reactor were not required. Relaxation of the design requirements in at least four major areas is worthy of consideration:

1. *Size*: Decrease the size to reduce the cost of materials, facility, and containment.
2. *Temperature*: Eliminate the high cost and complexity of a hot critical facility.
3. *Plutonium composition*: Utilize available plutonium, which is low in Pu-240 content.
4. *Heterogeneity*: Consider using cheaper fuel forms.

The following sections evaluate the effects of these four variables on the void coefficient and on the correction required for its extrapolation from that measured in simpler critical assemblies to the value for the reference power reactor.

2. TECHNICAL APPROACH

The void coefficient Δk_v is defined by the equation $\Delta k_v \equiv k_0 - k_{100}$, where k_{100} is the value of k_{eff} with the supercritical steam coolant present under normal operating conditions, and k_0 is the equivalent value when the coolant is completely removed from the core and blanket. The void coefficient for the reference power reactor (REF) can then be written

$$\begin{aligned}(k_0 - k_{100})^{\text{REF}} &= (k_0 - k_{100})^{\text{CA}} + (k_{100}^{\text{CA}} - k_{100}^{\text{REF}}) \\ &\quad - (k_0^{\text{CA}} - k_0^{\text{REF}}) \\ &= (k_0 - k_{100})^{\text{CA}} + \text{CF}_{\text{total}},\end{aligned}\tag{2-1}$$

where the superscript CA refers to the critical assembly.

We assume that the void coefficient is measured directly in the critical assembly, and we are not concerned here with the details of this measurement or its accuracy. To obtain the desired void coefficient for the reference power reactor, a correction factor (CF) must be added to the measured value to account for the differences between the critical assembly and the reference design (such as size, temperature, and composi-

tion). The correction factor (which is zero for the trivial case in which the critical assembly is identical with the reference reactor) must be derived from auxiliary experiments and/or theoretical calculations. However,

the virtue of the stratagem expressed in eq. 2-1 is that the term most difficult to compute (because it involves a large spectrum change) is measured directly, whereas each term in the correction factor involves much smaller

FIG. 1. Neutron Spectrum in the Reference Power Reactor.

spectral changes and should be computable with less error.

This point is illustrated in Figures 1 and 2. The neutron spectrum at the center of the reference power reactor at room temperature is given in Fig. 1; the spectrum when readily available plutonium (5 % Pu-240) is substituted for "equilibrium" plutonium (28 % Pu-240) is shown for comparison. The spectral differences, given as flux ratios in Fig. 2, show the difficulty of computing the void coefficient directly, which is caused by the large spectral change on voiding (median fission energy in-

creases from about 50 keV to 100 keV). On the other hand, in this example, the spectral differences caused by a change in the isotopic composition of plutonium are much smaller, so the correction factor is easier to compute. Similar conclusions are reached when spectral changes caused by critical-assembly size or temperature are compared.

In the following analysis, the magnitude of the correction factor is obtained by comparing calculated values of $(k_0 - k_{100})^{\text{REL}}$ and $(k_0 - k_{100})^{\text{CA}}$, and the accuracy with which we can determine this correction factor is

FIG. 2. Effect of Coolant Density and Pu-240 Content on Spectrum.

estimated in various ways. The results represent the uncertainty in the void coefficient of the reference power reactor when it is extrapolated from measurements in a simpler critical assembly.

With few exceptions (noted in the text), we used the general analytical method given by the WANDA code to obtain k_{eff} and the four-group fluxes from numerical solutions of the one-dimensional, few-group neutron-diffusion equations. Four-group, flux-weighted coefficients were generated by means of the RIP, MUFT-5, and KATE-1 codes in combination with the 68- or 34-group B&W standard cross-section library. RIP calculates the resonance integral of those elements in which the cross sections are shielded. Shielding and Doppler broadening of the neutron resonances were computed by use of the simple equivalence relations that lead to the NR or NRIA approximations. This corresponds to the use of the Adler-Nordheim method (e.g., GAM-1), except that Wigner's rational approximation of the escape probability is introduced, yielding a single expression that incorporates both the volume- and surface-absorption terms. The Dancoff factor was calculated by Sauer's method. MUFT-5 employs a Fourier transform to calculate the flux-weighted, few-group constants for the three fast groups in the P1 diffusion approximation. The thermal-neutron spectrum was computed according to the Wigner-Wilkins theory, and average thermal group constants were obtained from the KATE-1 sub-routine.

When the four-group representation is used, the energy bounds are

Group 1: 10 MeV to 0.3 MeV

Group 2: 0.3 MeV to 5 keV

Group 3: 5 keV to 0.4 eV

Group 4: 0.4 eV to 0.

TABLE 1. EFFECT OF SHAPE AND SIZE
ON THE VOID COEFFICIENT

Critical Assembly Size	Fuel Temperature, °C	Pu-240 Concentration, %	$k_0 - k_{100}$	Correction Factor CF_{size}
Annulus	20	28	-0.0373	
$\infty/1$ Slab	20	28	-0.0366	<0.001
5/1 Slab	20	28	-0.0379	<0.001
3/1 Slab	20	28	-0.0404	+0.003
1/1 Slab	20	28	-0.0524	+0.015
Annulus	20	5	-0.0638	-
5/1 Slab	20	5	-0.0634	<0.001
Annulus	1363	28	-0.0157	-
5/1 Slab	1363	28	-0.0170	+0.001
Annulus	1363	5	-0.0405	-
5/1 Slab	1363	5	-0.0411	<0.001

Although more sophisticated analytical techniques are available (and have been used to obtain a better absolute value of the void coefficient for reactor design studies), we are concerned here with *changes* in the void coefficient rather than with absolute values, so the simplified treatment should be adequate for error analysis.

3. EFFECT OF SHAPE AND SIZE

The reduction of material costs, particularly for plutonium fuel, is the major incentive for decreasing the size of the critical assembly. Equipment and containment costs would also be substantially reduced by decreased plutonium inventory requirements.

A reasonable choice for the shape of the critical assembly is a slab that matches, in thickness, the annular core of the reference reactor. This shape is preferable to others (e.g., cylindrical) because it closely reproduces the reference spatial variation of the neutron flux and spectrum in the transverse direction. Since the core of the reference reactor is so thin in one dimension that asymptotic conditions exist in only about half of the core volume, the nonasymptotic region is of major importance and should be accurately simulated in the critical assembly. The slab core is provided with blankets against both faces, which are identical to the outer blanket of the reference reactor, and with nickel reflectors against the short sides. In order to avoid complex corrections for differences in axial leakage, the dimensions and composition in the axial direction are identical to those of the reference reactor.

Table 1 shows calculations for the reference reactor shape and size (annular core) and for slabs varying in breadth (expressed as the ratio of breadth to thickness); in each case the concentration of plutonium is adjusted slightly to normalize to $k_0 = 1$. The shape and size effect may be expressed analytically in the manner of eq. 2-1:

$$\begin{aligned}
 (k_0 - k_{100})^{\text{ann}} &= (k_0 - k_{100})^{\text{slab}} (k_{100}^{\text{slab}} - k_{100}^{\text{ann}}) \\
 &\quad - (k_0^{\text{slab}} - k_0^{\text{ann}}) \quad (3-1) \\
 &= (k_0 - k_{100})^{\text{slab}} + CF_{\text{size}}.
 \end{aligned}$$

As expected, leakage increases as the slab size decreases, making the void coefficient more negative. However, the change is small, and the void coefficient for the reference reactor shape and size should be attainable with good accuracy from measurements with 5/1 or 3/1 slabs.

The last three sets of calculations in Table 1 show the effect of fuel temperature and isotopic composition of plutonium on the size correction. The void coefficient itself changes appreciably, so these effects must be carefully considered (see below). However, the difference between the void coefficient for the reference reactor annulus and that for the slab remains small, indicating

that the size correction is quite insensitive to the fuel temperature and isotopic composition in the range considered.

The reliability of these one-dimensional calculations was assessed by re-doing an extreme case (3/1 slab) in two dimensions using the PDQ code. The resulting void coefficient was within 0.001 of the one-dimensional case.

Although the correction factor for size is very small if the void coefficient is measured, for example, in a 5/1 slab critical assembly, it does not necessarily follow that the correction factor can be calculated with that accuracy. For example, if k_{∞}^* is defined as the ratio of productions to absorptions in the center of the core, and the nonleakage probability P_{NL}^* is defined as the ratio of k_{eff} to k_{∞}^* , the difference in these parameters between the reference annulus and the 5/1 slab is approximately 3%. The change in the void coefficient is very small because the changes in k_{∞}^* and P_{NL}^* are in opposite directions and strongly cancel. This type of procedure is not useful for error analysis, however, because k_{∞} and P_{NL} are not strictly separable in these high-leakage cores.

The approach used here is to calculate the sensitivity of CF_{size} to changes in cross sections in the manner described more fully below (for the cross-section changes listed in Table 8, the changes in CF_{size} are generally much smaller than the reported values for CF_{comp} and are usually of the opposite sign). The only larger effects result from parameters that particularly influence leakage (specifically, inelastic scattering in U-238 and elastic scattering in oxygen), and in these cases the CF_{size} uncertainty is only about 0.001.

In summary, the uncertainty in the reference reactor void coefficient (accounting for differences in shape and size) is approximately ± 0.001 if the void coefficient is measured in a 5/1 slab critical assembly. Further, the accuracy could possibly be improved, making the case for a 3/1 slab critical assembly more attractive, by effecting some separation of k_{∞} and P_{NL} in supplementary experiments. Such experiments, involving small changes in the slab dimensions compensated by changes in the hydrogen density, may permit a more accurate correction for the small differences in k_{∞} and P_{NL} between the slab critical assembly and the reference annular core.

4. EFFECT OF TEMPERATURE

The critical experiments would be much simpler if they were performed at room temperature, thereby avoiding the complexity and cost associated with the type of hot critical facility required to match the operating temperature of the reference reactor. As would be expected, any parameter (such as temperature or pressure) that changes the coolant density will have a large

TABLE 2. EFFECT OF FUEL TEMPERATURE ON THE VOID COEFFICIENT

Critical Assembly Size	Pu-240 Conc, %	Fuel Temp, °C	$k_0 - k_{100}$	Correction Factor, CF_{temp}	$k_{100}^{cold} - k_{100}^{hot}$	$k_0^{cold} - k_0^{hot}$
Annulus	28	20	-0.0354	+0.021	+0.026	+0.005
Annulus	28	1363	-0.0148			
5/1 Slab	28	20	-0.0380	+0.020	+0.024	+0.004
5/1 Slab	28	1363	-0.0182			
5/1 Slab	5	20	-0.0639	+0.021	+0.026	+0.005
5/1 Slab	5	1363	-0.0427			

effect on the void coefficient. However, the supercritical steam can be simulated in room-temperature experiments simply by using expanded plastic materials whose hydrogen density is the same as that in the reference power reactor at operating temperature and pressure.

The effect of the fuel temperature on the void coefficient is given in Table 2; expressed analytically,

$$\begin{aligned}
 (k_0 - k_{100})^{hot} &= (k_0 - k_{100})^{cold} \\
 &\quad + (k_{100}^{cold} - k_{100}^{hot}) \\
 &\quad - (k_0^{cold} - k_0^{hot}) \\
 &= (k_0 - k_{100})^{cold} + CF_{temp}.
 \end{aligned} \tag{4-1}$$

In calculating the reactivity change from cold to hot, k_0 was not renormalized to unity, so these void coefficients differ from those given in Table 1. The results indicate that the correction factor to be applied to room-temperature measurements of the void coefficient is (at least to the first order) relatively insensitive to the shape and size of the critical assembly and to its plutonium isotopic composition.

The temperature correction is so large that it is doubtful that it can be calculated accurately enough from basic data. For example, estimates of the uncertainty in fast reactor Doppler calculations range from $\pm 20\%$ to higher values, depending on the core composition and initial assumptions. Even if the optimistic value is assumed, the resulting uncertainty in the temperature correction (± 0.005) is substantial.

Although we can expect improvements in the calculation of Doppler coefficients, for the purposes of this study, it is probably more realistic to assume that the Doppler coefficient is measured in the critical assembly, rather than to rely on calculations alone for the temperature correction. The magnitude of the temperature defect in the SCBR is shown in the last two columns of Table 2; it is five times larger under operating conditions than when the reactor is voided because of the much larger component of low-energy flux (see Fig. 1). The

corresponding mean core Doppler coefficients are $19 \times 10^{-6} \Delta k/k^\circ\text{C}$ and $3.7 \times 10^{-6} \Delta k/k^\circ\text{C}$.

Doppler measurements can be made in critical assemblies by several techniques (e.g., see G. J. Fischer⁽²⁾) to an accuracy of perhaps ± 10 to 20% . Furthermore, it seems reasonable to expect that the accuracy can and soon will be improved to ± 5 to 10% as a result of current investigations of systematic errors (expansion effects, influence of heated-region size on incident spectrum, perturbation by heater), and improvements in experimental techniques (suppression of reactor drift, increased sensitivity, extension to higher temperature). If such measurements are made in the critical assembly, as a function of position so that the spatial variation of the neutron spectrum is properly weighted, and with and without coolant present, we should be able to obtain CF_{temp} to an accuracy of ± 0.0013 to ± 0.0026 .

For reasons discussed in the next section, it is preferable to fuel the critical assembly with readily available plutonium (5% Pu-240) rather than with "equilibrium" plutonium (28% Pu-240). Although the calculations indicate that the temperature correction is approximately the same (within 0.001) for both types of fuel, such calculations are not very reliable for the three-component systems under consideration (U-238, Pu-239, Pu-240) because of large uncertainties in resonance cross sections, resonance interference, and overlap effects. Therefore, if the critical assembly is fueled with plutonium that contains 5% Pu-240, the Doppler measurements should be made with both 5% Pu-240 and 28% Pu-240 fuel samples to provide supplementary information on the Pu-240 contribution and its interactions with the U-238 and Pu-239.

The neutron spectra in assemblies fueled with 5% and 28% Pu-240 material differ by less than 10% above about 100 eV (and by no more than 20% at lower energies); the changes with temperature in the two spectra agree within 2% at all energies. Hence the correction for spectral differences is small and within an energy range where the cross sections are well known, and the

Pu-239 and Pu-240 contributions should be small fractions of the U-238 contributions. Therefore, if the Doppler calculations are normalized to the experimental results in a critical assembly fueled with plutonium that contains 5% Pu-240, we should be able to extrapolate to the "equilibrium" plutonium case with good accuracy.

5. EFFECT OF FUEL COMPOSITION

The fuel for the reference power reactor is an "equilibrium" plutonium mixture that contains 28% Pu-240. It is probably unrealistic to assume that the required quantity of high-Pu-240 material (500 to 1000 kg) is readily available for the critical experiments. The use of Pu-240 also imposes some practical (although surmountable) difficulties in such areas as health physics, neutron background, and heat generation. Therefore, we must consider whether these experiments will provide useful results if they are fueled with readily available, low-Pu-240 plutonium; we must carefully estimate the accuracy with which the void coefficient measured in such a critical assembly can be corrected for the difference in Pu-240 concentration.

An alternative solution to this problem may be mentioned in passing: to simulate the Pu-240 with a more commonly available material, such as indium. Indium roughly approximates the capture cross section of Pu-240 over the entire energy range, within about $\pm 50\%$; its fission cross section is zero. Calculations indicate that the equilibrium plutonium neutron spectrum can be matched fairly well by adding indium to pure Pu-239; the resulting void coefficients are the same, but it is difficult to specify the exact indium concentration because of large uncertainties in the Pu-240 and indium cross sections.

Pu-240 self-shields, and some credit can be taken for the tendency toward saturation. However, the Pu-240 concentration must be at least 20% to obtain a resonance integral that is within 10% of the value for the 28% reference concentration (1050 b). In order to obtain a conservative limit, we assume in this analysis that the plutonium contains only 5% Pu-240; this material should be available in reasonable quantities.

Using the same technical approach as before,

$$\begin{aligned} (k - k_{100})^{28\%} &= (k_0 - k_{100})^{5\%} \\ &\quad + (k_{100}^{5\%} - k_{100}^{28\%}) \\ &\quad - (k_0^{5\%} - k_0^{28\%}) \\ &= (k_0 - k_{100})^{5\%} + CF_{\text{comp}}, \end{aligned} \quad (5-1)$$

where the superscripts refer to the Pu-240 isotopic concentration. The results, summarized in Table 3, show that the correction (for plutonium isotopic composition)

TABLE 3. EFFECT OF FUEL COMPOSITION ON THE VOID COEFFICIENT

Critical Assembly Size	Fuel Temperature, $^\circ\text{C}$	P-240 Concentration, %	$k_0 - k_{100}$	Correction Factor CF_{comp}
5/1 Slab	20	28	-0.0379	+0.026
5/1 Slab	20	5	-0.0634	
Annulus	20	28	-0.0373	+0.026
Annulus	20	5	-0.0638	
Annulus	1363	28	-0.0157	+0.025
Annulus	1363	5	-0.0405	

to the void coefficient measured in a critical assembly fueled with 5% Pu-240 plutonium is independent (≤ 0.001) of critical assembly shape, size, or temperature. However, the void coefficient itself is strongly dependent on the plutonium isotopic composition; hence the source of this dependency must be carefully considered in order to estimate the accuracy with which this large correction factor can be determined.

5.1 Effect of Pu-240

Table 4 lists the fraction of Pu-240 captures and fissions in a room-temperature, 5/1 slab critical assembly fueled with plutonium that contains 5 or 28% Pu-240; the energy distribution of the Pu-240 captures for these cases is plotted in Figure 3. Fortunately, a large fraction of the captures occurs at low energies where the cross section is known quite accurately; under operating conditions, with 5% Pu-240 fuel, roughly $\frac{1}{3}$ of the captures occur in the large resonance at 1.05 eV, $\frac{1}{3}$ occur in the ten resolved resonances up to 119 eV, and only $\frac{1}{3}$ occur at higher energies. Since the cross section is known much more accurately below 167 eV than above, this energy is a convenient break point in evaluating the uncertainty in the correction factor for Pu-240 concentration.

5.1.1. Analysis below 167 eV

Using as extreme limits the uncertainties in the resolved resonance parameters reported in BNL-325, the correction factor in eq. 5-1 was recomputed by the standard 34-group method. The difference between the two calculations represents a conservative estimate of the uncertainty in the correction factor. A similar calculation was performed for the smooth (nonresonant) part of the capture cross section between 0.4 and 167 eV, assuming a $\pm 10\%$ uncertainty in this value, which is consistent with available data. The resulting contributions to the uncertainty in the correction factor, summarized in Table 5, are quite small.

We must also consider the error that may be contributed by the theoretical model used to compute the self-shielding in these resonances. As a guide, two other resonance-integral programs were compared with the RIP program used in the standard calculation; in all three models isolated resonances were assumed. RIP uses the Wigner rational approximation for the escape probability and the equivalence theorem to form an expression for the resonance integral; the use of the NR or NRIA approximation for each resonance must be specified.⁽³⁾ The Cohen-Goldstein intermediate absorption theory is used for ERIC-2.⁽⁴⁾ It is an improvement over RIP in that it does not require a choice between NR and NRIA. GANIT utilizes the Nordheim integral treatment to obtain a numerical solution to the two-region, infinite-medium, slowing-down equations with an asym-

TABLE 4. NEUTRON BALANCE IN CRITICAL ASSEMBLY

Process	5% Pu-240		28% Pu-240	
	0	100	0	100
Pu-240 Captures	0.0035	0.0154	0.0232	0.0598
Pu-240 Fissions	0.0033	0.0027	0.0223	0.0183
Total Captures	0.2867	0.3662	0.2914	0.3813
Total Fissions	0.3260	0.3591	0.3239	0.3484
Total Leakage	0.3873	0.2747	0.3847	0.2703
Total	1.0000	1.0000	1.0000	1.0000

metric Doppler-broadened Breit-Wigner resonance shape;⁽⁵⁾ thus a rigorous expression for the integral is formed. GANIT is an improved and faster version of ZUT. The results for the case with coolant present are compared in Table 6 for several microgroups in the 34-group scheme. It is evident that the effect of a model change is no greater than that caused by uncertainties in the resonance parameters themselves, with a slight exception for the case of the 1.05-eV resonance. The contribution to the uncertainty in the correction factor (see Table 5) is still negligible.

The remaining uncertainty in the region below 167 eV is that due to the calculation of the incident neutron spectrum. To avoid the complex task of estimating this error analytically, it is assumed that the spectrum will be measured by resonance foil-activation techniques which are useful in this energy range. If the calculated spectrum is forced to fit the measured value within $\pm 10\%$, the contribution to the uncertainty in the correction factor is very small (see Table 5).

5.1.2. Analysis above 167 eV

Unfortunately, the differential capture cross section of Pu-240 has not been measured above 167 eV. In many cross-section libraries, including that at B&W, it is assumed that the capture cross section is 1.75 times that of U-238; this ratio has been derived from statistical considerations.⁽⁶⁾ At Argonne National Laboratory⁽⁷⁾ a ratio of 1.4 above 25 keV is used and the cross section computed at lower energies, using an s-wave strength function of 2.0×10^{-4} , and Γ_γ and $\bar{D}$ in the resolved range from BNL-325. To provide another point for comparison, ERIC-2 was run to compute microgroup cross sections from 167 eV to 15.03 keV, using the same inputs. The new cross sections were 38% lower in the lowest microgroup and 18% higher in the highest, varying smoothly over this range. However, it is impossible to make a realistic estimate of the uncertainty in the Pu-240 capture cross section without experimental evidence. As an illustration of the seriousness of this deficiency, the uncertainty in the correction to the void

FIG. 3. Energy Distribution of Pu-240 Captures.

coefficient, assuming a 50% uncertainty in the capture cross section of Pu-240 above 167 eV, is approximately ± 0.0055 .

Therefore, it is worth considering at this point what additional data we can obtain from these critical experiments (beyond the void coefficient) to reduce the dependency of the correction factor for plutonium isotopic composition on Pu-240 cross-sectional uncertainties.

The details of these auxiliary experiments are discussed in the next section, but the conclusions are pre-

sented here. The following approach is suggested: (1) measure the total Pu-240 capture integral in the critical assembly by means of reactivity techniques; (2) calculate the contribution from 0.4 to 167 eV; (3) derive the integral above 167 eV by difference. Table 7 shows the results for the two types of fuel and their respective spectra. The uncertainty in the calculated integral to 167 eV was obtained by weighting the individual sources of error (discussed previously) by the number of captures. The uncertainty in the measured total integral

TABLE 5. UNCERTAINTY IN VOID COEFFICIENT
CORRECTION DUE TO Pu 240

Source	Uncertainty, %	Uncertainty in		
		$k_{100}^{5\%} - k_{100}^{28\%}$	$k_0^{5\%} - k_0^{28\%}$	CF_{comp}
Captures below 167 eV				
1.05-eV Resonance	5	0.0001	0.0000	0.0001
Next 10 Resonances	17	0.0008	0.0000	0.0008
Smooth Capture	10	0.0007	0.0000	0.0007
Shielding Model	17	0.0007	0.0000	0.0007
Spectrum Model	10	0.0011	0.0000	0.0011
Captures above 167 eV				
With Single-zone Expt	35	0.0098	0.0059	0.0039
With Two-zone Expt	17	0.0047	0.0029	0.0018
Measurement of $\bar{\sigma}_f$	9	0.0024	0.0027	-0.0003
Measurement of $\bar{\nu}$	7	0.0031	0.0036	-0.0005
Total Uncertainty				
With Single-zone Expt	—	—	—	0.0043
With Two-zone Expt	—	—	—	0.0025

includes correction for Pu-240 fissions and for captures and fissions in the accompanying Pu-239. The uncertainty in the derived integral above 167 eV assumes that the other two errors are random.

If the total capture integral is measured in a critical assembly fueled with plutonium that contains 5% Pu-240, the integral above 167 eV can be derived to about

$\pm 3.5\%$. If more accurate results are required, a two-zone experiment can be performed. In the two-zone experiment, the critical assembly contains a small central zone fueled with 28% Pu-240 material and large enough to achieve a neutron spectrum characteristic of a large single-zone assembly fueled entirely with 28% Pu-240 plutonium. If the total capture integral is measured in this type of experiment, the integral above 167 eV can be derived much more accurately, because the spectrum is harder, and a much larger fraction of the measured total capture occurs in the energy range of interest (> 167 eV).

TABLE 7. Pu-240 CAPTURE INTEGRALS

Identification	Total Captures		Uncertainty ($\pm\%$)	
	5% Pu-240	28% Pu-240	5% Pu-240	28% Pu-240
1.05-eV Resonance	0.0028	0.0020	—	—
Next 10 Resolved Resonances	0.0033	0.0083	—	—
Smooth Capture, 0.4 167 eV	0.0039	0.0147	—	—
$\int_{0.4}^{167} \sigma_c(E)\phi(E)dE$ - Calculated	0.0100	0.0250	16	15
$\int_{0.4}^{107} \sigma_c(E)\phi(E)dE$ - Measured	0.0154	0.0598	7	8
$\int_{167}^{107} \sigma_c(E)\phi(E)dE$ - Derived	0.0054	0.0348	35	17

TABLE 6. COMPARISON OF LOW-ENERGY Pu-240 RESONANCE-INTEGRAL CALCULATIONS

Microgroup	Low-energy Limit, eV	Group Resonance Integral			Group Capture Cross Section			
		RIP	ERIC-2	GANIT	RIP	RIP ^(a)	ERIC-2	GANIT
Plutonium Containing 5% Pu-240								
33	0.638	464.02	484.86	(b)	1208	1249	1250	(b)
27	13.71	7.17	7.26	7.44	29	31	30	30
25	37.27	8.21	8.80	9.12	26	28	27	28
24	61.44	9.25	10.28	10.76	19	22	21	22
23	101.3	4.92	5.48	5.79	19	20	20	21
Plutonium Containing 28% Pu-240								
33	0.638	1195.0	1274.0	(b)	2671	2775	2829	(b)
27	13.71	15.6	15.7	15.9	46	49	46	47
25	37.27	19.4	20.0	20.5	48	53	49	51
24	61.44	22.1	23.0	23.7	44	54	46	47
23	101.3	12.2	12.5	12.9	33	38	34	35

(a) RIP using extreme values of resonance parameters.

(b) Not run; should fall between RIP and RIP^(a).

No additional error is encountered in deriving the effective cross section from these integral values, because the neutron spectrum above 167 eV is the same (within the accuracy of the integral value) for all cases appearing in the correction factor (see Fig. 2). Of course, measurements should be made with both 5 and 28 % Pu-240 samples, because the resonance shielding differs somewhat in this energy range. Using eq. 5-1, the uncertainty in the correction to the void coefficient due to uncertainties in the Pu-240 capture cross section above 167 eV was computed; the results are included in the summary in Table 5.

5.1.3. Auxiliary Experiments

In addition to the void coefficient itself, other measurements may be taken in the critical assembly to provide supplementary information on the Pu-240 cross sections and to reduce the uncertainty in the void coefficient correction for Pu-240 concentration.

The total fission integral of Pu-240 can be measured relative to Pu-239 (to about $\pm 2\%$) by activation or counting techniques, e.g., Kirn.⁽⁸⁾ Pu-239 is the preferred normalizer since its fission cross section varies less with energy than does that of U-233 or U-235. If we use a conservative estimate of the uncertainty in computing $\bar{\sigma}_f^{239}$, the total fission integral

$$\int_0^\infty \sigma_f^{240}(E)\phi(E) dE$$

can be obtained to an accuracy of about $\pm 9\%$ from measurements in the critical assembly under voided and supercritical steam conditions. It is not necessary to subdivide the fission integral into discrete energy ranges, nor to make the measurement in a spectrum characteristic of 28 % Pu-240 fuel, because the fission integral differs by less than 1 % in the two spectra. As summarized in Table 5, the effect of a 9 % uncertainty in $\bar{\sigma}_f^{240}$ on the correction to the void coefficient is negligible.

Data on the total capture integral and $\bar{v}$ can be derived from reactivity measurements in the critical assembly, e.g., Ref. 9, which are proportional to $(\bar{v} - 1)\bar{\sigma}_f - \bar{\sigma}_c^*$, where $\bar{\sigma}_c^*$ includes a small scattering correction that can be obtained experimentally. If such measurements are made in the harder, spectrum-voided critical assembly, to emphasize $(\bar{v} - 1)\bar{\sigma}_f$ over $\bar{\sigma}_c^*$, $\bar{v}^{240}$ can be obtained to about ± 0.2 using the previously determined value of $\bar{\sigma}_f^{240}$ and a very conservative estimate of $\bar{\sigma}_c^{240}$. As in the case of $\bar{\sigma}_f^{240}$, $\bar{v}^{240}$ is essentially the same in a spectrum characteristic of 28 % Pu-240 fuel, and (as summarized in Table 5) this uncertainty has a negligible effect on the correction to the void coefficient.

Information on $\bar{\sigma}_c^{240}$ can be obtained from the same type of reactivity measurement if the spectrum is softer to allow captures to predominate over fissions. Whereas

the reactivity is about 2.5 times more sensitive to $(\bar{v} - 1)\bar{\sigma}_f$ under voided conditions, it is about 2.5 times more sensitive to $\bar{\sigma}_c$ when the supercritical steam is present. By performing precise reactivity comparisons with plutonium samples that contain varying concentrations of Pu-240, the total capture integral

$$\int_0^\infty \sigma_c^{240}(E)\phi_{100}(E) dE$$

may be obtained to an accuracy of about $\pm 7\%$. The following sources of error were considered in making this estimate: (1) accuracy of reactivity measurements; (2) corrections for fissions and captures in Pu-239; (3) correction for fissions in Pu-240 as obtained from the above measurements in the voided spectrum; (4) calculation of the Pu-239 normalization.

As stated previously, the capture integral above 167 eV can be much more accurately derived if the reactivity measurements are made in a spectrum characteristic of 28 % Pu-240 fuel. Because of the large quantity of this material required to fuel the entire critical assembly, and the resulting cost and availability problems, the two-zone approach may be considered. With plutonium that contains 5 % Pu-240 in the outer zone and 28 % Pu-240 in the inner zone, calculations indicate that the inner zone needs a radius of only about 16 cm for the neutron spectrum to be asymptotic (to within $\pm 2\%$) over a 2.54-cm radius. The total quantity of the 28 % Pu-240 plutonium is only 20 kg, a very substantial reduction from the 750 to 1150 kg that would be required to fuel the entire critical assembly. The adjoint-spectrum match has not yet been examined, so the actual required quantity of 28 % Pu-240 material may be somewhat larger.

5.1.4. Summary of Pu-240 Corrections

The corrections to the void coefficient for the difference in Pu-240 concentration between a critical assembly that uses 5 % Pu-240 fuel and the reference power reactor, which contains 28 % Pu-240 fuel, are collected and summarized in Table 5.

5.2 Effect of Other Cross Sections

Because of the large magnitude of the correction factor for the difference in fuel composition, it is necessary to consider how much error is introduced in this calculation by uncertainties in other nuclear cross sections and parameters. Recalling eq. 5-1, the sensitivity of the correction factor to changes in cross sections is given by the net change in four criticality calculations:

$$CF_{\text{comp}} = (k_{100}^{5\%} - k_{100}^{28\%}) - (k_0^{5\%} - k_0^{28\%}). \quad (5-2)$$

Therefore, although there may be a relatively large un-

certainty in computing any one value of k_{eff} , we would expect a strong cancellation of errors in computing differences in k_{eff} and much less uncertainty in the final value of CF_{comp} .

The uncertainties to be applied to the cross sections could be obtained from an evaluation of experimental microscopic cross-section data. However, one of the primary purposes of this (or any other) critical experiment program is to obtain integral data that can be used to improve the reliability of the set cross section. It is obvious, then, that the proper assessment of the error in the calculated correction factor requires some prior assumption as to the eventual accuracy of the integral data; it is equally obvious that the degree of success will not be known until the experiments are completed and their results are analyzed. However, in order to proceed at this time, the following assumption will be made: k_{eff} will be measured in the critical experiments as a function of hydrogen density, from void to supercritical steam density, and these data will be matched to within $\pm 2\%$ over the entire range by plausible adjustments in the cross sections. The calculations could be forced to fit the measurements exactly by arbitrary changes in the cross sections, but this does not rule out the possibility that errors may then be revealed in the calculation of the correction factor.

Table 8 shows the results of a four-macrogroup condensation that has the energy limits stated in Section 2. The fourth column represents the change in a particular cross section required to produce a 2% change in either k_0 or k_{100} , and the last three columns

are the changes that occur in the correction factor (eq. 5-2) when the cross section is increased by that amount. Group 4 is not shown because in all cases the changes required in thermal cross sections to cause a 2% change in k_{eff} are greater than 200%, which is clearly outside reasonable bounds for experimental errors. The change in the capture cross sections for the cladding materials is the difference between the B&W and ANL and ORNL libraries, and is principally in group 2. The change in the inelastic scattering matrix represents the difference between the Westinghouse MUFT-5 library and GAM-1, which is used in the B&W library. As expected, the uncertainties in the correction factor are fairly small, even though k_{eff} itself changes by 2%, because the spectral and compositional differences between pairs of terms are small.

The reason for deriving the uncertainty in the correction factor separately for each isotope, process, and energy range on the assumption that all of the uncertainty caused by a 2% mismatch in k_{eff} is concentrated there rather than randomly, is to avoid the problem of deciding how to compound individual errors. From this point of view, the maximum uncertainty in CF_{comp} is the largest number in the last column of Table 8, i.e., 0.004. Those cases for which the change in CF_{comp} is small, and those requiring an unrealistically large cross-section change can be disregarded. Of the remaining cases, no combination of compensating simultaneous cross-section changes could be found that changed CF_{comp} by more than 0.004, subject to the constraint that k_0 or k_{100} did not change by more than 2%.

TABLE 8. UNCERTAINTY IN VOID COEFFICIENT CORRECTION DUE TO OTHER CROSS SECTIONS

Isotope	Process	Macrogroup	Change, %	Change in		
				$k_{100}^{5\%} - k_{100}^{28\%}$	$k_0^{5\%} - k_0^{28\%}$	CF_{comp}
Pu-239	Fission	1	17	0.0025	0.0033	-0.0008
	Fission	2	16	0.0025	0.0051	-0.0026
	Fission	3	11	0.0039	0.0006	+0.0033
Pu-239	Capture	1	531	0.0035	0.0043	+0.0008
	Capture	2	66	0.0035	0.0054	+0.0019
	Capture	3	18	0.0046	0.0006	-0.0040
U-238	Fission	1	36	0.0003	0.0009	-0.0006
U-238	Capture	1	70	0.0015	0.0014	-0.0001
	Capture	2	18	0.0016	0.0021	+0.0005
	Capture	3	18	0.0025	0.0003	-0.0022
Fe, Cr, Ni	Capture	1-3	~90	0.0019	0.0013	+0.0006
U-238	Inelastic	1 $\rightarrow$ 2	56	0.0036	0.0030	+0.0006
Fe-56	Inelastic	1 $\rightarrow$ 2	220	0.0034	0.0031	+0.0003
U-238	Inelastic Scattering Matrix			0.0007	0.0008	-0.0001
O-16	Elastic	1	124	0.0053	0.0048	+0.0005
	Elastic	2	118	0.0027	0.0021	+0.0006
	Elastic	3	290	0.0001	0.0007	-0.0006
—	$\nu = a + bE$	a	5	0.0065	0.0052	+0.0013
		b	153	0.0029	0.0036	-0.0007

6. EFFECT OF HETEROGENEITY

Although the local heterogeneities of the reference power reactor could be reproduced in exact detail in the critical assembly, the experiments would be simplified if some freedom could be allowed in this area without seriously compromising the accuracy of the void coefficient.

The use of expanded plastics to simulate the low hydrogen density of supercritical steam at operating conditions introduces foreign elements, e.g., carbon, and some mismatch in the O/H ratio. On the basis of comparative calculations between supercritical steam and expanded polyethylene, in which the hydrogen density was matched exactly, k_{100} changed only by 0.0008; therefore, the effect on the correction factor to the void coefficient should be entirely negligible. Differences in hydrogen bonding are negligible in these systems.

Because of the particular sensitivity of the SCBR to resonance effects, we prefer to use a fuel form in the critical assembly that approximates, at least to the first order, that of the reference power reactor, i.e., Pu-U oxide in metal tubes. However, the cost and handling problems of fuel pins would be substantially reduced if the diameter could be increased over the 0.230-in. reference design. Calculations were performed with 0.5- and 0.75-in.-diameter pins, taking into account the change in Dancoff factor and self-shielding; the correction factors to the void coefficient were found to be 0.0017 and 0.0030, respectively. If the correction factor could be computed to an accuracy of $\pm 50\%$, the

use of 0.5-in.-diameter pins would appear to be an attractive alternate because it would reduce the number of pins required by more than a factor of four. The incentive justifies a more detailed evaluation of this possibility.

The metal- and alloy-plated types of fuel commonly used to permit major composition changes in other fast critical assemblies have not been considered here because they are more expensive, and the addition of oxygen is a problem. Before such fuel forms could be considered for SCBR experiments, the accuracy of the void coefficient correction for heterogeneity would have to be studied in detail, not only because the correction is expected to be large, but also because it must be determined with absolute accuracy (± 0.001 to 0.002).

The reference power reactor has a number of control rods approximately $1\frac{1}{4}$ in. in diameter located on approximately 6-in. centers. Since the control rods and holes affect the void coefficient, the void coefficient must be measured in the critical assembly with and without control rods, as well as in the clean, uniform lattice. Although this is a measured rather than a calculated heterogeneity correction, it must be shown that the void coefficient in a slab critical assembly is the same as that in the annular reference design, with control rods present in both cases. Two-dimensional diffusion calculations were performed with the PDQ code, using the same routine for computing group constants that was described earlier for the case of the clean, uniform lattice. With the control rods in, the difference between the void coefficient of the critical assembly and that of the reference reactor was only 0.0005; with the control rods removed, and the rod guides and holes remaining in the core, the difference was only 0.0006. Thus, the correction factor is negligible for this source of heterogeneity.

TABLE 9. SUMMARY OF CORRECTIONS TO THE VOID COEFFICIENT

Source	$k_0 - k_{100}$	Estimated Uncertainty	Comments
Measurement in Critical Assembly	-0.063	<0.001	Assumed
Correction for Shape and Size	<0.001	0.001	Use 5/1 Slab Critical Assembly
Correction for Temperature	+0.021	0.0013 to 0.0026	Measure Doppler to ± 5 or 10%
Correction for Fuel Composition Pu-240	+0.026	0.0025	Use Two-zone Experiment
Other Isotopes		0.0020 to 0.0040	Match k_{eff} to ± 1 or 2%
Value Deduced for Operating SCBR	-0.016	0.004 to 0.005	Errors Added Randomly

7. SUMMARY AND CONCLUSIONS

The major differences between what may be considered a "practical" critical assembly and the operating reference SCBR reactor are summarized in Table 9. The magnitude of the correction factors and the estimated accuracy of the extrapolation of the void coefficient, as measured in a critical assembly, to that for the reference power reactor are shown for one of several possible sets of assumptions.

In this comparison, the critical assembly is assumed to be a 5/1 slab, rather than the full-size reference-design annular core. The measurements are taken at room temperature, and the extrapolation to the operating temperature is derived, from Doppler measurements in the critical assembly, to an accuracy of either ± 5 or 10%. The critical assembly is fueled with readily

available, 5% Pu-240 plutonium. To correct to the reference design "equilibrium" plutonium (28% Pu-240), Pu-240 cross sections are derived from integral measurements in a two-zone assembly (~ 20 kg high-Pu-240 material), and the basis for the uncertainty in the correction due to other cross sections is a match of the experimental k_{eff} to ± 1 or 2%. The size and shape of the reference design fuel element are used to avoid any heterogeneity correction.

If the void coefficient is required to an accuracy of better than ± 0.004 to 0.005 , it is evident that the entire critical assembly will have to be fueled with high-Pu-240 plutonium, since the correction for fuel composition has the greatest uncertainty. It should be unnecessary to go as high as 28% Pu-240, however. From a rough estimate, it appears that the use of 15 to 20% Pu-240 material should improve the accuracy by about a factor of two and make this source of uncertainty consistent with the optimistic value for the uncertainty in the temperature correction. The resulting void coefficient accuracy would then be about ± 0.002 . Short of a full temperature mockup, it does not seem realistic to expect accuracies much better than about ± 0.002 .

If accuracy in the range from ± 0.004 to 0.005 can be tolerated, further simplifications can be considered. The critical assembly could be a 3/1 slab rather than a 5/1 slab, thereby saving an additional 40% in fuel and material costs, and the diameter of the fuel pins could be doubled, decreasing the number of fuel pins by a factor of four. If optimistic values are assumed for the accuracy of temperature and fuel-composition corrections, then the overall accuracy of the void coefficient would be about ± 0.004 . However, if the temperature correction is limited to 10% accuracy and the critical-experiment data are matched only to $\pm 2\%$ in k_{eff} , the overall accuracy for the void coefficient would decrease to about ± 0.006 .

The following conclusions, although strictly applicable only to the SCBR, should be considered when designing a critical assembly to measure the void coefficient for large fast power reactors:

1. The size of the assembly can be reduced substantially without introducing errors that are larger than

those from other sources. For example, the core volume and critical mass of the 3/1 slab critical assembly are only about $1/4$ as large as the reference reactor volume and mass.

2. The correction for temperature is quite important and is highly dependent on accurate Doppler measurements. Although Doppler measurements for kinetics and safety information are needed only to perhaps $\pm 20\%$ accuracy, techniques must be developed to the point where ± 5 to 10% accuracy can be guaranteed when the void coefficient is considered.

3. The largest correction is that for plutonium isotopic composition, and unless 750 to 1150 kg of higher-Pu-240 material is made available and used for these experiments, it represents the principal limitation on the accuracy of measurements of the void coefficient. The uncertainty in this correction will be reduced as the accuracy of the microscopic cross-section data for Pu-240 and the other common isotopes is improved.

4. Heterogeneity corrections can be avoided by using the reference-reactor fuel forms. However, the use of other fuel forms would impose a heavy burden on the heterogeneity correction, and the accuracy of the void coefficient could well be limited by the accuracy of such calculations.

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Sodium-void Coefficients

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An understanding of the sodium-void coefficient for large fast sodium-cooled power reactors is of paramount importance from safety considerations. Considerable effort is being devoted both experimentally and theoretically to further the understanding of this effect by Argonne National Laboratory. In the past, the sodium-void effect has been measured on a number of assemblies constructed on ZPR-3.^(1, 2) During the past two years, there have been two large carbide assemblies which have been thoroughly studied for the sodium coefficient on ZPR-6.^(3, 4) This paper is a status report on the work with the ZPR-6 facility.

The first assembly studied was a 610-liter uranium carbide core which had a length-to-diameter ratio of approximately unity. The second core was a flat, pancake type of core whose volume was about 950 liters

with a length-to-diameter ratio of $\sim 1:3$. A comparison of the dimensions of the two cores is given in Table 1. Both Assemblies No. 2 and 3 had the identical composition as given in Table 2.

A typical drawer loading is shown in Fig. 1. The drawer was 2 in. in width and 2 in. in height. The depleted uranium and graphite were $\sim 1/8$ in. in thickness, the fuel plates $1/8$ in. in thickness, and the sodium $1/4$ in. in thickness. The sodium-void measurements were performed by three separate runs, in each of which critical positions of the rods were established at a given power level. First, a reference run establishing the critical rod positions for a normal core configuration was made. Second, a run with sodium voided in the region of interest was made. Sodium cans were replaced by stainless steel frames which contained, with an accuracy of about 10%, the same amount of stainless steel as the cans. Third, a run was made in which the worth of stainless steel was measured. This was necessary in order to correct for the difference in stainless steel between the sodium cans and the frames. In general, all results were found to be reproducible to between 0.5 and 1 lh.

In both cores, a thorough mapping of the sodium-void coefficient as a function of location has been accomplished. With Assembly No. 3 a second type of measurement, concerning the effects of material arrangement and heterogeneity on the sodium-void coefficient, was carried out. The latter measurements were expected to be helpful in interpreting the sodium-void mapping results.

The mapping of the sodium-void coefficient was accomplished by obtaining sodium versus void measurements in central regions with different diameters, in regions along the core axis, in cylindrical rings, and finally in a sector comprising $1/8$ of the total of the core. Outlines of some of the regions are shown in Figs. 2 to 4; measured results are given in Tables 3, 4, and 5. Figure 2 shows examples of the regions which were voided in the core for both assemblies. Table 3 gives a comparison of the sodium-void coefficients which were obtained in the central regions in Assemblies 2 and 3. Tables 4 and 5 give the sodium-void co-

TABLE 1. CORE DIMENSIONS OF ZPR-6^(a)

	Assembly No.	
	2	3
Volume (liters)	610	950
Length (cm)	91.4	50.8
Diameter (cm)	92	154.5
L/D	1	1/3
Critical Mass (kg)	544	850

^(a) Both cores had 30-cm-thick axial and radial blankets.

TABLE 2. COMPOSITION FOR ASSEMBLIES 2 AND 3 OF ZPR-6

	Core		Blanket	
	Atom Density, $\times 10^{24} \text{ cm}^{-3}$	YOM Volume Fraction ^(a)	Atom Density, $\times 10^{24} \text{ cm}^{-3}$	YOM Volume Fraction
U-235	0.0022859	0.04760	8.104×10^{-5}	0.001688
U-238	0.009853	0.2052		
C	0.01290	0.1540		
Na	0.007910	0.3595	0.006003	0.07122
SS-304	0.01475	0.17506		

^(a) Corrected volume fractions for use with the macroscopic cross sections by Yiftah, Okrent, and Moldauer, *Fast Reactor Cross Sections*, Pergamon Press.

FIG. 1. Typical Drawer Loading for Assembly No. 3.

FIG. 2. Central Regions.

efficients of axial and cylindrical ring regions for both assemblies. These regions correspond to those shown in Figs. 3 and 4.

Experiments to study the heterogeneity effect were

done in a central region of Assembly No. 3 (see Fig. 2C) with a 28-cm diameter which contained 21 drawers in each half of the reactor. Axially, the void extended either 30.5 cm or 50.8 cm, which constituted

FIG. 3. Axial and Cylindrical Ring Regions for Assembly No. 2.

FIG. 4. Axial and Cylindrical Ring Regions for Assembly No. 3.

TABLE 3. SODIUM COEFFICIENTS IN CENTRAL REGION

Region	Length, cm	Diameter, cm	Assembly 2, lh/kg	Assembly 3, lh/kg
A	15.2	18.7	-1.6	0
B	30.5	28.5	-2.1	-1.31
C	40.6	40.2	-2.99	—
	40.6	41.3	—	-2.29
E	50.8	18.7	—	-3.54
F	50.8	28.5	—	-3.48
D	50.8	49.8	—	-3.23
	91.4	40.2	-4.89	—

the entire length of the core. Measurements were made with:

1. different materials placed next to the sodium;
2. different widths of sodium columns;
3. partial voids in a 1-in.-wide sodium column;
4. discontinuous or staggered void in the normal drawer loading.

Typical examples of the effects of material arrangement and heterogeneity are shown in Tables 6 and 7. In Tables 6 through 8, the alphabetic notation represents the materials as follows:

D: depleted uranium

N: sodium

C: carbon

E: enriched uranium.

The letters which are italicized indicate those columns which were voided.

In Table 6 are given results when all of the sodium in the three types of drawers were replaced by void. The first line of Table 6 represents the way in which a normal drawer was loaded. The second and third lines represent modified patterns in which, in the former case, the sodium is predominantly adjacent to the enriched uranium and graphite. In the latter case, the sodium is predominantly next to U^{238} . It can be seen there is as much as about 20% variation between the highest and lowest value for the sodium coefficient.

Table 7 gives results from a series of measurements in which the drawers were severely modified to determine the effect of a particular material adjacent to the sodium can. The first line shows the case in which only the sodium adjacent to the depleted uranium was voided. The second line shows the measurement when only the sodium next to the graphite was replaced. The third case represents the situation when all of the sodium was removed. Again, it can be seen that there are large variations of the sodium coefficient which are dependent on the material arrangement.

TABLE 4. SODIUM-VOID COEFFICIENTS OF AXIAL REGIONS

Assembly 2 (see Fig. 3)				Assembly 3 (see Fig. 4)			
Region	Axial Boundaries		lh/kg	Region	Axial Boundaries		lh/kg
	Inner	Outer			Inner	Outer	
GI	0	10.16	-1.9	G	0	7.62	-0.18
GII	17.78	27.94	-4.0	H	7.62	15.24	-2.33
GIH	35.56	45.72	-7.5	I	15.24	20.32	-5.0
				K	20.32	25.4	-5.8

TABLE 5. SODIUM-VOID COEFFICIENTS OF CYLINDRICAL RINGS

Assembly 2 (see Fig. 3)				Assembly 3 (see Fig. 4)			
Ring	Radius		lh/kg	Ring	Radius		lh/kg
	Inner	Outer			Inner	Outer	
HI	14.6	24.9	-4.4	L	25.9	36.5	-1.06
II	25.1	35.4	-5.96	M	46.3	58.2	-2.16
JI	35.5	46.2	-7.26	N	65.1	70.0	-2.25
				O	2.7	77.9	-2.13

TABLE 6. HETEROGENEITY EFFECTS FOR VOID LENGTH OF 30.5 CM

Drawer Pattern	lh/kg
DN $\bar{N}$ CE $\bar{D}$ N $\bar{N}$ CD $\bar{N}$ NECD $\bar{N}$ N	-1.31
N $\bar{N}$ CE $\bar{D}$ N $\bar{N}$ CD $\bar{D}$ N $\bar{N}$ VED $\bar{C}$ N $\bar{N}$	-1.48
N $\bar{N}$ CE $\bar{D}$ N $\bar{N}$ DC $\bar{D}$ N $\bar{N}$ CE $\bar{D}$ N $\bar{N}$	-1.25
Average	-1.35

TABLE 7. HETEROGENEITY EFFECTS

Pattern	Void Length	
	30.5 cm	50.8 cm
	lh/kg	lh/kg
DNNCEC $\bar{N}$ N $\bar{N}$ CD $\bar{N}$ NDED $\bar{N}$ N	-1.20	-3.10
D $\bar{N}$ $\bar{N}$ CEC $\bar{N}$ N $\bar{N}$ CD $\bar{N}$ NDED $\bar{N}$ N	-1.97	-4.06
D $\bar{N}$ $\bar{N}$ CEC $\bar{N}$ N $\bar{N}$ CD $\bar{N}$ NDED $\bar{N}$ N	-1.34	-3.59
Average	-1.50	-3.58

Further measurements were made with sodium columns of varying thickness and with different configurations of partial voids in sodium columns of a constant thickness of 1 in. In each case the remaining core materials were rearranged to approximate a homogeneous mixture. The results given in Table 8 show

TABLE 8. HETEROGENEITY EFFECTS DUE TO VARIABLE THICKNESS OF SODIUM COLUMNS

Thickness of Sodium Columns, in.	Material Arrangement	Ih/kg
$\frac{1}{4}$	NNCEDNNDNCDNVCEDNN	-1.24
$\frac{1}{2}$	DCEDN>NNNVCEDN>NNN	-1.52
1	NNNNNNNNNDCEDCDECD	-1.96
2	N ^(16 Cols) N DCEDCDECDNDCEDCDECD (two drawers)	-2.64
Thickness of Void Columns, in. (Sodium Thickness = 1 in.)		
$\frac{1}{4}$	DCEDN>NNNNNNNVCEDCD	-1.94
$\frac{1}{2}$	DCEDN>NNN>NNNVCEDCD	-2.00
1	DCEDN>NNNN>NNNVCEDCD	-1.85

that the sodium-void coefficient changed considerably when the thickness of the sodium columns was varied, but was essentially the same when the thickness of the sodium column was fixed and only the voiding pattern was changed.

To date only simple one-dimensional diffusion-type sodium-void calculations have been made for comparison with the experimental results. Agreement between experiment and theory has only been quantitative.

From looking at these heterogeneity experimental results, it would appear necessary to use two-dimensional transport calculations which properly take into account the material arrangement in the drawers. Otherwise an agreement between experimental and theoretical results better than about 10% must be considered fortuitous. It would appear evident that for larger dilute cores, where the spectrum is more

degraded, it is even more important to take into account the heterogeneity effects.

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Measurement and Analysis of Doppler Effect in Plutonium-fueled Fast Reactor Assemblies

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I. Introduction

The measurements described in this paper were made with the ZPR-3 fast critical assembly of the Argonne National Laboratory. Zone-loading procedures were used in Assembly 45A to produce an all-fast assembly which contained in its central zone a mockup of a large fast power breeder using plutonium-239, uranium-238 monocarbide fuel, sodium coolant, and stainless steel clad and structure. The center zone of Assembly 45 differed from that of 45A in the replacement of 40% of the sodium cans by graphite. This change gave a second neutron-energy spectrum having more neutrons in the region which is the source of the Doppler effect reactivity changes. Figure 1 shows the geometrical arrangement of this loading in ZPR-3; Table 1 gives the outer radii and atomic compositions of the zones.

An unusually small central zone was designed for this assembly in order to reduce reactor-drift problems related to decay heat of the plutonium and to reduce the neutron-source problem. The real and ad-joint spectral match at the position of the Doppler measurement deviated by a few percent at most from the single-region spectrum for neutrons in the energy interval that contributes to the Doppler effect.

II. Equipment and Procedure

A schematic diagram of the apparatus to measure the Doppler effect as used in ZPR-3 is shown in Fig. 2. A drive unit moved the Doppler equipment back and forth, exchanging the Doppler elements, at one of two temperatures, and reference material at the center of the assembly. A fine, calibrated control rod maintained the power of the reactor essentially constant throughout the Doppler measurement. If there were no secondary events occurring at the same time, the change in the reactivity worth of the Doppler elements on heating would be due to the Doppler effect alone. An important part of the Argonne Doppler program consists of designing against and estimating the magnitude of these secondary effects.

The construction of the basic Doppler element is

shown by the photograph of Fig. 3. Pellets of appropriate isotopes, in the form of oxides at 0.70 theoretical density, were pressed into cylinders, 0.5003 in. in diameter and 0.500 in. in length, and fired. Twelve of these pellets were used to produce an element 6 in. long.

Two such Doppler elements were mounted in line, symmetrically about the axial center of the reactor in which they were to be used, as indicated in Fig. 2; the separation between them was 12.1 cm. One end of each Doppler element was fixed in position in such manner that both elements expanded in the same direction when heated. This design caused the material of one element to move in the direction of a slightly increasing flux gradient, and the material of the other element to move into a correspondingly decreasing flux gradient. A first-order cancellation of flux-gradient expansion effects occurred as a result. Theoretical calculations by two-dimensional transport theory were used to evaluate the importance of residual expansion effects, which were significant for the fissile isotopes only.

Table 2 lists the kinds of Doppler elements which were prepared in this manner and gives their isotopic composition. In addition, elements of similar design but having diameters of 0.73 in. and 1.004 in. have been fabricated and filled with natural uranium dioxide.

III. Results and Conclusions

The experimental results obtained using Doppler elements made up of U-238 and U-238 mixed with Pu-239 in a ratio of seven to one are shown in Table 3; results of theoretical analysis of these measurements are also included in the table. The following section will discuss both experimental and theoretical uncertainties of the numbers reported in this and the next table.

It can be seen that there is relatively satisfactory agreement between experiment and theory for the cases of Table 3. There is much less theoretical uncertainty, arising mainly from uncertainties in basic nuclear data, for U-238 than for the cases of fissile

FIG. 3. Basic Doppler Element.

Extra windings of the sheathed heater wire at each end provided a flat axial temperature distribution. Three thermocouples along the length of every element measured this distribution. The jacket was a high-nickel alloy

well against the experimentally measured worth of plutonium at the same position in the reactor, giving -185 In/kg theoretically versus -176 ± 4 In/kg experimentally. The radial flux dependence given by diffusion theory also compared well with that of a similar one-dimensional transport calculation for the case of a plutonium Doppler element at the center of Assembly 45A. A two-dimensional, nine-group transport calculation of one of the Doppler reactivity changes agreed with the perturbation calculation of the same case to within a few percent.

Table 4 contains the results of measurements made with Pu-239 and, for completeness, experimental Doppler results for U-235 obtained by J. Gasidlo and P. Amundson in the same assembly.⁽¹⁾ These fissile isotope results lead to some very interesting observations which, certainly, must be tested by further experimental work. The correction for the expansion effect has been evaluated with some care, so that the experimental results, as corrected for expansion effects, will be compared to the theoretical results.

The experimental results for Pu-239 show remark-

TABLE 2. ATOMIC DENSITIES OF DOPPLER ELEMENT, atoms/cm³ $\times 10^{-24}$, AT 500°K
Effective length = 15.28 cm
Effective radius = 0.6366 cm

Isotope	Type Element				
	Pu-239	Pu-239/ U-238	U-238	U-238 (1 in.)	U-235
Pu-239	0.01471	0.001528	—	—	—
Pu-240	0.00038	0.000123	—	—	—
U-238	—	0.013267	0.01560	0.01732	0.000421
U-235	—	0.000095	0.00011	0.000124	0.01535
O	0.03016	0.030118	0.03142	0.03488	0.03155

ably small, positive Doppler reactivity responses. This holds true even for the much softer spectrum of Assembly 45, where there should be many more neutrons in the region of the Doppler effect.

It can be seen from Table 4 that as the neutron energy spectrum hardens in proceeding from Assembly 45 to Assembly 45A and then to Assembly 45A with B₄C about the Doppler element, agreement be-

TABLE 3. DOPPLER REACTIVITY CHANGES FOR U-238 AND FOR U 238 + Pu-239 IN 7/1 RATIO ($\Delta T = 500$ TO 1100°K)

Meas.	Experimental				Theoretical ($\Delta k/k$)		
	Assy.	$\Delta\rho, \text{Ih}$ ($\pm \sim 0.020$)	$\Delta k/k$ per kg ($\times 10^{+6}$)	$\Delta k/k$ ($\times 10^{+6}$)	Columbia ($\times 10^{+6}$)	Firk ($\times 10^{+6}$)	Exact ($\times 10^{+6}$)
<i>U-238</i>							
$\frac{1}{2}$ -in. D.E.	45	-0.327	-27.7	-6.39	-6.19	-	-
1.0-in. D.E.	45	-1.38	-23.3	-26.8	-26.11	-23.8	-26.4
Dummy	45	-0.006	-	-0.112	-	-	-
<i>U-238</i>							
$\frac{1}{2}$ -in. D.E.	45A	-0.184	-16.0	-3.68	-2.66	-	-
<i>238/239, 7:1</i>							
$\frac{1}{2}$ -in. D.E.	45A	-0.156	-13.6	-3.12	-1.99	-	-

TABLE 4. DOPPLER REACTIVITY CHANGES FOR Pu-239 AND U-235 ($\Delta T = 500$ TO 1100°K)

Measured ^(a)	Experimental			Expansion Correction, $\Delta k/k$ ($\times 10^{+6}$)	Corrected $\Delta k/k$ expt ($\times 10^{+6}$)	$\Delta k/k$ theory ($\times 10^{-6}$)
	$\Delta\rho$, Ih ($\pm \sim 0.020$)	$\Delta k/k$ ($\times 10^{+6}$)	$\Delta k/k$ per kg ($\times 10^{+6}$)			
<i>PU-239</i>						
First Measurement	-0.060	-1.21	-5.36	—	-	—
3 Wks Later	-0.049	-0.99	-4.39	—	--	—
Average	-0.055	-1.10	-4.88	+1.35	+0.25	+5.06
Homogeneous	-0.034	-0.68	-3.02	+1.35	+0.67	—
In B ₄ C	+0.042	+0.83	+3.70	+0.90	+1.73	+2.33
1 Element	-0.044	—	—	—	—	—
Assy. 45	-0.024	-0.56	-2.49	+1.63	+1.07	+12.95
<i>U-235</i>						
J. Gasidlo and P. Amundson	+0.054	+1.1	+4.64	+1.05	+2.15	+2.90

^(a) Assembly 45A, except as indicated.

tween theory and experiment improves remarkably. This behavior may be related to uncertainties in the lower-energy Doppler cross sections for plutonium-239, discussed in the next section. The experimental Doppler reactivity change for U-235 is calculated much better. That the basic cross-section information, in particular the value of α , is relatively better known for U-235 tends to support the U-238 evidence that the flux in the Doppler region may be reasonably well calculated.

The case labeled "Homogeneous" represents the experimental difference resulting from replacing the "scattering zone" and "fuel zone" by a completely uniform core region. One sees that the Dancoff correction is not large. In the "1 element" case, that one of the two Doppler elements which expands into a decreasing flux gradient was heated while the other was not. The difference between this reactivity change and half of the two-element response is a measure of the compensation occurring, as explained in Section II,

when the two elements expand in the same direction and into opposite flux gradients.

The U-235 measurement was done with the same apparatus except for the U-235 Doppler elements, which were fabricated in the same manner as the plutonium elements. The procedure of the measurement was also the same. Experiments are currently being performed by one of us (CET) with Doppler elements constrained to prevent axial expansion, constrained to prevent both radial and axial expansion, and having expansion properties very much like those used in the experiment reported here, in another assembly on ZPR-6.

The results obtained in these measurements indicate that the positive Doppler reactivity contribution of plutonium-239 in the fuel, and in the neutron-energy spectrum of a large power breeder reactor, is small in magnitude compared to the strong negative Doppler effect of U-238. Further experiments should be performed to verify and refine these measure-

ments as well as to extend their range. It should also be remembered that these results are for a "clean" fast reactor. The presence of fission products and of Pu-240, which builds up with fuel recycle, would reduce the magnitude of the results seen here.⁽²⁾

IV. Experimental and Theoretical Uncertainties

The principal source of experimental uncertainty was reactor drift during the experimental data taking operation. Because the central zone of the assembly was plutonium-fueled, there was a significant amount of heat to be removed by drawing air through the zone. The procedure led in turn to a drift sensitivity related to the temperature of the reactor cell air as it entered the matrix. Control of these parameters improved continuously during the course of the experiments and has been worked on in anticipation of future work. Another source of difficulty which was observed during the course of measurements reported here and corrected is related to the cooling air, which removed about 35 W of heat per Doppler element at 1100°K. A cover over the Doppler element drive tube now forces the air to move through the dummy vacuum manifold tube, preventing direct contact with the matrix. The reactivity signal due to the fissile isotopes was unnecessarily small. Time limitations did not allow for preparation and employment of several pairs of Doppler elements to thereby multiply the signal, as was possible with the equipment available.

The expansion correction is a serious problem which is being treated by further design. The corrections used in this report were calculated by 2-dimensional (2-D) transport theory. Both 5- and 9-group cross sections were produced for the 2-D code for each reactor region by a flux-weighting routine working from a 25-group cross-section set related to the set 224 reported elsewhere.⁽³⁾ Only a very slight reactivity difference was seen when the expansion effect was calculated when nine instead of five groups were used and when the convergence criterion was tightened by a factor of two.

A relatively large and probably quite conservative overall experimental uncertainty of $\pm 0.40 \times 10^{-6} \Delta k/k$ has been assigned to all data of Tables 3 and 4. Improved data-taking procedures currently being used for Doppler measurements with ZPR-6 (CET) are producing results with uncertainties smaller by approximately an order of magnitude than this.

The theoretical calculations have separate difficulties of their own. A continuous problem to all fast reactor calculations is whether the calculational procedure produces the correct flux magnitude in the tail of the fast reactor neutron-energy spectrum, where the entire Doppler effect occurs.⁽⁴⁾ For the calculations

TABLE 5. DOPPLER CHANGE IN EFFECTIVE CROSS SECTIONS FOR URANIUM-238 FOR 1.0-IN.-DIAMETER ROD (500–1100°K)

Energy Interval, keV	NR Approximation		Exact (RIFF-RAFF)
	Columbia Parameters $\delta\bar{\sigma}_\gamma$	Firk's Parameters $\delta\bar{\sigma}_\gamma$	Firk's Parameters $\delta\bar{\sigma}_\gamma$
4.0–2.8	0.04764	Unresolved Region	
–2.0	0.06962		
–1.4	0.08608		
–1.0	0.13215		
–0.55	0.19323		
–0.30	0.21485		
–0.10	0.34438 (IA)		
–0.030	0.2920 (IA)		
		0.13035	0.13367
		0.13976	0.14111
		0.19212	0.20029
		0.12612	0.14168
		0.23037	0.32911
		0.18940	0.37472

TABLE 6. DOPPLER CHANGE IN EFFECTIVE CROSS SECTIONS FOR PLUTONIUM-239 (500–1100°K)

Energy Interval, keV	Schmidt Parameters		Hummel Parameters	
	$\delta\bar{\sigma}_f$	$\delta\bar{\sigma}_\gamma$	$\delta\bar{\sigma}_f$	$\delta\bar{\sigma}_\gamma$
15.0–9.1	0.0095	0.0043	0.0102	0.0053
–4.0	0.0286	0.0173	0.0284	0.0155
–2.8	0.0722	0.0412	0.0753	0.0429
–2.0	0.1238	0.0757	0.1337	0.0773
–1.4	0.1708	0.1165	0.1882	0.1158
–1.0	0.2519	0.1876	0.2849	0.1808
–0.55	0.3840	0.3119	0.4445	0.2905
–0.30	0.6534	0.5846	0.7680	0.5275
–0.10	0.8853	0.8715	1.0524	0.7624
–0.03	0.9423	1.0591	1.1498	0.9006

reported here most regional cross sections were produced by use of the ELMOE code⁽⁵⁾ with radial leakage buckling corrections being added. Other cross sections were prepared from the new ANL code MC², which produces multigroup cross sections from a basic library containing very detailed nuclear data.

The reaction cross sections for the Doppler effect for the analyses of the experiment were produced by one of the authors (RH). The remainder of the discussion will center on this procedure and on uncertainties in the Doppler cross section.

The Doppler cross sections reported in Tables 5 and 6 were calculated using the usual narrow-resonance approximation, the assumption of a flat neutron source, flat flux, and Wigner's rational approximation applied to calculate the escape probability for the Doppler element, which then leads to the well-known equivalence relation. It is therefore possible to write the effective cross section into the form appropriate to a homogeneous medium by introducing an equivalent potential scattering cross section.

The evaluation of the effective cross section has been discussed extensively for cases with and without resonance overlap.⁽⁶⁻⁸⁾ Overlap corrections were included in the Doppler reaction cross sections used here. A discussion of some of the possible limitations of the use of assumptions leading to the simplified form of the effective cross section is given later in this paper.

The best available Doppler-region nuclear parameters are those for U-238. For this study the recent Columbia University parameters⁽⁹⁾ which extend the resolved region to 4 keV were used. This covers a substantial portion of interest in these calculations. For comparison, the data of Firk, Lynn, and Moxon⁽¹⁰⁾ have also been used. The resultant Doppler changes in cross sections for U-238 are shown in Table 5. Because resonance parameters exist for energies beyond those for which the above procedure might be in doubt, a more exact calculation was made by means of RIFF-RAFF.⁽¹¹⁾ This code takes into account lethargy, space, and temperature dependence of the source, and by use of correct escape probabilities, calculates both the resonance integrals and the perturbed group flux. Results of use of this code are included in Table 5. They show that the exact method gives essentially the same cross sections in regions for which there is significant flux.

Our lack of adequate nuclear data for the fissile isotopes in the Doppler neutron energy region could account for a major part of the disagreement with experiment. Because almost all resonances are not resolved in the Doppler region, we must rely on a statistical treatment involving an extrapolation from resolved resonances at low energies. The Doppler effect of the fissile isotope, which is determined by the competition between fission and capture, is very sensitive to the value of α and even more so to the value of the ratio $\delta\sigma_f/\delta\sigma_\gamma$. These ratios in turn depend strongly on the fission width distribution function and also on the average fission width.

For the calculation of Pu-239 Doppler effect in this paper a $\nu = 1$ (Porter-Thomas) distribution for the fission width and average fission widths of 0.04 eV and 0.156 eV for spins $J = 0$ and $J = 1$, respectively, as suggested by Schmidt,⁽¹²⁾ were used. Slightly higher values of the fission widths were used beyond 5 keV to obtain better agreement with measured α values at high energy. For comparison, calculations were also made using the spin-independent Γ_f of 0.099 eV and a $\nu = 2$ (random) distribution which has been widely used in the past.⁽²⁾ For U-235 the χ -squared distribution of $\nu = 2$ was used. The average fission width Γ_f was varied about the usual value of 0.120 eV in such a way that the resulting average α value

would agree approximately with the measured values at various energies as given in Ref. 13.

Table 6 gives the group-dependent change in cross sections obtained by use of these two sets of assumptions.

Unlike the case for U-238, the use of the flat-flux assumption appears to be dubious for a rod composed of high concentration of a fissile isotope below 1 keV because significant flux depression must occur in this region. Therefore, narrow-resonance calculations based on a flat-flux assumption may not be satisfactory. This seems to be indicated by the experimental results which agree relatively well with theory when the lower-energy neutrons have been removed in the B₄C experiment. Whether the discrepancies between theory and experiment for the soft-spectral case are due to uncertainties in knowledge of resonance parameters or the validity of the oversimplified method, or both, is not yet certain.

It seems very evident that much more theoretical and experimental work must be done with the fissile isotopes in varying fast reactor spectra.

VI. Acknowledgment

J. Gasidlo and P. Amundson were responsible for supervision of the ZPR-3 reactor while these measurements were being made. We are most happy to acknowledge their capable help.

A number of shop men in Building D-20, and, in particular, Clare Kotora and the machinists of the D-316 shop, are to be thanked for their help. W. Hills helped greatly during various stages of the experiment.

D. White did most of the work of producing the ceramic pellets of the Doppler elements, while J. Handwerk, A. Shuck, J. Ayer, and A. Hins, among others, were exceptionally cooperative and helpful in planning and executing the ceramics work.

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Recent Doppler Measurements with ZPR-6

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I want to report briefly on the current series of Doppler measurements with ZPR-6 that are just now being finished. These measurements follow chronologically the set described by Dr. Fischer in the last paper. We have not had time to analyze the results in any depth or for any detailed comparison with theory, but we believe that some of the results are of sufficient interest that they should be presented to this conference. The data have been reduced and analyzed to, I think, final form.

The equipment used for the current measurements is similar in part to that used in the measurements described in the last paper. We have made, however, two significant changes. First, by improving the data-gathering techniques—and this largely is in providing a servo-controlled regulating rod for the reactivity measurement, fully automated with the oscillator system—we have improved the precision of the measurements by better than an order of magnitude. This, in turn, has allowed us to trace out the temperature dependence of the Doppler effect, something that was not feasible in the previous series, while still retaining precisions at each temperature better than the previous results by a factor of about ten. Secondly, in this series we have taken a direct approach to the problem of separating expansion effects from the true Doppler effect by designing three different sample types, each identical to the others except that in one type the sample material is allowed to expand freely; in the second type the axial expansion is constrained, and in the third type expansion is constrained both axially and radially. By comparing the results obtained from use of these otherwise identical samples, the expansion effects are shown directly. We believe, in fact, that one of the significant advantages of the heated-sample technique is the control one has over experimental conditions and, in particular, a potential control over the expansion problem.

The measurements were done in a zoned version of a 2600-liter carbide core. The core contained 42 v/o sodium, with a fuel isotopic ratio of 7:1 U^{238} to U^{235} .

One of the first things of concern was the possible

effect on the Doppler effect in the heated sample of resonances in material around the sample, which is the same as the sample material, but unheated—the so-called “hot-sample, cold-reactor” effect. We compared the results of two measurements with the same sample in two slightly different environments. The first measurement was done in the normal reactor environment. In the second, a zone of material about a mean free path or so thick around the sample had the U^{238} removed and replaced by molybdenum—the quantity of molybdenum selected so that the calculated Doppler energy fluxes and adjoints were held constant for the two cases. The idea was to form an isolation zone around the sample without perturbing the gross fluxes or adjoints in the Doppler energy region. How successful we were in this matching was tested experimentally by measuring the worth of boron in each environment. The two boron worths checked to fraction of one percent.

Figure 1 gives the results of measurements with the 7:1 sample, having an isotopic ratio the same as the reactor itself. The upper curve shows the result with the sample in the isolation zone and the lower curve that with the sample in the reactor environment. One can see that there is probably a statistically significant, but small, effect at lower temperatures, but no very significant effect overall is noted.

Figure 2 shows the results of three separate experiments. The upper curve gives the U^{238} Doppler effect, the curve containing experimental results from two determinations about ten weeks apart. In the intervening time, the core and sample arrangement had been changed around several times. They were then returned to the original configuration and the measurement redone. The result gives us considerable confidence in our reproducibility, for as is apparent in the figure, all points fell on the same curve. I might say in passing that the shape of the curve fits a $1/T$ dependence within a few percent. The lower curve is the result for the same sample but with a 1/2-in. natural boron carbide filter installed around the element to illustrate that the measured effect is, in fact, due to low-energy neutrons. The calculated reduction in

the Doppler energy fluxes and adjoints caused by the boron was such that the calculated Doppler effect was reduced to about 20% of its previous value and the measurement agrees nicely.

FIG. 1. Reactivity Change as a Function of Temperature with an Isolation Zone Surrounding Sample.

FIG. 2. Reactivity Change, with a Boron Filter Surrounding the Doppler Element, as a Function of Temperature.

FIG. 3. Reactivity Changes with Freely Expanding and Axially Constrained Samples as a Function of Temperature.

FIG. 4. Reactivity Changes for Elements of Various Isotopic Ratios of U^{235} to D^{236} as a Function of Temperature.

FIG. 5. Linear Thermal Expansion of Uranium Metal.

Figure 3 compares the results for the U^{235} oxide freely expanding element and for a similar element in which no axial expansion was allowed. An expansion effect is apparent, but it is not large. The U^{235} Doppler effect thus is shown to be positive in these spectra and, comparing its magnitude with the U^{238} Doppler effect given in Fig. 2, to be about $1/3$ the magnitude at low temperatures, decreasing to about $1/5$ at $1100^\circ K$. The shape, therefore, is such that the U^{235} Doppler effect appears to fall off more rapidly than the approximate $1/T$ dependence found for U^{238} .

Figure 4 summarizes data for samples with varying isotopic ratios of U^{238} to U^{235} . In general, the Doppler effect of the mixtures seems to be about that given by pro-rating, simply on the basis of the amounts present in each mixture, the experimental results of the two component parts.

Finally, in Figures 5 and 6 are shown the results of a measurement that gives strong confirmation of the essentially positive nature of the U^{235} Doppler

Fig. 6. Reactivity Changes of U^{235} Metal Samples as a Function of Temperature.

effect in these spectra, and also gives quantitative indication of the reason why negative effects have been observed in the past on heating U^{235} metal. The measurement consisted simply of a Doppler measurement of a U^{235} metal sample in the normal manner and taking advantage of the volume change in the α - β phase change of the metal to calibrate the reactivity effect of expansion. Figure 5 simply shows the expansion of as-cast uranium metal on heating, with the discontinuity at the phase change.

In Fig. 6 the circles indicate the measured reactivity effect on heating. The lower curve shows the reactivity effect of expansion as given by Fig. 5, calibrated in reactivity units by the experimentally observed reactivity change through the phase change. The upper curve is the experimentally observed U^{235} Doppler effect taken from the U^{235} oxide nonexpanding element result shown in Fig. 3. Thus both upper and lower curves are based on experiment.

The middle curve is the sum of the upper and lower curves. The agreement with the observed result is, of course, remarkable. However, even if the agreement were worsened considerably, the data would still be convincing, for the odd shape of the observed reactivity curve, much different than any other result we obtained, can only be explained on the basis of a

positive Doppler effect falling off rapidly with increasing temperature combined with strong negative expansion effect increasing in a discontinuous manner through the phase change.

Discussion

Mr. Pitterle (APDA): I wonder if you could comment on how thick the nickel heating element around the sample is?

Mr. Till: 50 mils.

Mr. Pitterle: I wonder if you are planning to pursue this zone around the sample any further. I don't believe the question here is the hot sample, cold reactor effect, but what is the flux incident on the sample. And here, I think, very often the question comes out what is the effect of the heterogeneity of the core? I wondered if you plan any experiments of two types—one, where you might vary the sample size of this core around it, and see how much of the fine structure is coming from the core and how much might be from the sample; secondly, whether you might be considering varying the plates immediately around the sample, perhaps to try and take one extreme of homogeneity versus another to see what types of effect this has. I would like to emphasize this because I think the interpretation in terms of the Doppler effect of a reactor is much clearer when you can put the homogeneous zones around the sample, provided your heating-element thickness is so small as not to perturb the fine structure.

Mr. Till: We have, in fact, done experiments in which the immediate environment around the sample has been changed. The normal reactor environment around the sample was built out of small pieces selected so that we did have a much more homogeneous arrangement of materials than is true throughout the remainder of the reactor. We then removed that more homogeneous zone and replaced it with material arrayed in the same way as the rest of the reactor, and we got no significant difference. As for the other point about interpretation of the hot sample in a cold reactor, the calculations I have done indicated that one doesn't expect much effect from the interaction between the cold resonances and the hot-sample resonances as long as the temperature change is high enough that the sharp dip in the flux from the cold resonance doesn't really bear on the resonance broadening taking place out in the wings. Certainly the region around the center line of the cold resonance gives the sort of sharp dip that isn't very important as soon as the ΔT becomes large.

Mr. Chernick (BNL): One always worries about these small-sample experiments with very small reactivity exchange. Is there any chance that you might

take a bundle of elements and repeat the measurements?

Mr. Till: We are planning just such a measurement with essentially a cluster. We have no plans at the moment of going on to a large heated zone, but we have started design of a cluster experiment.

Mr. Küsters (Karlsruhe): The results giving the negative plutonium Doppler coefficients seem to be the same as the British have obtained. Only the correction for expansion seems to be different. I

wonder if Dr. Codd or Dr. Fischer would say the only correction one has to do to have a positive coefficient is to put the right correction on the expansion coefficient?

Mr. Fischer (ANL): I think that in these series of measurements we have obtained some very interesting clues in that the plutonium Doppler coefficient is much smaller than we had earlier expected it to be, but I don't think we can say that we have the ultimate experiment here.

Recent Results of Doppler Measurements in Fast-neutron Spectra

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I would like to discuss briefly the status of the Fast Spectrum Doppler Measurements program at Atomics International and to outline the areas of study in which future work will be carried out. It should be pointed out that some of the results to be shown have already been discussed in recent technical meetings; consequently, a full paper will not be given. However, since the last ANS meeting, measurements have been carried out on some new materials, the addition of which brings the extent of material coverage to encompass W, Ta, Nb, and Mo, in metal form, Th as metal and oxide, and U^{238} and U^{235} as metal, oxide, and carbide forms. The range of spectra have extended from 200-keV median fission energy (mfe) systems down to approximately 0.01-eV mfe. Our measurements were made in a manner very similar to the technique discussed in the last paper, i.e., small samples in a multiregion reactor. The samples were generally 4 in. long and 0.438 in. in diameter.

The scale of median fission energy in Fig. 1 is a very rough indication, taken from calculations, of the energies at which the various experiments were made. These results apply to U^{235} metal, and the $\Delta\rho$ were determined over the temperature interval from 20 to 600°C. The experimental measurements of the total temperature coefficient of reactivity are shown by the square symbols and the dashed line. The expansion coefficient of reactivity is represented by the circular symbol and the dotted line. The former values are the results which were reported, at one-mfe value, as a negative Doppler coefficient for U^{235} at the meeting two years ago. Since that time, we have done careful expansion-effect measurements by five independent methods, including the use of an alpha-beta phase change and a sample whose density has been artificially reduced to approximately 85% of theoretical, in order to find the effect of expansion of the samples in these cores.

When we make an algebraic subtraction point by point, the points represented by the triangles in Fig. 1 result. A rough curve drawn through these points is shown as the solid line. This curve now represents our estimate of the Doppler effect in the

U^{235} material, roughly as a function of median fission energy. These measurements were done in some cases with polyethylene regions around the sample to reduce the energy of the neutron spectrum and to give us an indication of those lower-energy cores which might be of interest for further experiments.

Our main conclusion from this work is that the Doppler effect in fast cores is positive for U^{235} . It is about half the effect that we calculate and curves over a little faster than our calculation would predict. It does go negative someplace, probably in the low-keV region and then continues negative below that point. Similarly, the expansion coefficient goes through zero, going from negative to positive, as one decreases the mfe from 200 keV to the low-eV region.

Figure 2 shows the results from a typical calculation. The curves are similar to those shown by Till, in the last paper, of the total measured effect, the expansion effect, and then the subtraction giving the Doppler effect for the sample as a function of energy. The measured total reactivity as a function of temperature will vary from a curve which starts out in the negative direction and continues in this direction with constant slope for a hard spectrum to a curve similar to Fig. 2, where it starts out in the negative direction, goes through zero, and then becomes positive for a soft spectrum.

Among the Doppler problems which are currently under study is one concerned with fitting the measured points to various temperature-dependent functions and extrapolating the results to very high temperatures. In Fig. 3, the measured change in the reactivity with respect to temperature T has been compared with plots of $T^{-\gamma}$. The two particular curves plotted in this case are for $\gamma = 1/2$ and 1, made by a least-squares fit. Our results for U^{238} , shown in Fig. 4, in general show that the exponent γ is slightly less than 1 for the hard cores and becomes smaller as we go down to the low-energy cores. For the fast cores, the fit is a little harder to make because the smaller signal makes more difficult the distinguishing among the several curves.

Our immediate plans for the future are to use

FIG. 1. Variation of Reactivity of U^{235} due to Temperature and Expansion, and Derived Doppler Reactivity for Different Calculated Median Fission Energies.

FIG. 2. Reactivity Worth vs Temperature for U^{235} .

FIG. 3. Temperature Coefficient of U^{238} Metal.

FIG. 4. Temperature Coefficient of U^{238} Metal.

plutonium, which we now have on order, in different isotopic compositions, including a small sample of almost pure plutonium-241. We plan to go to temperatures of the order of 2000°C and are just in the process of starting measurements in which the reactivity is determined as a function of temperature from room temperature down to 40 or 50°K. Particular attention is being paid to the effects of the Debye temperature.

Doppler Effect in U^{238} Capture and U^{235} Fission in a Fast-neutron Spectrum*

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1. INTRODUCTION

The Doppler effect which is so important to the inherent safety of fast reactors has been investigated in the fast-neutron spectrum of the Mixed Spectrum Critical Assembly (MSCA). Reference (1) describes the reactor and other experimental results obtained in this program, and reference (2) describes the physics design of the MSCA. This study includes both reactivity and activation determinations of the U^{238} Doppler effect. The activation measurements, the subject of this paper, are believed to be the first to be performed in a fast spectrum.

2. CONCLUSIONS AND RECOMMENDATIONS

The activation Doppler measurement is an extremely worthwhile complement to the more familiar reactivity measurement; it permits a direct evaluation of the ability to predict the change in U^{238} capture. Further, the activation technique provides the only opportunity to separate the effects of cross section and fine flux spectrum components of the gross Doppler effect. The measurement technique requires careful control of foil position and counting procedures, since the signal is in the 1 to 2% range. Nevertheless, experience has shown that reproducible measurements can be performed to ± 0.002 or approximately 10% of the magnitude of the observable Doppler effect.

The activation measurements have indicated a significant underprediction of the Doppler effect. This discrepancy is believed to be at least partly due to the calculated neutron spectrum being substantially too hard. This conclusion is borne out by measurements of neutron spectrum and $1/v$ poison worth. These results, coupled with the generally good agreement obtained between predicted and measured Doppler reactivity effects in similar fast spectra, indicate that present

cross sections must lead to compensating errors. For example, an overprediction of the change in U^{238} capture and an underprediction of the "importance" of this change due to significant errors in predicting gross spectra could explain present discrepancies. Future work in this area should be closely coupled with neutron-spectrum determinations.

Additional work is clearly needed to resolve several unanswered questions. First, what is the energy distribution of Doppler events in typical fast spectra? This can be determined by a series of measurements with varying foil thicknesses and known fuel buffers between the foil and fuel environment. Second, what is the flux energy spectra? This can be determined by proton-recoil and time-of-flight spectrum measurements over the energy range from 50 eV to 1 MeV. Finally, through a comparison to reactivity measurements in known spectra remaining compensating errors could be evaluated and hopefully eliminated.

3. MEASUREMENT METHOD

3.1 Comparison of Activation and Reactivity Techniques

Two basic methods of measuring the Doppler effect are suitable for application in zero-power (critical) assemblies.⁽³⁾

In the *activation method* a sample of either fissile or fertile material is irradiated at an elevated temperature and its induced activity compared to that of a cold sample which was irradiated under the same conditions.

In the *reactivity method* a sample is heated and the resulting change in reactivity measured. The measurement may be either static or oscillatory. Further, the oscillator measurements may involve either thermal or mechanical oscillation.

Each measurement method measures different components of the Doppler effect. The two techniques are, therefore, complementary rather than duplicative in their approach. The reactivity measurement, to first order, determines exactly the reactivity effect per gram at the selected location of the reactor that would occur

* Work sponsored by Atomic Energy Commission under Contract AT(04-3)-189, P A. 13.

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in a reactor power excursion, whereas the activation measurement determines the exact change in neutron capture or fission in a reference spectra. Attention will now be focused on the activation technique which was the method used in this study.

Considering an infinite homogeneous medium at uniform temperature, the reaction rate in volume v , per unit energy, at energy E is

$$R = \int_v \varphi \sigma dv = v \varphi \sigma. \quad (1)$$

Upon taking the derivative with respect to temperature we obtain

$$\frac{dR}{dT} = v \left(\varphi \frac{d\sigma}{dT} + \sigma \frac{d\varphi}{dT} \right). \quad (2)$$

This expression illustrates the relative contributions, to first order, of a change in cross section of the heated sample and of a change in the spectrum impinging upon the sample. For very small samples (foils) almost all reactions in the foil are due to neutrons whose last collision was in either surrounding fuel or structural material. Under these circumstances only the first term on the right-hand side (RHS) of Eq. (2) is important. On the other hand, if the heated samples are large, or if a heated buffer zone is present immediately surrounding the sample, the second term on the RHS of Equation (2) becomes important. This term may be either positive or negative. For the present measurements only the first term was determined.

3.2 Experimental Technique

Two identical furnaces loaded with uranium metal foils are placed in symmetric positions within the fast core of the MSCA. Figure 1 shows a cutaway view of the special fuel bundle and furnace in the foil-holder region. Figure 2 shows the foil-placement detail. Three concentric tubes are used in the furnace construction to provide thermal insulation and paths for gas flow. The furnace assembly fits into a special fast core fuel bundle from which the central nine fuel rods have been removed, leaving the outer sixteen rods in place. Cooling air is introduced into the fuel channel at the lower end and flows upward along the outside of the furnace and then down through the outer furnace annulus. An inert cover gas (argon) is maintained in the innermost tube to prevent oxidation of the heated uranium foils. The sample foil holder is aluminum with heater wire wrapped on the lower portion. The furnace design minimizes neutron scattering between the foil and the surrounding fuel to assure a resonance flux spectrum at the foil location representative of the fuel environment. However, for selected experiments an 0.15-in.-thick nickel sleeve was inserted between the foil and the inner

FIG. 1. Furnace for Doppler Effect.

furnace tube to increase intentionally this non-fuel scattering and, hence, provide a basis for experimentally extrapolating to zero scattering.

Two thermocouples are used at the sample-foil location with a third located at the center of the coiled heater wire. There are also two thermocouples on the outer furnace tube at the foil elevation. Four enriched U-235 flux-monitored foils are mounted on the outer furnace tube at the same elevation as the heated foil. These foils permit determination of small asymmetries in the gross flux distribution between the two furnace locations. Great care was taken in the design and testing of the furnace components to assure a negligible effect due to thermal expansion of the structural components.

One-half-inch-diameter uranium metal foils of both 93% and natural enrichment are utilized. For most of the experiments the foils are all 3 mils thick; however, some of the experiments utilized 0.5-mil-thick natural uranium metal foils. The foils for a given irradiation are matched closely in weight to eliminate any differences in gamma self-absorption.

One natural and one enriched uranium metal foil are loaded into the foil holder which, in turn, is loaded into the furnace assembly. Two 1-mil-thick buffer foils of aluminum are used to separate the uranium foils and to prevent transfer of fission products between them. Two

FIG. 2. Foil-holder Furnace Assembly.

other aluminum foils are used to prevent fission product contamination of the foil holder. Four enriched U-235 foils are installed in the flux-monitor locations on the outer furnace tube. The entire furnace assembly is then loaded into the appropriate fuel bundle position in the MSCA fast core.

After approximately 2 hr of argon purging, one of the two furnaces is heated to the desired temperature. Approximately 45 min is required to reach 800°F starting from room temperature. Once the desired temperature is obtained, the reactor is brought to power. To induce the desired foil activity it is necessary to irradiate at approximately 1000 W for one hour; after completion of irradiation the reactor is shut down, the furnace allowed to cool, and the short-lived fission products permitted to decay. After approximately 15 hr the foils are removed and their counting is initiated.

The irradiated foils are gamma counted with a 3 × 3-in. sodium iodide (T1) scintillation crystal. The U-238 capture is inferred by observing the 106-keV, 56.3-hr-half-life activity due to Np-239. The fission product activity on the other hand is determined by observing the gross gamma activity between 500 to 800 keV, which has approximately a 24-hr half-life at one day after formation. The scintillation pulses observed by the photomultiplier are amplified and analyzed by a 256-channel pulse-height analyzer. Two unirradiated foils, one natural and one enriched, are used to determine the residual background activity. The uranium metal foils from each irradiation are counted approximately once every hour over the two days following the irradiation. The counts obtained from the natural uranium foils in the 100-keV region are corrected for the contribution of fission product gammas, utilizing the

measured gross fission product gamma activity in the 400- to 800-keV region as a measure of total fission product activity present.

3.3 Analytic Model

It is convenient to analyze the Doppler effect observed in the heated foil in terms of a three-region system: the foil, the surrounding non-fuel material, and the fuel environment. The following assumptions are then made:

1. A scattering collision with fuel or non-fuel material results in a neutron energy change large compared to the resonance width (NR approximation).
2. The resonances are widely separated so that the flux outside a resonance is constant in lethargy and space.
3. Resonance scattering can be neglected.
4. Cross sections are independent of space within a given region.

Using the methods outlined by Reynolds,⁽²⁾ it follows that

$$\begin{aligned}
 I_1 = & (1 - T_0) \int_{\text{RES}} \sigma_{a,1} \frac{\Sigma_{p,1} + S}{\Sigma_{t,1} + S} dE \\
 & + T_0 \int_{\text{RES}} \sigma_{a,1} \frac{\Sigma_{p,3}}{\Sigma_{t,3}} dE \\
 & + T_0 \int_{\text{RES}} \sigma_{a,1} \frac{\Sigma_{p,1}}{S + \Sigma_{t,1}} \left[1 - \frac{\Sigma_{p,3} \Sigma_{t,1}}{\Sigma_{p,1} \Sigma_{t,3}} \right] dE,
 \end{aligned} \quad (3)$$

where

T_0 = the transmission probability across the non-fuel region between the foil and the fuel;

$\sigma_{a,1}$ = microscopic absorption cross section of the foil at temperature T_1 ;

FIG. 3. Doppler-activation Results for $780 \pm 20^\circ\text{F}$ Foil Temperature 5.5 In. from Core Center for 23.1-23.6 w/o Enrichment.

Σ_p = the macroscopic partial scattering cross section;

S = reciprocal foil thickness [$S/(S + \Sigma_{t,1})$ is used for the foil escape probability];

Σ_t = the macroscopic total cross section with subscript 1 referring to the foil region and subscript 3 referring to the fuel region.

In most practical cases the third integral may be neglected, giving

$$I_1 = (1 - T_0) \int_{\text{RES}} \sigma_{a,1} \frac{\Sigma_{p,1} + S}{\Sigma_{t,1} + S} dE + T_0 \int_{\text{RES}} \sigma_{a,1} \frac{\Sigma_{p,3}}{\Sigma_{t,3}} dE. \quad (4)$$

The gross neutron-energy spectrum is obtained from a 24-group diffusion-theory calculation. The resonance integrals are evaluated over specific energy intervals in the form

$$I = \int_{\text{RES}} \sigma_a(T_1) \left[\frac{\Sigma_p + S}{\Sigma_t(T_3) + S} \right] dE, \quad (5)$$

where T_1 and T_3 can be equal or unequal temperatures. Resolved resonance parameters are used up to 1 keV, and a statistical model used for resonances above 1 keV. Doppler-broadened cross sections were obtained by use of the RAPTURE Code⁽⁵⁾ which has been modified by the addition of the PITFORK option to calculate integrals of the type shown in Eq. 5. The transmission probability T_0 for the intermediate region between the foil and fuel is determined by a collision probability method reported by Reynolds.⁽⁴⁾

Note that this expression for the foil resonance integral is independent of the foil thickness in the limit of $T_0 = 1$. Dependence on the foil thickness should be present at this limit, and its absence is the result of neglecting an apparently small term in the resonance-integral expression. For this reason, the predicted values in Fig. 3 are not shown at the value $T_0 = 1$.

4. RESULTS

4.1 Comparisons with Theory

The results, expressed as the normalized ratio of the heated-foil activity to the unheated-foil activity, may be compared to the ratio of effective resonance integrals over narrow energy intervals (see Eq. 3) weighted by the calculated multigroup flux spectrum. Several measurement conditions were utilized in the program as indicated in Table 1. The foil thickness was varied to evaluate the effect of changes in the potential scattering cross section. The nickel sleeve was inserted to determine the effect of varying T_0 , the transmission probability of the region between the foil and surrounding fuel. The potential scattering cross sections were 3000 and 18,000 b, respectively, for the 3- and 0.5-mil foil thicknesses. The transmission probabilities were 0.3 and 0.7, respectively, with and without the nickel sleeve in place.

The measurements at 5.5 in., with and without the nickel sleeve present, are compared with predictions on Fig. 3. Here, the activation ratio (*hot to cold*) is plotted as a function of T_0 of the non-fuel region. Calculated curves are shown for potential scattering cross sections corresponding to both the 3- and 0.5-mil foils.

Two important conclusions can be drawn immediately. First, the calculated values for the actual 23.1 w/o U-235 enrichment lie considerably below the measured points, indicating that the spectrum is likely to be softer than predicted. To evaluate this possibility further, the calculated spectrum was arbitrarily softened to yield agreement with the measurements. This agreement was obtained by using a fictitious enrichment of 16 w/o U-235. The degree of spectrum change required to bring experiment and theory into agreement can be assessed by the fact that the median fission energy decreased from 200 to 150 keV and the fraction of fissions below 9.1 keV increased from 8.0 to 12.4%, respectively. Second, the 3-mil foil exhibits a very strong self-shielding effect, namely, the weak depend-

ence upon the presence of the nickel sleeve. The data for the 0.5-mil foil, on the other hand, indicate a stronger dependence on the presence of the nickel sleeve and, hence, less self-shielding. Both conclusions are further substantiated by the data for the 3-mil foil at 12.2 in. Table 1 summarizes all the measured values.

4.2 Measurement Errors

It is important to consider random and systematic errors in the activation ratio measurement so that realistic error bounds can be established. The random errors considered here are:

1. determination of gamma activity of the uranium foil;
2. determination of uranium weight in the foils;
3. overall random error in the measurement of the activation ratio of foils irradiated at the same temperature.

The systematic errors which have been considered are:

1. errors in the flux correction due to relative motion of the furnaces or thermal expansion effects in the heated furnace;
2. asymmetry in the neutron spectrum causing different Doppler effects at the two supposedly symmetric furnace locations;
3. effect of the 106-keV gamma-pulse-height spectrum width used in determining the hot-to-cold Np-239 activation ratio;
4. effect of the wait time after irradiation at which the activation ratio is determined;
5. effect of fission product activity on the determination of the Np-239 activity;
6. self-absorption of gamma radiation in the foils.

4.2.1 Random Errors

4.2.1.1 Determination of Np-239 Activity. The heated and unheated foils were in the sequence A-B-B-A, and the activation ratio determined by the ratio of the sum of the respective activities. This sequence required about 10 to 15 min and was repeated many times for one set of foils so as to accumulate about 10^6 counts in the 106-keV peak for each foil. The error in the activation ratio as determined from a single A-B-B-A sequence is $(2/N)^{1/2}$, where N is the total counts accumulated for one foil in the sequence. A plot of the cumulative distribution function obtained from 16 such counting sequences showed the data to be normally distributed. About 10^5 counts were accumulated on each foil for the determination of each ratio value.

Thus, the standard error of the activation ratio samples due to counting statistics alone was about 0.003. The measured standard deviation of the 16 samples was 0.0037, indicating that the counting error is dominant. The vast majority of data obtained showed the same

TABLE 1. RESULTS OF ACTIVATION MEASUREMENTS

Distance from Fast-core Center (in.)	Temperature, °K	Foil Thickness (in.)	Nickel Sleeve around Foil	Measurement Repetitions	(A_H/A_C) for U-238
5.5	780 ± 20	0.003	No	14	1.016 ± 0.001
5.5	780 ± 20	0.003	Yes	4	1.012 ± 0.002
5.5	780 ± 20	0.0005	No	4	1.017 ± 0.002
5.5	780 ± 20	0.0005	Yes	4	1.006 ± 0.002
12.2	780 ± 20	0.003	No	4	1.019 ± 0.002
12.2	780 ± 20	0.003	Yes	4	1.017 ± 0.002
5.5	440 ± 20	0.003	No	2	1.0098 ± 0.003

result, so that it is believed that errors due to positioning the foils on the crystal and variation of pulse-height-analysis equipment are negligible.

4.2.1.2 *Determination of Foil Weights.* When combining the results of activation ratios determined from irradiation of several sets of foils, the errors in uranium weights must also be considered.

The foils used in a given irradiation were first cleaned and then weighed repeatedly. The random error of weighing appeared to be about 0.1%. The foils were also weighed after irradiation, and no significant weight gain or loss was noted. The 0.003-in.-thick foils weighed approximately 0.15 g; the random weight error was about 0.00015 g. For most of the weighings, a balance was used with a vernier scale marked in 0.0001-g increments. Weighings were repeated two or three times, re-zeroing the balance between weighings. Repeatability of 0.0001 or 0.0002 was obtained in the weighings. The 0.0005-in.-thick natural uranium foils were weighed with a balance whose vernier scale could be read to 0.00001 g. These weighings also appeared to have a random error of about 0.1%. Some of the 0.003-in.-thick natural uranium foils were also weighed with this more sensitive balance. It is estimated that the actual random error in the uranium content of the foils was approximately 0.1% of the foil weight when surface oxidation and moisture are considered.

4.2.1.3 *Overall Random Error in the Measurement.* The hot-to-cold activation ratio corrected for the neutron flux difference at the two furnaces can be expressed for error estimation purposes as

$$R = \frac{N_H}{M_H} \frac{M_c}{N_c} R_\phi, \quad (6)$$

where

N = Total Np-239 counts accumulated for one foil (subscript H indicates heated foil, c an unheated foil);

M = Foil weight;

R_ϕ = Flux-correction ratio.

The error in R can be written as

$$\frac{\sigma^2(R)}{R^2} = \frac{2\sigma^2(N)}{N^2} + \frac{5}{2} \frac{\sigma^2(M)}{M^2} + \frac{1}{2} \frac{\sigma^2(F)}{F^2}, \quad (7)$$

where

N = Total Np-239 counts accumulated for one natural uranium foil;

F = Total fission product counts accumulated for one flux-monitor foil;

M = Foil weight.

Since the ratio R is nearly 1.0, we have

$$\sigma(R) = \left[2 \frac{\sigma^2(N)}{N^2} + \frac{5}{2} \frac{\sigma^2(M)}{M^2} + \frac{1}{2} \frac{\sigma^2(F)}{F^2} \right]^{1/2}. \quad (8)$$

In most cases, the count statistics yield

$$\begin{aligned} \frac{\sigma^2(N)}{N^2} &= \frac{1}{N} \\ &= 10^{-6} \quad (\text{Natural uranium-foil counts}) \end{aligned} \quad (9)$$

and

$$\begin{aligned} \frac{\sigma^2(F)}{F^2} &= \frac{1}{F} \\ &= 10^{-6} \quad (\text{Flux-monitor-foil counts}). \end{aligned}$$

The weight error is estimated to be 0.1%, so

$$\frac{\sigma^2(M)}{M^2} = 10^{-6}. \quad (10)$$

Thus, the population standard error $\sigma(R)$ is

$$\sigma(R) = \sqrt{5 \times 10^{-6}} = 0.0022. \quad (11)$$

A total of nine separate irradiations were made over a period of one year with both furnaces at the same temperature. In two of the nine cases, the furnaces were both at 800°F; in the other seven, they were both at room temperature. The cumulative probability distribution for the nine ratios shows the data to be normally distributed. The data were treated in exactly the same manner as the other heated-foil cases, including flux correction. The average value and standard error of the nine sample values are

$$R(T_1 = T_2) = 1.0000 \pm 0.0023.$$

The ratio is expressed as the activation ratio of Furnace #1/Furnace #2. It is thus demonstrated that the actual population standard deviation is essentially equal to the expected value.

A total of 14 data points were obtained from separate irradiations at the same conditions over a period of one year:

Foil size—0.003 in. thick by 0.5 in. in diameter;

Foil location—5.5 in. from center of fast core;

Heated foil temperature—780 ± 20°F.

Four of the irradiations were made with a fast-core U-235 enrichment of 23.6 w/o and with 40% of the reactor power in the fast core. The other 10 irradiations were made with a fast-core enrichment of 23.1 w/o, and with 30% of the reactor power in the fast core. Since no calculation shows that an enrichment change from 23.6 to 23.1 w/o has a significant effect on the measurement, all 14 points are considered to have been obtained under the same conditions. The cumulative probability distribution of the 14 points shows the data to be normally distributed. The average value and standard error of the 14 sample values are

$$R = 1.0164 \pm 0.0022.$$

Again, the population standard deviation of these 14 samples is essentially that predicted from consideration of the basic measurement errors.

The data obtained during the period 9/29/64 to 10/2/64 (shown in Table 3 below) appear to be in general lower than the other data points. Since these were rather early measurements and the careful routine of foil weighing, activation, and counting was not as uniformly established, these four points have been given a weighting of 0.25 compared to the remaining ten data points.

By use of the T -statistic it is possible to establish the range of the true mean about the sample mean as

$$\bar{X} - \frac{\sigma}{\sqrt{n}} T_p < m < \bar{X} + \frac{\sigma}{\sqrt{n}} T_p.$$

This relation is read as follows: "Given a sample of size n with mean $\bar{X}$, we are $P\%$ certain that the true mean lies within $\pm(\sigma/\sqrt{n}) T_p$ about the sample mean." T_p is the value of the T -statistic for probability P with $n-1$ degrees of freedom. Assuming $\sigma = 0.0025$, we obtain the results in Table 2 for various sample sizes. Assuming the Doppler activation difference to be 1.6% (Doppler ratio of 1.016), Table 2 shows that a sample of size 4 should give a random error of ± 10 to 25% in the measurement, depending on the desired confidence level.

4.2.2 Systematic Errors

The effects of foil thickness and furnace structural material on the measurement were investigated experimentally and discussed earlier in this paper. It was felt that those effects were fundamental to the interpretation of the measurement and deserved special investigation. Other possible systematic errors have also been investigated and are the subject of this Section.

4.2.2.1 Effects of Thermal Expansion and Relative Motion. The radial displacement of the heated sample foil relative to the outer flux-monitor foils was measured in a prototype furnace. Holes were drilled through the furnace tube walls at the foil locations. Micrometer dial gauges with 0.0005-in. resolution were used to measure the change in position of the inner foil holder relative to the outer tube. The total relative displacement at a sample foil temperature of 750°F was less than 0.010 in.

TABLE 2. RANGE OF TRUE MEAN ABOUT SAMPLE MEAN

Sample Size	Probability		
	95%	80%	68%
2	± 0.0224	± 0.0054	± 0.0030
4	± 0.0040	± 0.0020	± 0.0014
7	± 0.0023	± 0.0014	± 0.0011
14	± 0.0015	± 0.0009	± 0.0007

TABLE 3. EFFECT OF INTERCHANGING HEATED-FOIL POSITION AT 5.5-IN. SYMMETRIC LOCATIONS

Irradiation Date	#1 Hot/#2 Cold	Irradiation Date	#2 Hot/#1 Cold
6/12/64	1.0173	6/18/64	1.0159
6/18/64	1.0188	6/30/64	1.0161
9/29/64	1.0151	9/30/64	1.0107
10/1/64	1.0142	10/2/64	1.0138
4/21/65	1.0137	4/22/64	1.0175
6/27/65	1.0160	6/26/65	1.0171
7/14/65	1.0173	7/15/65	1.0170
Average = 1.0160 ± 0.0019		Average = 1.0154 ± 0.0024	

The activity data obtained from the four monitor foils showed the following radial flux gradients at various core locations:

Distance from Fast Core Center, in.	Radial Flux Gradient at Furnace, %/in.
5.5	1.5
12.2	3.3

Thus, a displacement of 0.010 in. along the line of maximum gradient yields an activation difference of about 0.015% at the 5.5-in. location and of 0.03% at the 12.2-in. location. This would contribute a 1 to 2% error in a Doppler activation difference of 1.6% (Doppler ratio of 1.016).

Since the fuel bundles in the core have about a 0.1-in. freedom of motion in the radial direction, about 0.1% to 0.6% flux correction is possible. This is the range of flux correction observed experimentally.

The axial expansion of the sample foil holder was measured to be 0.062 in. relative to the outer furnace tube when the sample foil temperature was raised from 72°F to 750°F. This value compares favorably with an estimated expansion of 0.08 in. In each measurement the heated sample holder was shimmed down 0.0625 in. so that it would expand into the same position as the unheated foil holder. Activity measurements with enriched uranium flux-monitor foils showed an axial gradient of 0.6%/in., so that small uncertainties in the relative position are negligible with respect to the Doppler ratio of the order of 1.016.

4.2.2.2 Asymmetry of the Doppler Effect at the Two Furnace Locations. The 14 Doppler ratio values obtained at 5.5 in. with the 3-mil foils were obtained by heating the foil in both symmetric locations in the fast core. Table 3 compares the average values obtained for 7 measurements with the heated foil in each location. The error shown in Table 3 is the standard deviation of the seven samples. Evidently there is no measurable significant effect of interchanging the position of the heated foil at the 5.5-in. location. The standard deviation of

each of the seven sets of measurements is approximately that which is expected considering the basic measurement errors.

4.2.2.3 *Effect of the Pulse-height Spectrum Width Used to Determine the Np-239 Activation Ratio.* Usually, the 106-keV gamma activity of Np-239 was determined by using a 45-keV energy width about the peak, that is, 106 ± 23 keV. The effect on the Doppler ratio of varying this energy width was studied by using the data of two separate irradiations. Figure 4 shows the variation obtained.

The error bands shown for the data in Fig. 4 reflect the error due to counting statistics. There appears to be a trend established by the results of the two separate irradiations, such that the average change in activation ratio with energy width is $-2.3 \times 10^{-5}/\text{keV}$, that is, as the energy width is narrowed, the hot-to-cold ratio increases. However, this observed effect is well within

the limit of the statistical error of counting, so that it is difficult to establish such an effect definitely.

The value of the activation ratio obtained by extrapolating these curves to zero energy width may or may not be the true value, since it is possible to either under-correct or over-correct the data in trying to obtain the true Np-239 activity. At any rate, we can say that the apparent error is small and within the random error of the measurement.

4.2.2.4 *Effect of Time after Irradiation at Which Activity is Measured.* The Np-239 activity was measured at times from 12 to 100 hr after the irradiation. Examination of the hot-to-cold activation ratios thus obtained showed no trend with time.

4.2.2.5 *Effect of Fission Product Activity on the Np-239 Activation Measurement.* In the fast-neutron spectrum characteristic of the MSCA, the U-235 and U-238 fission product activity in the 106-keV γ -ray

FIG. 4. Np-239 Activation Ratio vs Energy Width Used for 106-keV Peak.

peak of Np-239 is believed to contribute negligible error to the measurement. By measuring the gamma activity in both natural and enriched uranium foils in the 106-keV (Np-239) and 400-to-800-keV (fission product) energy regions, it is possible to obtain an estimate of the fission product activity in the 106-keV energy region. The effect of this experimentally determined correction was to raise the hot-to-cold Np-239 activation ratio by approximately 0.0005, that is, if the ratio without correction is 1.0170, then after the fission product correction it is 1.0175.

4.2.2.6 *Effect of Self-absorption of Gamma Activity in the Foils.* The distribution of Np-239 in the foil will be nonuniform because of the foil self-shielding effect. Thus, the Np-239 may be distributed somewhat more uniformly in the heated foil than in the unheated one. The self-absorption of the gamma activity would then result in a lower measured activity for the heated foil. This difference in gamma self-absorption due to spatial distribution of the Np-239 was investigated analytically. Assuming the Doppler line has a rectangular shape, we can derive an approximate expression for the measured gamma activity. For the 1.5% change in activation due to the Doppler effect, it was determined that the difference in gamma self-absorption can be neglected. This is not necessarily true for cases in which the Doppler effect is large and spatial self-shielding is appreciable.

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Discussion

Mr. Davey (ANL): In your counting at 106 keV, I understand you have a contribution of a few percent due to fission products, both from U-235 and U-238. I wonder, in the case of heating the foil, whether or not some evaporation of fission products could occur? Unless you correct for this effect, would it not appear as an apparent Doppler effect?

Mr. Pflasterer: First of all, I neglected to say that the U-235 Doppler measurement, which we also measured but for which we did not show any results, turned out within the error of our measurement to be essentially zero; that is to say, the cold-cold value of the ratio was one and the hot-cold ratio was also one within the 0.2% error. To measure this Doppler effect in U-235 fission, we used a 500- to 800-keV gamma spectrum. If the fission products were driven off by raising the temperature, the gamma-spectra energy would be of the order of 0.8 or higher MeV. The effect, insofar as it concerns the 500- to 800-keV region, is essentially zero or at least below the resolution of the measurement. We also have not detected any variation in the correction that we apply, that is, we have not detected any variation of this correction as a function of either foil thickness or time of radiation. We did make some irradiations that lasted one hour and some that lasted three hours, so that the boil-off time might be different in those cases. We believe that all the indications are that the effect is quite small and within the resolution of the experiment.

Transient Flux Distributions by Discontinuous Modal Synthesis

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Introduction

The neutron-flux distribution in a core transient is, without doubt, one of the most investigated phenomena in nuclear technology. The literature survey in reference (1) indicates the extent of domestic and foreign attempts to perfect existing solutions.

Figure 1 shows schematically the convergence of two separate systems of study towards a common goal of description of the transient flux. Although the compromise forces a loss in precision in description of the flux distribution and in plant-feedback description, it does bring a useful tool within reach of the nuclear engineer and systems analyst.

The neutron kinetic problem, hence the system-analysis and control-design problems, are more critical in fast reactor plants than in thermal reactor plants. There are several reasons for this, among which are:

- the larger power swings due to a lower fraction of delayed-neutron and shorter average lifetime;
- the greater importance of fuel motion resulting from temperature changes due to the higher power densities and mean free path-core dimension ratio;
- the effect of a positive coolant coefficient that must be compensated for by the moderator density changes, as in some thermal reactors,* to ascertain spatial stability.

Furthermore, the control-rod circuits require faster and more precise operation; hence, knowledge of flux distribution during reactor transients is of paramount importance to the reactor designer.

In systems analysis, a complete plant representation is usually considered, with the core divided into (at most) three regions. The resulting kinetic calculations must thus be adapted to few-region models. Furthermore, analyses of such models are usually performed

by analog computation. Available descriptions of the transient flux are unsuitable for these complete-system analyses because of their complexity or inadaptability to analog simulation. For this reason, a modal-synthesis method which is attractive for few-region problems and is applicable to analog or digital computation was considered as most appropriate.

In the modal synthesis used, the overall modes across the reactor are taken as a composite of the region modes, with the interface conditions of continuous flux and current being satisfied. In turn, the region-wise modes satisfy the buckling equation for each respective region, giving rise to "intrinsic" or region-wise buckling. Defined in this fashion, the overall modes are functions with finite discontinuities in their derivatives at the region interfaces.

This paper presents the $1\frac{1}{2}$ -group kinetics model considered for a systems study of the Westinghouse Fast Breeder Reactor.⁽²⁾ The slow-flux group is averaged over the core and represented as a point, while the modal synthesis is used to describe the spatial behavior for the fast-neutron group.

Attention is focused on the modal-analysis problem, with a brief description of the averaging process proposed for two-dimensional analysis. This topic opens the way for further investigation and clearly shows the strong points of the process for analog-computer complexes having repetitive compressed time units. Elements of the analog analyses necessary to implement the transform solution are discussed, and an example of application via digital procedures is briefly described.

The Reactor Equations

The equations used are derived from diffusion theory and are the familiar two-group diffusion equations with m delayed-neutron groups. It has been observed that for the fast reactor considered, the slow-neutron flux defined by energies below the 5-keV level, has a rela-

* The study presented in this paper must be extended to a complete two-neutron-group model to enable an adequate representation of this effect.

FIG. 1. Convergence of the Studies towards the Modal Synthesis Model.

FIG. 2. The Flow Pattern of the Reactor Equation.

tively flat distribution and an effectiveness of only 5 to 10%.⁽²⁾ The slow flux may thus be approximated by a point representation without sacrificing overall accuracy. In order to separate the entire slow-neutron flux effect, the delayed-neutron group is divided into two parts, those arising from fast fission and those arising from slow fission. The line of thinking is shown in the upper section of Figure 2.

The neutron kinetic equations may then be written in a matrix form, with the dependent variable expressed as a column matrix of flux dimension containing the flux and the precursor densities multiplied by the average velocity of the neutron-flux group. This gives the following set of equations:

$$L_f \phi_f + l_f \phi_s + S_f = \dot{\phi}_f; \quad (1)$$

$$l_s \phi_s + S_s = \dot{\phi}_s, \quad (2)$$

with

$$\begin{aligned} \underline{L}_f &= \begin{bmatrix} [\nabla D_f \nabla - (\Sigma_A^f + \Sigma_R^f) + (1 - \beta) \nu_f \Sigma_f^f] v_f & \lambda_1 \cdots \lambda_m \\ \beta_{f1}' v_f & -\lambda_1 \cdots 0 \\ \vdots & \vdots \\ \vdots & \vdots \\ \beta_{fm}' v_f & 0 \cdots -\lambda_m \end{bmatrix}; \\ \underline{l}_f &= \begin{bmatrix} (1 - \beta) \nu_s \Sigma_f^s v_f & \lambda_1 \cdots \lambda_m \\ 0 & 0 \cdots 0 \\ \vdots & \vdots \\ \vdots & \vdots \\ 0 & 0 \cdots 0 \end{bmatrix}; \\ \underline{l}_s &= \begin{bmatrix} \left(\frac{S}{\alpha V} - \Sigma_{A_{avg}}^s \right) v_s & 0 \cdots 0 \\ \beta_{s1}' v_s & -\lambda_1 \cdots 0 \\ \vdots & \vdots \\ \vdots & \vdots \\ \beta_{sm}' v_s & 0 \cdots -\lambda_m \end{bmatrix}; \\ \underline{\phi}_f &= \begin{bmatrix} \Phi_f \\ C_{f1} v_f \\ \vdots \\ C_{fm} v_f \end{bmatrix}; \quad \underline{\phi}_s = \begin{bmatrix} \Phi_s \\ C_{s1} v_s \\ \vdots \\ C_{sm} v_s \end{bmatrix}; \quad \underline{S}_f = \begin{bmatrix} S_f v_f \\ 0 \\ \vdots \\ 0 \end{bmatrix}; \quad \underline{S}_s = \begin{bmatrix} (p \Sigma_R^f \Phi_f + S_s)_{avg} v_s \\ 0 \\ \vdots \\ 0 \end{bmatrix}. \end{aligned}$$

All the factors of equations (1) and (2) are dimensionally defined. Artificial sources can be included through the terms S_f or S_s . The subscript avg means that a core average must be evaluated. No weighting function is specified but, if judged necessary, may be included.

Equation (2) can be directly solved:

$$\underline{\phi}_s = e^{\underline{L}_s t} \left(\underline{\phi}_{s0} + \int_0^t e^{-\underline{L}_s t} \underline{S}_s dt \right),$$

where $\underline{\phi}_{s0}$ is the slow-neutron flux matrix at time $t = 0$. This is a column matrix, consisting of the initial average slow-neutron flux and initial average precursor densities due to slow-neutron fissions. (The precursor densities are multiplied by the slow-neutron average velocities as in $\underline{\phi}_s$.) The problem then resolves to a time and space solution of (1), for which the modal analysis is used as described in the following.

The Kernel and the Transform Equations

Equation (1) may be solved by the use of a transform method. A one-dimensional problem is first considered. The transform kernel is defined by

$$L_f \underline{\psi}_{nj} = -\lambda_{nj} \underline{\psi}_{nj} \quad (3)$$

(with appropriate boundary conditions), giving the modal solution

$$\underline{\phi}_f = \sum_n \sum_j a_{nj}(t) \underline{\psi}_{nj} \quad (4)$$

with

$$\underline{\psi}_{nj} = \begin{bmatrix} \psi_{nj} \\ \xi_{(nj)1} \\ \vdots \\ \xi_{(nj)m} \end{bmatrix}.$$

The double subscript is due to the presence of the precursors. For a given flux kernel shape defined by the subscript n , all of the precursor kernel distribution shapes are determined, but not their relative amplitudes. The $m + 1$ solutions with the different relative amplitudes are necessary in order to satisfy the $m + 1$ initial distributions of the flux and precursors.

An adjoint kernel equation is formed by

$$\underline{L}_f^T \underline{\psi}_{nj}^* = -\lambda_{nj}^2 \underline{\psi}_{nj}^*, \quad (5)$$

with identical boundary conditions as for (3). The eigenvalues λ_{nj}^2 are evidently the same for equations (4) and (5) as can be easily demonstrated,^(2,3) and the functions $\underline{\psi}_{nj}$ and $\underline{\psi}_{nj}^*$ present the property of biorthogonality (on two indices).

By definition, the transform equation is

$$\int_{\text{core}} \underline{\phi}^T \underline{\psi}_{nj}^* d(\text{core}) = \bar{\phi}_{nj}, \quad (6)$$

which is a scalar variable.

We make the replacement

$$a_{nj}(t) = \frac{\bar{\phi}_{nj}(t)}{N_{nj}} \quad (7)$$

in (4) with N_{nj} defined by

$$\int_{\text{core}} \underline{\psi}_{nj}^T \underline{\psi}_{nj}^* d(\text{core}) = N_{nj}. \quad (8)$$

By taking the transpose of (1), multiplying by $\underline{\psi}_{nj}^*$, and integrating over the core using (3), (6), and the biorthogonality property, we obtain the following:

$$-\lambda_{nj}^2 \bar{\phi}_{nj} + \bar{S} = \bar{\phi}_{nj}. \quad (9)$$

The flux matrix solution is given by solving (9) and introducing the result in (7) and (4):

$$\underline{\phi} = \sum_n \sum_j^{m+1} \frac{\exp(-\lambda_{nj}^2 t) \left[\bar{\phi}_{0(nj)} + \int_0^t \bar{S}(t) \exp(\lambda_{nj}^2 t) dt \right]}{N_{nj}} \underline{\psi}_{nj}, \quad (10)$$

where $\bar{\phi}_{0(nj)}$ is given by the transformation of the initial ($t = 0$) flux distribution matrix.

The Multiregion, Multidimensional Reactor

As was indicated in the Introduction, the preceding transformation may be applied to multiregion problems^(1, 3) such as multicore flux description. The constants defining the flux descriptions are related by the boundary conditions between each region, such as flux continuity and current continuity. If the kernels are considered as functions satisfying the diffusion matrix L and the boundary conditions between each region, as well as the outer boundary conditions, the transformed dependent variable has the same properties. It follows, then, that the flux description in each region is a function of the material buckling of that region; since the buckling changes from region to region, the function expressing the flux is discontinuous (analytically speaking). An important consideration is that the first mode describes the steady-state asymptotic condition, whereas the higher modes describe the departure of that condition. This property largely decreases the number of terms necessary to obtain an acceptable description of the transient flux for systems analysis of a nuclear plant.

For a multidimensional analysis, by appropriate decomposition of the terms of the first-row, first-column element of the L_f matrix, the matrix can be decomposed into as many components as there are dimen-

sions to be considered. Thus, the first row and first column of each component would contain the derivatives of one dimension. As an example, for a decomposition performed along the x (axial) and r (radial) dimensions of a reactor (the line of thoughts can be followed in Fig. 2), $L_f = L_x + L_r$, where L_x contains only derivatives of the form $\partial/\partial x$ and L_r only derivatives of the form $\partial/\partial r$. The terms of the second to the $m + 1$ columns are left with the L_r matrix; hence the L_x matrix has only its first column different from zero. A spatial transformation may be applied to the x coordinate, and the resulting transformed equation is only a function of the coordinate r . This equation can be solved either as a one-dimensional problem, or further transformed and solved by a second modal synthesis.

The transformed variable may now be averaged in the x direction. The resulting equation contains only the r -independent variable. This approach enables approximation of the peak flux value by the following process:

1. The peak of the average* in the r direction is first found by "normal processes."
2. It is assumed that the overall peak occurs at the same value of r as the peak of the flux averaged in x , i.e., $f(r)$. Consequently, a detailed modal synthesis is performed at that value of r in the x direction, and the peak value is retained as an approximated value of the overall peak.

This problem was adapted for analog-computer simulation studies which are considered as follows.

The Analog-computer Simulation^(1, 3)

The analog-computer study of transient-flux descriptions by finite transformation as given by equation (10) consisted of the following:

- finding the eigenvalues and eigenfunctions for the respective modes and orders;
- obtaining solution of the flux equation with discontinuous spatial coefficients in the steady state;
- solving boundary-value problems for the transient flux.

The first items above define the elements necessary for the axial transformation; the two last items give the solution in the radial coordinate for the average values in a steady state and during a transient.

From a computer programming point of view, the first two items constitute a simple iteration problem; the third is one of iteration and memory.

Figure 3 shows the machine search for a first eigenvalue by iteration. As may be seen, the iteration continues until λ_n^2 satisfies the condition of zero flux at the

* Actually the peak of $\int_{\text{axial}} \phi(r, x) dx / \int_{\text{axial}} dx = f(r)$.

FIG. 3. Boundary-value Search for Fixed Initial Tangent Search of the Eigenvalue

reactor boundary. Figure 4 shows the computed eigenfunction together with its first and second derivatives. A typical rod-insertion process in a critical core is shown in Figure 5, with the dotted line showing the extent of the rod insertion. In Figures 3, 4, and 5, the spatial coordinate is horizontal; the dependent variable is vertical, directed downwards.

A block diagram of a complete space-time simulation of a nuclear plant with a core divided into two regions is given in Fig. 6. This simulation makes use of a repetitive and a real-time analog computer linked together and controlled by logic intervention. All sections of this simulation have been tested successfully. However, the present memory capacity of the equipment constitutes the limiting feature in coupling the repetitive core calculations with the remainder of the plant data.

A simple digital check of the transform method was also performed. In this check, a slab reactor consisting of two regions, with one flux group (thermal) and one group of delayed neutrons, was considered. The matrix L was updated step-wise in time so that fuel-temperature feedback could be taken into account; hence the solution modes were changed with time. Transients were computed for absorption cross section decreased in the center, rodded region, which was considered as $\frac{1}{38}$ and $\frac{1}{8}$ of the core volume. Worst-condition profiles (largest deviation from steady state) were checked by a steady-state calculation through use of the adiabatic assumption.⁽³⁾

Conclusion

Although the transient-flux distribution can be calculated by different existing methods, the technique outlined above is advantageous and adaptable to

FIG. 4. The Eigenfunction Solution and Its Derivative.

systems analyses. It is, in effect, a compromise between elaborate multigroup calculations and simple point or adiabatic representations.

The $1\frac{1}{2}$ -group behavior of the Westinghouse Fast Breeder Reactor evolves primarily to a time and space solution for the fast-neutron group. All elements of the modal analyses necessary to perform this task have been investigated via analog simulation, using repetitive compressed time units with adequate logic and by a simple digital check.

The merits and disadvantages of modal analysis for design and analysis of fast reactors will become apparent only through further investigation.

FIG. 5. Rod-insertion Process in an Initially Critical Core (Rod Inserted from the Left).

FIG. 6. Analog Simulation of a Fast Reactor Plant.

The above approach is an initial step to bring transient-flux-distribution solutions within the scope of complete systems analysis. It is anticipated that subsequent improvements in equipment and technique will permit complete solution by analog methods and that the above kinetic treatment will be incorporated into an overall plant digital code.

Acknowledgment

The authors extend their thanks to Dr. E. F. Restelli, Jr. for his helpful suggestions during the study and the review of this paper.

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Criteria for the Density of Monitoring Points in Large Reactors

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Summary

In large fast reactors of modular core design, each module is very nearly critical independently. In this situation, small changes in the nuclear properties of individual modules can lead to large power imbalances, and each module must be monitored as if it were a separate reactor. Corresponding control capability is required.

In large, nonmodular cores, the density of monitoring points requires additional criteria for its determination. Monitoring results are used to estimate the local power at all points in the reactor. The maximum tolerable deviation between such estimated power and the actual power is one of the required criteria. When the largest expected deviation in local nuclear properties is chosen as a second criterion, the density of monitoring locations can be fixed. An illustrative description is given of the application of these criteria to a situation involving both radial and angular variations in the local k .

A general theory of reactor monitoring applicable to problems of this type is outlined in the final section.

Introduction

In the operation of nuclear reactors it is necessary not only that the total power remain within design limits, but also that the local power in various portions of the reactor remain within design limits at those locations. The power, or some quantity closely related to it, is usually observed at a finite number of locations in the reactor. The power at all other points is estimated by use of reactor theory based upon the known or estimated composition of the reactor.

This estimation process becomes especially significant in large reactors, both because small variations in the reactor properties can produce disproportionately large power shifts and because the large size may provide greater scope for local variations in composition, temperature, and positioning of the reactor components.

In this paper we consider the effect that these possible variations have upon the problem of monitoring the performance of a reactor in a quasi-steady state. Kinetic situations are not considered, but the arguments here used appear capable of extension to include variations in time.

Description of the Criteria

In large fast reactors of modular design, each module characteristically consists of a core surrounded by a blanket and possibly some reflecting material. A number of these modules are assembled closely to form the complete reactor. The modules are loosely coupled neutronically, and each module would be nearly critical in an isolated condition. In such a situation, small deviations in the nuclear properties of individual modules could produce large variations in the flux and power distributions in the total reactor. It is clear that a suitable monitoring process must be carried out for each individual module and that it must be possible to use the results in the operation of a control system of similar distribution. Only in this way can a desirable power distribution be maintained.

In large reactors of nonmodular design a similar situation exists. Portions of the core would be nearly critical in isolation and are weakly coupled to other portions. The maintaining of a balanced power distribution will require similar monitoring and control facilities. However, there is no naturally occurring module which can be used as a basis for design of the monitoring system. Any portion of the large core could be regarded as a core surrounded by a high-quality reflector and nearly critical by itself. Some additional criteria must therefore be introduced in order to determine the number and spacing of the monitoring points.

In many reactors a large number of temperature monitors are placed in the outlet coolant stream near the outlets of individual channels for the combined purpose of monitoring power and local coolant flow. It might therefore seem that the monitoring question has been answered by providing an abundance of points. However, independent monitoring of local power is still required in order to separate power variations from flow variations. Also, an abundance of data requires a reduction process which recreates the problem on a new level.

The criteria for the selection of monitoring points here proposed are related to the uncertainties attached to the interpolation or extrapolation of the measured values to obtain values of the quantity of interest at nonmonitored points. Some uncertainties exist with

respect to temperatures, compositions, and positions of materials in local regions of a reactor, and these produce corresponding uncertainties in the local neutronic properties. Normally, some upper and lower bounds can be placed on these uncertainties but, within these bounds, local properties may vary from place to place in such a manner that overall criticality is maintained.

Local increases in the reproduction factor are compensated by local increases in power, which in turn lead to increases in net neutron leakage away from that region, thus reestablishing a steady state.

When the power is measured at a number of monitoring points in a reactor, the expected power at all other points can be determined based upon the normal or expected properties of the reactor medium. The reactor design will provide for the effects of such a power distribution when all of the measured values are within the design limits. The design will also provide for some local excesses above the expected values.

Thus the two considerations used in establishing monitoring locations are: first, the expected deviations in the local properties of the reactor medium away from their normal values; second, the maximum undetected increases in power above expected levels which the design can tolerate. Since, in general, these power uncertainties increase when the distance between monitoring points increases, these considerations may be used to determine the monitor spacing.

In applying these considerations to an actual reactor design, detailed calculations would probably be carried out by multigroup methods to determine the power uncertainties from the property uncertainties. It should be noted, however, that existing monitoring devices provide a signal proportional to a weighted sum over the energy groups. Thus, the full power of a multigroup calculation is lost in the absence of a detector able to produce values of the flux in individual energy groups. Such detectors are not beyond the bounds of possibility,⁽¹⁾ and further exploration of the concepts here presented may indicate its usefulness for applications to power reactors.

For an exploratory study of the concept here presented we have chosen to avoid the complexities of multigroup calculations in the belief that a one-group representation of the quantity determined by the monitoring devices would illustrate the main features of the method.

Two cases have been studied: a large cylindrical (pancake) reactor treated as a bare core, and the same core treated as perfectly reflected so that a flat power distribution is its normal condition. Figure 1 shows the undetected power deviations as percentages of peak power that result from local leakage increases as per-

FIG. 1 Flux Variation in Bare Reactor.

cent of normal radial leakage for various detector arrangements. The solid lines indicate flux variations resulting from angular variations of the reactor properties with monitors equally spaced on a single ring at about 0.5 to 0.6 of the reactor radius. The curves were calculated by use of a variational method described in the Appendix. Radial variations for the same conditions are indicated by the dashed R_0 curve.

Addition of a detector at the center of the reactor will strongly reduce the radial variations. The amount of the reduction is dependent both upon the maximum local increases and decreases of reactivity to be expected. However, some general approximate arguments indicate that, with a central detector, radial variations will again exceed the angular ones when more than about eight monitors are used on the ring. For patterns containing more than nine detectors, the locations should be approximately equally spaced over the reactor. Methods for treating this type of problem are outlined in the next section.

Results for the perfectly reflected core with a normal flat power distribution are shown in Fig. 2. In the previous case, the requirement that the flux vanish on the outer extrapolated surface provided an additional monitoring surface, at no extra charge. Loss of this constraint in the present case is reflected in the much larger variations escaping detection by a given monitoring pattern. As with the first case, addition of a central monitor would reduce the radial variations, but beyond the octant monitoring pattern, a single ring is not the best distribution. The curves with higher numbers of monitors have been included only for the purpose of indicating the trend of improvement for a ring configuration.

FIG. 2. Flux Variation in Flat Reactor.

A More General Monitoring Theory

A number of methods exist for fitting calculated reactor flux or power distributions to values measured at a selected number of points in a reactor. From the standpoint of design and use of monitoring systems, it is an advantage to have the measured values appear explicitly in a linear manner in the fitted expression. Such a method is available and can be applied in general to the type of problems we are considering.⁽²⁾

If, for example, the flux is measured at a number of locations r_i in a reactor to obtain the values ϕ_i , then the flux throughout the reactor can be obtained from the expression

$$\phi(r) = \sum_i \frac{\phi_i}{\psi_i(r_i)} \psi_i(r), \quad (1)$$

where the ψ_i are a species of Green's functions, different from, but related to, the ordinary Green's functions and having the following properties:

The radial variations in flux were computed by use of a variational method described by Becker,⁽³⁾ which applies the least-squares method to the error in satisfying the flux equation. The functional to be minimized is

$$\varepsilon^2 = \int (\nabla^2 \phi + B^2 \phi)^2 dV. \quad (3)$$

The problem is one in which alternate sectors of the reactor have values of B^2 above and below the value B_0^2 required for criticality of a homogeneous reactor of the same size in such a way that criticality is maintained. We are here concerned only with the sectors having increased buckling, since these will be the

They are solutions of the equation satisfied by the flux:

$$H\phi = 0. \quad (2)$$

They satisfy the boundary conditions satisfied by the flux on the external real boundaries of the reactor and

$$\psi_i(r_j) = 0 \quad \text{for } j \neq i.$$

These conditions allow the ψ_i to be constructed, usually by machine computation. If there is more than one monitoring point, the function ψ_i will have a positive singularity at r_i . Choice of a cutoff radius about r_i is arbitrary and may be chosen differently for the different points as a means of assigning different weights to the measurements.

For our purposes, H is to be regarded as consisting of a normal part H_0 and a part representing the maximum expected deviations in the reactor properties, δH . Correspondingly, $\delta \psi_i$ will be generated which can be used to determine $\delta \phi(r)$ between the measured points.

The smoothness of the $\phi(r)$ constructed from Eq. (1) with a given set of measurements is an indication of the accuracy with which Eq. (2) represents the actual reactor situation. Lack of a good fit will be evidenced by discontinuities in the gradient of ϕ at the points r_i . Adjustments in H would then be required to obtain smoothness. The adjustments will in general not be uniquely determined, and some δH will remain to contribute a $\delta \phi$ for areas between the monitoring points.

It is not beyond the realm of possibility that such computations could be continuously performed by computer in the monitoring of an operating reactor and the results displayed for the guidance of the operators.

The general theory also appears capable of extension to kinetic situations though the inclusion of the time variable.

Appendix

higher-power regions. Each sector is π/p in angular width and the trial function taken for ϕ is

$$\phi = J_0(\alpha_{01}r) + A J_p(\alpha_{p1}r) \cos p\theta, \quad (4)$$

where α_{ij} is the j th root of J_i . Inserting (4) in (3) and carrying out the variational process with respect to the parameter A , we obtain

$$A = \frac{\frac{1}{2} (B^2 - \alpha_{01}^2) \int_0^1 J_0(\alpha_{01}r) J_p(\alpha_{p1}r) r dr}{\pi (\alpha_{p1}^2 - B^2) \int_0^1 J_p^2(\alpha_{p1}r) r dr}. \quad (5)$$

For the normally flat reactor the trial function was

$$\phi = 1 + AJ_p(\beta_p r) \cos p\theta, \quad (6)$$

where β_{p1} is the first maximum of J_p . This resulted in

$$A = \frac{4}{\pi} \frac{B^2 \int_0^1 J_p(\beta_{p1} r) r dr}{\beta_{p1}^2 - B^2 \int_0^1 J_2^p(\beta_{p1} r) r dr}. \quad (7)$$

Thanks are due to Dr. H. C. Thacher for computation of the Bessel function integrals.

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Discussion

Mr. Thompson (MIT): I wonder if you've tried to apply this model to any thermal reactors, first, and second, have you thought about extending this to locations of control rods?

Mr. Gast: Yes, as a matter of fact, although I put this in as a paper for a Fast Reactor Symposium, it was really sparked by some problems that had occurred at Hanford over a number of years. I think it might be simpler to apply to thermal reactors in the sense that in most of them one could do this adequately with at most two-group calculations; I am not sure this would be the case with fast reactors. If there is a control rod near a monitor, one has to include that in his operator description in order to get any kind of a proper solution to the problem. It does, I think, raise the question of whether it might not be worthwhile to

attach some special computers to the reactor control room which might develop this kind of information in a continuous way for operators.

Mr. Godts (WAPD): Do you feel that this would be applicable to finding the transient-flux distribution with the operational limits?

Mr. Gast: In a formal sense, yes. One can include the time variable and go through the whole thing again. This becomes much more complicated if you are thinking of transients in which you may have boiling and that kind of thing. It gets to be very difficult.

Mr. Godts: I was thinking of the inclusion of an on-line computer.

Mr. Gast: That might possibly be helpful.

Mr. Burton (Risley): This does seem to be a very useful technique for the thermal reactor. But in the present state of our knowledge of the fast reactor, as we have seen throughout the conference, what is worrying us is the local blockage. Do you see any prospect for improving your resolution to do this?

Mr. Gast: I think that is a problem that is somewhat beyond the formalism that is included here.

Mr. Burton: Yes, but you're liable to do something formally by considering, for example, the extent that the sodium boiling of voiding would reproduce on these monitors. It might be possible.

Mr. Gast: I think that might be possible. I think, however, that a direct approach, such as monitoring temperatures or looking directly for hydraulic effects, might be better if you are looking for blocked tubes.

Mr. Burton: Well, in engineering terms, this is what we are finding particularly difficult. One really wants to achieve a minimum number of monitors, and this is one way of integrating the effect. It does have merit, in principle.

General Discussion

Mr. Chernick (BNL): I have a question I first raised two years ago. At that time I wondered whether it might not be better in the long run, in getting Doppler coefficients, to set up an expensive facility, with good heating sources and everything that goes with it, as they have done for thermal reactors. I would like to get the opinion of the experimental people as to whether they are satisfied with what has been done and whether it is perhaps not too late to still ask for an expensive attack on this problem.

Mr. Greebler (GE): I think the SEFOR program is an answer.

Mr. Chernick: That's what I was told last time.

Mr. Fischer (ANL): I don't think you would want a more expensive answer than SEFOR. But there are other measurements, which you have seen even today. The question is what do you want to do.

Mr. Chernick: SEFOR is an answer in an integral way.

Mr. Fischer: I think they are trying in one integral way to make this kind of a measurement. But I think Wheeler, this morning, gave you a first British pass at the kind of a thing you are asking for. And Till has mentioned that he and Lewis are planning a small clustered-pin experiment.

Mr. Chernick: Is this going to give us the answer? With the kind of facility I am talking about we would have some certainty within some given time of having the answer, not only to the U-238 Doppler effect, which is fairly well pinned down already, but also to the question of the U-235 and plutonium Doppler effects.

Mr. Fischer: The fact that the Doppler effect for U-238 is negative, large and pretty well known is the big thing that you need to know. I think we have to go on now asking—as Till will in ZPR-6 in the near future—what happens to the Doppler coefficient when you remove sodium? I think we should build ZPPR as rapidly as possible to find out what the change in the Doppler coefficient is when you put in things like plutonium-240, for which you really can't expect to have good cross sections in three to five years. You want to do the dirty plutonium Doppler experiments. You'd like to do something about fission products. I think you can do all of these things with the criticals rather nicely. SEFOR will cover only one specific composition.

Mr. Greebler: I was just going to say something about the temperature effect. It was mentioned that

the Pu-239 effect may be near zero at room temperature, but may come up appreciably because of the influence of Doppler worth on the resonances. It is important that we be able to cover the full range of temperatures in a reactor at full power and perhaps under transient conditions above power. We may see some surprises, particularly in regard to the Pu-239 effect in that area.

Mr. Fischer: In this area of the working criticals, both at AI and at Argonne, we are reaching very rapidly to significantly higher temperatures. I think this will show up pretty nicely.

Mr. Thompson (MIT): Speaking as a naive experimentalist, it appears to me that perhaps both approaches may be required. The small-sample technique allows you to make corrections for such things as expansion, and the in-out type of measurement allows you always to zero back in a way which I think will be found to be quite difficult with the large integral experiments of the SEFOR type. With the SEFOR you are very likely to be actually measuring a combination of Doppler plus expansion and overall core expansion plus individual rod expansion. Therefore I think in the long run you will need both.

Mr. Chernick: One other thing that hasn't been mentioned is the question of spectrum effects. You may know what the delta signal is, but without such a facility it is going to be extremely difficult to determine spectrum effects on the Doppler.

Mr. Till (ANL): I think that the results that I showed this afternoon would indicate that, although the signal for the small-sample experiments is small, we have techniques that can measure the signal to the sort of accuracies that we need. Further, the small-sample technique does allow us a good way of separating these Doppler and expansion effects, and I think this is a significant point.

Mr. Zartman (USAEC, Washington): It seems to me that one has to be very careful with these integral experiments in being able to differentiate, just as has been mentioned by others, between the various effects that you sum up in these large integral experiments. I would like to see, however, one thing in the other experiments taken into greater consideration than I think anybody has, and that is the spectrum in which the measurements are being made. I don't think, or at least I am not convinced, that the actual spectrum in which these measurements are made is known well enough and compared or taken into consideration, in

the comparison between the results of the different people that have been reporting. Isn't this one thing that ought to receive a much greater attention than it has in the past? I am not too much concerned that we won't be able to interpret or measure the small signals. I think there has been a good deal of improvement along this line. There is a need for further ingenuity in the preparation of the samples and in attempts to separate expansion effects from true reactivity changes of the material. I would like to hear a little more about the spectrum aspect.

Mr. Fischer: I think we all agree with you that spectrum is a very important quantity in trying to analyze what we claim to have measured. For years at Argonne on our assemblies we have been trying various devices to measure spectrum. I would say the best news about spectra is essentially a bulletin out of ZPR-6. Ed Bennett has been able to measure down into the kilovolt region, which is a tremendous achievement. Recently at San Francisco, he reported a comparison between measurement and calculation for the current 4Z assembly of ZPR-6. Since then he has worked on the thermal source reactor in a Snell block and has been able to see a small undetected experimental difficulty he hadn't known about at San Francisco. When he made a correction to the numbers he reported at San Francisco, there was surprisingly good agreement from his results of the analysis of the spectrum, done by a proton-recoil technique, and the spectrum we calculated using ELMOE corrected set 224. The disagreement, which was almost a factor two in the lowest-energy region, is now very largely removed with Bennett's latest correction. The results are surprisingly good. This I think is our best hope for spectrum. There is some sandwich work with lithium detectors in Idaho, probably the next best thing currently going on in spectra.

Mr. Greebler: I thought that the time-of-flight technique provided the best method of measuring the spectrum. Does anybody have a feeling as to how good the advanced proton-recoil methods compare with the time-of-flight procedures? Further, is there need for time-of-flight equipment with critical assemblies?

Mr. Thalgott (ANL): It seems to me that the time-of-flight technique works best for the low-energy

flux, and there may be some difficult problems at the higher energies in the few-kilovolt range. I think that it will be necessary for us to pursue these things, as the British have been doing, and also at Karlsruhe. The second point—concerning the spectrum. I think we must be able to reconcile calculation with experiment as far as the spectrum is concerned if we are to calculate the Doppler effect in a reactor. However, the measurements in critical experiments are made in the spectrum that is associated with that composition, and the calculations are either right or wrong. The neutrons behave themselves in the critical as they should, and the measurements should then reflect the proper spectrum for this mixture of materials. Two years ago I agreed with Chernick that an elaborate facility might very well be necessary, even though there is some difficulty with respect to measurements in such a facility that I don't quite know about. The measurements in critical experiments hadn't been made and I thought they might turn out to be extremely difficult. The measurements reported today seem to be reasonably consistent, and it is gratifying to see that such measurements can be made. I still am not sure that this answers the whole problem, but I have much more confidence now than I did two years ago.

Mr. Chernick: We have to be careful how we interpret any type of experiment, including large-scale experiments. I am impressed with what was done at Hanford on measuring Dopplers in their PCTR and what was done at Winfrith in measuring assemblies where the fuel elements had been heated with gas and so on. So there is some precedent in thermal reactors indicating the flexibility and the high probability of being able to get good data from such a measurement. There is one thing you mentioned I agree on—the time-of-flight method. This is an extremely good tool for going up at least to 10^4 eV. I am impressed with what the Russians have already done and reported at the Geneva conference with their IBR reactor with various filters—iron, stainless steel, and things like that. They seem to get extremely great detail in the spectrum in this energy range. Beyond 10^4 eV the resolution gets very bad. I think that new techniques are going to be needed to determine the spectrum in that range. But for the extremely important range from about 100 to 10^4 eV I think that good time-of-flight equipment is very useful.

Session VII-A
October 14, 1965

Maximum Accident and Containment

Chairman: W. J. McCARTHY, JR.
Power Reactor Development Corporation

Secretary: G. J. Fischer
Argonne National Laboratory

A Critical Review of the Properties of Materials at the High Temperatures and Pressures Significant for Fast Reactor Safety

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The equation of state plays an important role in calculations of the course of a hypothetical fast reactor excursion, for it serves as the link between the neutronic reactions and the dynamic behavior of a core which leads to ultimate shutdown. Determination of the energy appearing as kinetic energy provides insight into the potential damage which might result from such an excursion. The principal relations necessary for such calculations are the pressure as a function of energy and volume for hydrodynamics calculations, which give the time variation of geometry (including purely elastic deformations such as thermal expansion), and the temperature as a function of energy and volume, to give the proper temperature dependence of nuclear cross sections (with special emphasis on the Doppler coefficient).

Several techniques for determining these relations have been used, and will be critically reviewed and compared. In addition, some future directions will be indicated.

Energy releases estimated for hypothetical nuclear accidents could lead to temperatures and pressures beyond those possible with static experiments and present technology. Resort must therefore be made to theory and correlation for the estimation of these physical properties at extreme conditions.

The equation of state as applied to fast reactors has been reviewed briefly by Okrent.^(1, 2) Effort has been concentrated on the application of the law of corresponding states⁽³⁻⁷⁾ and on empirical relations which were fit to reactor excursion data.

It should be noted that the energy to heat uranium from room temperature to the boiling point is 0.65×10^{10} ergs/gm, whereas the heat of vaporization at the boiling point is about 1.7×10^{10} erg/gm.⁽⁸⁾

Corresponding States

The well-known law of corresponding states⁽⁷⁾ implies that substances are characterized by their thermodynamic critical properties: critical temperature T_c , critical density ρ_c , (or critical volume V_c), and critical pressure P_c , and that materials at the same reduced temperature, pressure, and volume will have similar behavior.

This approximate relation has been used by several investigators⁽³⁻⁵⁾ to first estimate the critical properties of reactor fuel materials and to then use generalized correlations⁽⁷⁾ to predict their equation of state.

Elrod^(5, 6) has used empirical information on low-boiling-point nonmetallic liquids to estimate $T_c = 14,200^\circ\text{K}$ and $P_c = 5,130$ atm for uranium. Brout⁽³⁾ accepted his estimate of T_c but used the empirical relations $V_c/V_{\text{solid}} = 3.11$ and $P_c V_c / RT_c = 0.3$ for many substances to obtain $V_c = 40$ cm³/mol and $P_c = 8650$ atm.

Gross⁽⁹⁾ has recently suggested application of the entropy of vaporization vs reduced temperature (with mercury as the only well-measured metal) as an alternative method of estimating critical temperature. The application of the law of "rectilinear diameters" was also suggested, and the two methods lead to consistent agreement for all but the transition metals, such as Fe, Co, and Ni. The latter technique will be discussed below.

Since the first edition of their text, which was used by Brout and others, Hougen and Watson⁽⁷⁾ found it necessary to use a third parameter, the critical compressibility $Z_c = (pV/RT)_c$, to correlate accurately the thermophysical properties of ordinary substances. The range of their correlations is for $0.24 < Z_c < 0.30$, which corresponds to most of the inert gases and organic materials upon which the correlations are based.

Table 1 shows that many materials, including mercury (the only heavy metal for which experimental data exist to the critical point) do not fall in this range and would not be expected to agree well with the correlations.

Empirical rules for estimating critical properties cannot be relied upon. One such rule states that the critical temperature is 1.5 times the absolute boiling temperature T_b . For mercury, bismuth chloride, and cesium T_c/T_b is 2.75, 1.66 and 2.0, respectively. Similar rules for volumetric behavior which have been used⁽¹⁰⁾ are equally unreliable.

Brout, in applying the corresponding-states correlation, comments that the agreement is not good for argon gas at high pressures, although this is one of the

materials on which the correlation is based and which has a Z_c in the range of the correlation. In his consideration of heat capacity, Brout rejects a corresponding-state principle based on argon and neon because they are unlike uranium, although he is willing to accept the generalized correlations which are not based on any metallic fluids.

For the materials (CO_2 , CH_4 , NH_3 , etc.) on which the generalized correlation used by Brout is based, T_c is so low that ionization is negligible. Calculations⁽¹¹⁾ from the Langmuir-Saha equation show, as was rationalized by Nicholson,⁽¹²⁾ that ionization is not important until 3 or 4 times the critical temperature. Morris⁽¹³⁾ has considered the effects of ionization in an attempt to shore up the law of corresponding states for metals. His approach is to provide a modification to the Grüneisen equation

$$P = \rho\gamma(\rho)[E(\rho, T) - E_0(\rho)],$$

where

P is the pressure;

ρ is the density;

E is the internal energy;

E_0 is the Grüneisen energy parameter;

γ is the Grüneisen ratio $\alpha/\rho c_p \kappa_s$;

α is the volumetric thermal expansion coefficient, $-(1/V)(\partial V/\partial T)_P$;

c_p is the constant pressure heat capacity;

κ_s is the adiabatic compressibility $(1/V)(\partial V/\partial P)_s$, by considering the effect of the electrons on pressure and heat capacity.

In a study of experimental data on shock compression of reduced-density uranium Skidmore and Morris⁽⁸⁾ used the law of corresponding states without modification.

Although the law of corresponding states appears to be inapplicable for high accuracy it should be useful as a guide until more satisfactory estimation procedures can be tested.

As a temporary expedient to explore the temperature, pressure, and density ranges of interest, the density-temperature relation for the saturated liquid-vapor region has been estimated (up to the critical point) for some materials of reactor interest by the "rectilinear law" method.⁽⁹⁾ This method assumes that one-half

FIG. 1. Density vs Temperature for Uranium

the sum of the liquid and vapor densities is linear up to the critical point. This empirical law has been substantiated for quantum fluids, such as hydrogen and helium; polar liquids, such as ammonia and hydrogen fluoride; a fused salt, BiCl_3 , and metals, such as mercury, cesium, rubidium and sodium,⁽¹⁴⁾ and many ordinary fluids.

By the use of data for the density of the liquid, as a function of temperature along the saturation curve, and estimates of the vapor density from a vapor pressure curve or its extrapolation, the coexistence curves can be estimated. The technique is illustrated in Fig. 1 for uranium. The constants estimated for other reactor materials are given in Table 2. Care must be taken in the use of these estimates since they require extrapolation of experimental data far beyond the region of measurement. This is particularly true for vapor pressures.

A parameter study has been made⁽¹⁵⁾ to investigate the effects of uncertainties in experimental data on the properties of UO_2 . Rectilinear-law curves were constructed for several vapor pressure equations, critical compressibilities, coefficients of expansion of the liquid, and heat of fusion. The results tend to cluster in a fairly narrow range around the value obtained for the "best" values chosen for the needed data. The results are presented in Table 3.

Figure 2 shows the vapor pressure curves of UO_2 used for the calculations. The code ALT is used for the Ackerman⁽¹⁶⁾ low-temperature, AHT for Ackerman⁽¹⁶⁾ high-temperature, O for Ohse,⁽¹⁷⁾ and BMI⁽¹⁸⁾ for an equation reported by Battelle Memorial Institute. An estimated heat of fusion of 3.3 kcal/mol at the melting

TABLE 1. MEASURED CRITICAL PROPERTIES

	T_c , °K	ρ_c , g/cm ³	P_c , atm	$Z_c = \frac{P_c}{RT_c \rho_c}$
Mercury	1733 ± 50	4.70 ± 0.20	1587 ± 50	0.477
BiCl_3	1178 ± 5	1.210 ± 0.006	118.1	0.318
Acetonitrile	547.9	0.237	47.7	0.19
Deuterium	38.3	0.0151	16.3	1.37

TABLE 2. ESTIMATED CRITICAL PROPERTIES OF REACTOR MATERIALS

Material	Critical Temperature T_c , °K	Critical Density ρ_c , g/cm ³	Critical Pressure P_c , atm	Critical Compressibility $Z_c = (P_c/RT_c) (M/\rho_c)$
Uranium	12,500 $\pm$ 700	3.22 $\pm$ 0.36	6860 $\pm$ 1500	0.493 $\pm$ 0.15
Plutonium	10,000 $\pm$ 500	1.414 $\pm$ 0.380	1800 $\pm$ 350	0.371 $\pm$ 0.020
UO ₂	9,115	1.59	1230	0.27 ^(a)
Iron	7,000	1.22	3,060	0.245
Sodium	2,780	0.17	440	0.285
Beryllium	8,600	0.435	14,000	0.411

^(a) Assumed.TABLE 3. RANGE OF ESTIMATED CRITICAL CONSTANTS OF UO₂

	T_c , °K	P_c , atm	ρ_c , gm/cm ³
High Temperature	12,780	493	0.127
Low Temperature	4,910	1037	3.47
Most Probable	9,115	1230	1.59

point is included. The effect of the choice of vapor pressure equation is shown in Fig. 3 with the "most probable" heat of fusion of 3.3 kcal/mol, liquid expansion coefficient of $1.05 \times 10^{-4}/^\circ\text{K}$ and critical compressibility of 0.27 used. The critical temperatures found range from 5100°K for the AHT to 9100 for the ALT vapor pressure curves.

In Figure 4 is demonstrated the effect of varying the critical compressibility from 0.20 to 0.40 with the AHT vapor pressure relation and the "most probable" constants. Critical temperatures estimated are about 5% lower and 7% higher for $Z_c = 0.20$ and 0.40, respectively, as compared to $Z_c = 0.27$.

The choice of the value for the latent heat of fusion is seen in Fig. 5 to make a major change in the estimated critical temperature. The values used represent several different correlations of heat of fusion.

In Fig. 6 the effect of the slope of the curve of liquid density vs temperature is investigated, using the error limits of Christiansen's⁽¹⁹⁾ measurements (which covered a very small temperature range compared to those considered here). The net change is less than $\pm 2\%$, well within other errors in applying the rectilinear-diameter method.

An additional technique which can be used is comparison of the entropy of vaporization⁽⁹⁾ vs. reduced temperature using a known material as a standard. This is illustrated in Fig. 7 for some common substances. Here the entropy of vaporization is calculated from the heat of vaporization and temperature or by use of the rectilinear-law curves and the Clapeyron equation. Figure 8 shows the effect of the choice of vapor pressure equation with the choice of "best" constants. Some confidence is gained in the choice of the ALT curve and the "best" constants since the entropy curve

FIG. 2. Vapor Pressure of UO₂

derived from them falls between the curves of mercury and the common substances. This may be due to coincidence; any additional experimental data would help justify these choices. Agreement with the results of Meyer and Wolfe⁽²⁰⁾ is within the accuracy of their assumptions. The results of the variation of parameters used in this study shows care must be used in applying this method.

In general, critical temperatures derived from the entropy of vaporization for metals using mercury as the "stand-in" agree well with those from the rectilinear-law plots except for the transition metals Fe,

FIG. 3. Effect of Vapor Pressure for UO_2 .

FIG. 4. Effect of Critical Compressibility for UO_2 .

FIG. 5. Effect of Heat of Fusion for UO_2 .

FIG. 6. Effect of Liquid Expansion Coefficient for UO_2 . FIG. 7. Effect of Material and of Critical Compressibility for UO_2 . FIG. 8. Effect of Vapor Pressure Curve for UO_2 .

Co, and Ni. The disagreement is explained on the basis of the d-band electrons and the rectilinear-law estimate is favored.

Vapor Pressure

The vapor pressures of reactor fuel materials are important in determining the equilibrium pressure as a

function of temperature below the thermodynamic critical point. Since evaporation is a rate-limited process it is possible that a phase transition would not occur in a sufficiently rapid transient.

A selection of vapor pressure equations for substances of interest as reactor fuels is given in Table 4. In gen-

eral, these equations were obtained by fitting experimental data taken by effusion, transpiration, or mass-spectrometric methods at pressures below 1 mm Hg. They are therefore sensitive to the details of stoichiometry, vacuum, and the materials of containment used for the experiment. For reactor safety calculations, these equations must often be extrapolated six to nine orders of magnitude higher in pressure than experimentally measured. Such extrapolations are often reasonably accurate for ordinary substances, but at higher temperatures the effects of dissociation, excited states, and ionization would be expected to complicate the behavior for the reactor fuel materials.

The first vapor pressure equation for PuC in the table was developed by Palmfreyman and Potter⁽³⁰⁾ from results of a transpiration experiment and is based on five experimental determinations of vapor pressure at four temperatures with only 65°C in range. These are shown in Figure 9. As the maximum weight loss in these experiments was 14 mg, the presence of 5 ppm oxygen in the argon carrier gas could have led to significant error, since at the temperatures of the experiment PuO₂ is more volatile than PuC.

The heat of vaporization (which is related to the slope of the curve) from this work is out of line with values for other actinide carbides and with the results of recent measurements at Los Alamos,⁽³¹⁾ and therefore must be suspect. This work⁽³⁰⁾ was used, without critical examination, as the basis for the estimation of equation-of-state information by Kubit⁽¹⁰⁾ along with a questionable rule for estimating critical volumes.

Critical temperatures and pressures estimated by Kubit are sufficiently low as to be within experimental reach in steady or transient measurements. Such experiments are under consideration by the author and C. E. Dickerman.

The lack of agreement by several investigators study-

TABLE 4. VAPOR PRESSURES OF REACTOR FUEL MATERIALS

Material	Reference	log P(atm)	Phase
U	21	5.702 - (23,300/T)	liquid
UO ₂	16	22.805 - (33,115/T) - 4.026 log T	solid
UC	22	7.219 - (32,910/T) ^(a)	solid
UN	23	8.913 - (29,540/T) + (5.27 × 10 ⁻¹⁸ T ⁵) ^(b)	solid
US	24	7.666 - (29,970/T)	solid
Pu	25, 26	5.014 - (17,587/T)	liquid
PuO ₂	27, 28, 29	8.129 - (27,900/T)	solid
PuC (1)	30	22.3 - (51,000/T)	solid
PuC (2)	31	5.096 - (18,561/T)	solid

^(a) Total pressure above UC.

^(b) Decomposition pressure of nitrogen above UN.

FIG. 9. Vapor Pressure of PuC.

ing the same substance, UO₂,⁽¹⁶⁻¹⁸⁾ is indicative of the importance of careful technique, the necessity for characterization of the sample, and the residual gases in high-vacuum systems in order to obtain relations which can be extrapolated over wide temperature ranges reliably. In this most widely studied reactor fuel material the effects of stoichiometry are marked. Both hypo- and hyperstoichiometric UO₂ have higher vapor pressures and tend to vaporize to the stoichiometric material under equilibrium conditions.⁽³²⁾ Recent experiments at Argonne⁽³²⁾ and elsewhere tend to confirm Ackerman's⁽¹⁶⁾ low-temperature equation.

Equations of State

The Grüneisen equation^(33, 34) which relates energy, density, and pressure can be written in the form

$$P(\rho, T) - P_k(\rho) = \rho\gamma(\rho)[E(\rho, T) - E_k(\rho)], \quad (1)$$

where

P = pressure, dyne/cm²;

T = temperature, °K;

ρ = density, g/cm³;

$\gamma\rho$ = Grüneisen ratio;

E = specific internal energy, erg/gm,

and P_k and E_k are the pressure and specific internal energy as functions only of density at 0°K, and are related by

$$P_k = \rho^2 \frac{\partial E_k}{\partial \rho}. \quad (2)$$

Now if Eq. (1) is rearranged and Q^* is defined by

$$\gamma(\rho) \left[E_k(\rho) - \frac{P_k(\rho)}{\rho\gamma(\rho)} \right] = E(\rho, T) + Q^*(\gamma - 1), \quad (3)$$

then

$$Q^* = \frac{\gamma}{\gamma - 1} E_k(\rho) - \frac{P_k(\rho)}{\rho(\gamma - 1)} - \frac{E(\rho, T)}{\gamma - 1} \quad (4)$$

$$= \frac{\gamma}{\gamma - 1} E_k - \frac{\rho}{\gamma - 1} \frac{\partial E_k}{\partial \rho} - \frac{E}{\gamma - 1}.$$

Equation 1 may be written in the form of the Bethe-Tait "threshold equation of state" ⁽³⁵⁾

$$P(\rho, T) = \rho[\gamma - 1][E(\rho, T) - Q^*], \quad (5)$$

where $P = 0$ for $E < Q^*$.

It can be seen that, in general, Q^* is not a constant in spite of its utility in providing a physical feeling for the buildup of pressure when the energy is greater than Q^* .

The limiting value of the Grüneisen coefficient for a monatomic substance at sufficiently high temperatures is that of an ideal gas:

$$\lim_{T \rightarrow \infty} \gamma = \frac{R}{C_v} = \frac{2}{3}.$$

Consideration of this limiting lower bound indicates that $\gamma - 1$ could be negative in the threshold equation of state, and pressure would become negative for increasing energy and temperature. Caution should be exercised in using that relation other than as a curve fit over limited ranges of energy and density.

In the first formulation of the computer code AX-1 ⁽³⁶⁾ the equation

$$P = a + bT + c\rho \quad (6)$$

was used along with the caloric equation

$$\frac{\partial E}{\partial T} = C_v = A_{cv} + B_{cv}T, \quad (7)$$

with the constants recommended by Stratton for uranium:

$$\begin{aligned} a &= 0.545649 \text{ megabar;} \\ b &= 278.46/\text{keV;} \\ c &= 0.02873 \text{ cm}^3/\text{gm;} \\ A_{cv} &= 12.163 \text{ cm}^2/\mu\text{sec}^2\text{-keV;} \\ B_{cv} &= 5.78 \times 10^3 \text{ cm}^2/\mu\text{sec}^2\text{-keV} \end{aligned}$$

for temperatures measured in kilovolts (1.16×10^7 °K) and pressures measured in megabars (0.9869×10^6 atm). Jankus ⁽³⁷⁾ has shown the thermodynamic inconsistency of Equations 6 and 7 by means of thermodynamic identities. The constants a , b , and c are also inconsistent at the critical point.

Equation (6) can be compared with Brout's work in reduced form or directly. This is shown in Fig. 10 and 11. The van der Waals equation, which generally gives good agreement above the critical point, is also shown for comparison. It can be seen that Stratton's equation is at variance with Brout's calculations and the van

der Waals equation, which show reasonable agreement. If the results of Brout's calculations and Stratton's equation are plotted on an absolute basis, normalized to a common point as in Fig. 11, the disagreement persists but is not as marked. Also shown is the linear-energy-threshold model used in the Bethe-Tait calculation with a value of the Grüneisen ratio taken as 2.

Constancy of the Grüneisen ratio has also often been assumed. Only Nicholson ⁽¹²⁾ has attempted to include the effect of density, and he assumes a linear relation between this ratio and density. It can be seen from Fig. 12 that the slope and values chosen by Nicholson do not agree with extrapolated values of γ from experimental shock compression data of Persson and Persson. ⁽³⁸⁾ The effects of reduced density are presently under consideration and these will be reported later.

Shock Equation of State

Generation of extremely high pressures by shock compression provides important information on the equation of state ^(39, 40) cogent to reactor safety. The basic equation necessary to interpret the measurements is the conservation of mass across the shock wave:

$$\rho_0 U_s = \rho_1 (U_s - U_p), \quad (8)$$

FIG. 10. Estimate of Reduced Pressure of Uranium as a Function of Reduced Temperature.

FIG. 11. Estimates of the Pressure of Uranium above the Melting Point.

FIG. 12. The Grüneisen Ratio for Uranium as a Function of Density.

where

$$\begin{aligned} \rho_0 &= \text{density before shock;} \\ \rho_1 &= \text{density after shock;} \\ U_p &= \text{particle velocity;} \\ U_s &= \text{shock velocity.} \end{aligned}$$

The velocities are usually measured by photographic techniques or by closing of electrical circuits. The law of conservation of momentum is similarly given by

$$P_0 - P_1 = \rho_0 U_s U_p, \quad (9)$$

where P_0 and P_1 are the pressure before and after the shock, respectively. Conservation of energy across the shock leads to

$$P_1 U_p = \frac{1}{2} \rho_0 U_s U_p^2 + \rho_0 U_s (E_1 - E_0). \quad (10)$$

These can be combined to give the Rankine-Hugoniot equation:

$$E_1 - E_0 = \frac{1}{2} (V_0 - V_1) (P_1 + P_0), \quad (11)$$

where $V = 1/\rho$ is the volume.

Experimental measurements in regions where no phase transitions occur usually provide a linear relation^(39, 40) between shock and particle velocity:

$$U_s = C + S U_p.$$

This holds for experiments with high-density $U^{(8, 38)}$ and is approximately true for low-density material,⁽⁸⁾ although the investigators fitted a different form of equation to the results from porous U . The data for uranium are shown in Fig. 13. When these data are combined with Eqs. 8 and 9, an expression is obtained for the pressure-density behavior on the Rankine-Hugoniot curve or shock-Hugoniot, as it is called:

$$P_H = C^2 \rho_0 \left(1 - \frac{\rho_0}{\rho}\right) / \left[1 - S \left(1 - \frac{\rho_0}{\rho}\right)\right]^2. \quad (12)$$

Although C and S are constants for fitting the data, in the limit of zero pressure, C is the velocity of sound, and experimental data are in good agreement with this conclusion. It can also be shown that S is related to the Grüneisen ratio γ by

$$S = \frac{\gamma + 1}{2}. \quad (13)$$

Here the agreement is not so good.

As a first approximation for porous materials, $S_{\text{porous}} = S_{\text{solid}}$, whereas $C_{\text{porous}} = C_{\text{solid}}(\rho_{\text{porous}}/\rho_{\text{solid}})^2$. This approximation was found to work for iron as well as uranium.

Since

$$\gamma = \frac{\alpha}{\rho \kappa_s c_p} = \frac{\alpha}{\rho \kappa_T c_v}, \quad (14)$$

where

α = volumetric expansion coefficient, $(1/V)(\partial V/\partial T)_p$;

$\kappa_{s,T}$ = adiabatic or isothermal compressibility, $-(1/V)(\partial V/\partial P)_{s,T}$;

$c_{p,v}$ = specific heat at constant pressure or volume, $(\partial E/\partial T)_{p,v}$,

the compressibilities are related, as may be obtained thermodynamically, by

$$\kappa_T = \kappa_s + \frac{T\alpha^2}{\rho c_p}. \quad (15)$$

Compressibilities are available from high pressure compression or velocity of sound measurements where $C_s = \sqrt{1/\kappa_s \rho}$ or can be estimated from elastic properties. If K is the bulk modulus of compressibility, E is

TABLE 5. SHOCK-VELOCITY CONSTANTS FOR MATERIALS OF REACTOR INTEREST

Material	ρ , gm/cm ³	γ	C , (cm/sec) $\times 10^{-5}$	S
U	19.04	1.91	2.55	1.504
UO ₂	10.97	1.88	2.37	1.438
UC	13.61	2.11	2.08	1.555
UN	14.32	0.915	2.80	0.858
Pu	19.79	2.56	1.543	1.780
Al	2.79	2.18	5.25	1.39
Be	1.85	1.16	7.98	1.09
Fe	7.84	1.60	3.80	1.58
Mo	10.20	1.58	5.16	1.24
Na	0.97	1.17	2.563	1.50
Nb	8.60	—	4.45	1.21
Th	11.68	1.26	2.13	1.28
Ti	4.51	1.10	4.78	1.09
V	6.10	1.29	5.11	1.21
W	19.71	1.54	4.00	1.27
Zr	6.49	—	3.77	0.93

Young's modulus of elasticity, and ν is Poisson's ratio, then

$$\kappa_T = 1/K = 3(1 - 2\nu)/E. \quad (16)$$

The Grüneisen ratio can be calculated from its definition if the other parameters are available, as they generally are. This leads to a good estimate of the shock equation of state. Estimated values of C and S for reactor materials are given in Table 5.

The energy can be obtained from the relation

$$E_H = E_k + \frac{C^2}{2} \{[1 - (\rho_0/\rho)]/[1 - S(1 - (\rho_0/\rho))]^2\} = E_k + \frac{P_H[1 - (\rho_0/\rho)]}{2\rho_0}. \quad (17)$$

All that is necessary to estimate the energy in shock compression is an equation to calculate P_k or E_k since they are related. Very accurate measurements of sonic velocity as a function of pressure⁽⁴¹⁾ or estimations from statistical theories of the atom^(42, 53) can be used to provide an interpolation equation for P_k .

This estimation⁽⁴⁴⁾ is shown in Fig. 14. Here are plotted the simple Thomas-Fermi (TF),⁽⁴²⁾ Thomas-Fermi-Dirac (TFD),⁽⁴²⁾ and Thomas-Fermi with correlation correction (TFC)⁽⁴³⁾ models. The points plotted are calculated values. It can be seen that knowing the pressure from shock experiments in the region from 0.1 to 0.5 megabar and using the statistical theories as asymptotic limits, one can fit an equation to a smooth curve fit to these. The form for such a curve was suggested by Kormer⁽⁴⁵⁾ and used by Altschuler.⁽⁴⁶⁾

An equation fitted to similar curves for uranium is referred to in Ref. 38:

FIG. 13. Data of Skidmore and Morris for Shock and Particle Velocities.

$$p_k = 1.08[(\rho/\rho_0) - 1] + 3.88[(\rho/\rho_0) - 1]^{2.30}, \quad (18)$$

This equation was integrated numerically from Eq. 2 to find E_k and E_H from Eq. 17. With P_k and/or E_k one can calculate the pressure and energy for any density and temperature from⁽⁴⁷⁾

$$P(\rho, T) = P_k(\rho) + \gamma(\rho)c_v\rho T + \frac{1}{4}\rho_0\beta_0(\rho/\rho_0)^{1/2}T^2; \quad (19)$$

$$E(\rho, T) = E_k(\rho) + c_v T + [\frac{1}{2}\beta_0 T^2/(\rho/\rho_0)^{1/2}], \quad (20)$$

where β_0 is the electronic contribution to the heat capacity and is measured at very low temperatures. Alternatively, a Debye model⁽⁴⁶⁾ can be used, and the equations become

$$P(\rho, T) = P_k(\rho) + \gamma(\rho)\rho \frac{3RT}{A} D\left(\frac{\theta}{T}\right) + \frac{\gamma_e}{2}\beta(\rho)\rho T^2; \quad (21)$$

$$E(\rho, T) = E_k(\rho) + \frac{3RT}{A} D\left(\frac{\theta}{T}\right) + \frac{\beta(\rho)}{2} T^2, \quad (22)$$

FIG. 14. Estimates of Uranium Pressures.

FIG. 15. The Effect of Temperature on Pressure and Energy as a Function of Compression for Uranium.

where

A = atomic weight;

$D(\theta/T)$ = Debye function;

θ = Debye temperature;

$\beta(\rho) = \beta_0(\rho_0/\rho)^{\gamma_e}$ = approximate density dependence of the electronic heat capacity

γ_e = electronic Grüneisen ratio (which varies from 0.5 to $\frac{2}{3}$ for free electrons)

The results of calculations for uranium are shown in Figs. 15 and 16. Here the values⁽³⁸⁾ 1.04×10^6 erg/gm-°K and 170 erg/gm-°K were used for c_v and β_0 . In Fig. 17, the pressure and temperature along the shock Hugoniot as well as the cold compressive pressure are plotted vs energy. It can be seen that the pressure is approximately parabolic in energy while the temperature is approximately linear with energy.

The results of these calculations can be compared to the equations of state used for fast reactor calculations as compiled in the recent publication of Kazi and Cherry⁽⁴⁸⁾ and shown in Fig. 17.

FIG. 16. Pressure and Temperature as a Function of Internal Energy for Uranium.

1. FERMI (FARM)
$\gamma = 1.192$	6. 1000 MW FCR GEAP-4418
(FARM-REV)
2. FERMI
(NICHOLSON)	7. UO_2 (BÖHM)
CUBIC
3. FERMI (FARM)
$\gamma = 1.98$	8. UO_2 (BÖHM)
LINEAR
4. FERMI
(FARM-REV.)	9. 1000 MW FCR GEAP-4418
(FARM)
5. SEFOR
(FARM-REV.)	10. Fe (BÖHM)
CUBIC

FIG. 17. Equations of State Which Have Been Used for Calculation of Hypothetical Reactor Accidents.

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Discussion

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pounds?

Mr. Miller: For ordinary substances, one can use the law of corresponding states or techniques obtained from statistical mechanics to get the deviations from ideality in order to know the partial pressures of various materials above a mixture. However, in the case of the reactor substances, we don't have enough information at high temperatures to have great confidence in predictions. We use these techniques because we don't have any others. We need additional data.

Mr. Zebroski (GE): In very rapid processes, a great deal of evidence indicates that the equilibrium equations of state, particularly as concerns vapor pressure, do not apply for rate of vaporization. Brewer and his coworkers at Berkeley, particularly for semiconductors and especially for cadmium sulfide, have shown that evaporation rates are lower by a factor of 10 than we calculate from equilibrium considerations. Secondly, I think in accident analysis we are concerned with the

product of two uncertainties. Here we have an uncertainty in the thermodynamic data and an uncertainty in the assumption of isentropic expansion. Although we can use the theoretical approach, I think some integral experiments are needed, in which measured amounts of energy are dumped into the substance and we find how much work is done.

Mr. Miller: The work of Prof. Brewer is certainly quite interesting, but was done at low pressures where one is concerned if Langmuir evaporation is taking

Theoretical Studies on the Fast Reactor Maximum Accident

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Introduction

The fast reactor development programme of the UK Atomic Energy Authority includes the design of a prototype power-producing reactor. The ultimate safety of a large fast reactor is concerned with the possibility of a superprompt critical power excursion which causes the core to disintegrate with explosive violence. It has, therefore, been a major aim of the programme to ensure that the design incorporates features which make such an incident incredible. At the same time, however, the usual practice of estimating the magnitude and consequences of various hypothetical explosive releases of energy has been followed, and calculations of this nature have been in progress for several years.

The present paper attempts to summarize the work done on several particular problems, chosen because the conclusions appear to be of fairly general applicability and importance. These problems were originally tackled at various times over a period of several years, and as a result there was considerable variation in the reactor parameters used in the original calculations. However, it is believed that the most significant conclusions would remain true for a substantial range of parametric values corresponding to a sodium-cooled PuO_2/UO_2 -fuelled fast reactor operating at about 500–1,000 MW (H), so it was not thought necessary to repeat the calculations for a consistent set of parameters.

Three main problems are discussed. The first of these is the calculation of the total amount of energy generated in the assumed hypothetical excursion, and the lesser amount which is available to do mechanical work on the reactor structure; the second is a semi-quantitative discussion of a mechanism involving sodium boiling, which could increase the amount of energy available to do work; the third is a mainly qualitative comparison of the immediate mechanical effects of the nuclear excursion and of an "equivalent"

chemical explosion, together with some brief comments on longer-term mechanical effects.

The calculation of energy release starts from a known core configuration and a given rate of reactivity addition, usually derived from a model of gravitational collapse, as in the Bethe-Tait theory. A comprehensive treatment clearly requires a more realistic study of the mode of core collapse, and several attempts have been made to follow the course of events during core collapse in detail. Unfortunately, it appears to be extremely difficult to make calculations on core collapse which preserve at least a modicum of realism. In any case, the calculations reported below indicate that the effective energy release, that is, the energy available to do work, of the primary nuclear excursion will normally be small, and that a SPIERT-type explosive interaction of the hot fuel with liquid sodium may be of greater concern. If this is so, the rate of core collapse could be a fairly unimportant parameter, provided of course that the amount and rate of collapse were large enough to give the relatively small nuclear explosion needed to disrupt and disperse the core.

Calculations of Energy Release

Calculations of the energy release during a superprompt critical excursion have been based on a mathematical model which retains the essential features of the Bethe-Tait model,^(1, 2) that is, reactivity changes during the excursion are estimated by perturbation theory and mechanical wave propagation is ignored.⁽³⁾ The two main modifications which have been made to the simple Bethe-Tait theory are the inclusion of a prompt Doppler reactivity coefficient and the use of a more realistic equation of state of the fuel. We have also distinguished between the fuel density relevant to the equation of state and the total density used in the equation of motion in exactly the same manner as described by Nicholson.⁽⁴⁾ The most significant recent results have been obtained with a programme entitled PHOENIX, written for a Ferranti Mercury computer. PHOENIX assumes spherical symmetry, but in other respects is considered to be of adequate generality and flexibility for routine calculations of energy release. Its principal features are outlined as follows.

The thermodynamic properties of the fuel have been estimated from the principle of corresponding states applied to nonmetals by use of tables published by Hougen, Watson, and Ragatz.⁽⁵⁾ The fuel was assumed to be UO_2 , and the critical constants were estimated to be: $T_c = 8,000^\circ\text{K}$, $p_c = 2,000 \text{ atm}$, $V_c = 0.27 RT_c/p_c$; the ideal gas specific heat was taken to be $6.5R$. All these values, which were derived independently, are in quite good agreement with corresponding estimates by Meyer and Wolfe.⁽⁶⁾ The vapour pressure law, which

determines the pressure at lower temperatures, was derived by extrapolating published data for UO_2 in a manner which was in satisfactory agreement with corresponding-states tabulated data for a two-phase mixture of saturated liquid and vapour, whereas the single-phase tabulations gave the pressure and temperature as functions of the specific volume and energy density. A set of empirical analytic expressions were then derived for the pressure and temperature as functions of the energy density at constant volume. These expressions were in most cases simply straight lines which fitted the more detailed calculations to within a few per cent for all energy densities and specific volumes likely to occur in practice. In particular, the pressure-energy density relationship was approximated by a straight line in the single-phase region. A modification of the calculation which effectively extended this linear relationship down to zero pressure enabled PHOENIX to be used for calculations of energy release with the threshold-type equation of state of the simple Bethe-Tait theory. The zero for energy density has been taken arbitrarily at 0°C .

The work done during the adiabatic expansion of the core, after the (assumed) constant-volume energy release has terminated, was calculated as the decrease of internal energy from the compressed state to an expanded state at the same entropy and at temperature $3,450^\circ\text{K}$, the estimated boiling temperature of UO_2 at atmospheric pressure. In the case of the threshold equation of state, it was assumed that the work done was equal to the excess of the energy generated over the threshold energy. It is obvious from a pressure-volume diagram that the energy remaining as heat after the adiabatic expansion has been completed must increase with the energy generated, and hence the excess of energy generated over threshold energy is an upper-limit estimate of the work done for the threshold-type equation of state. However, the calculations reported below show that the work done is often increased when the equation of state is modified by allowing for vapour pressure at lower energy densities, even though the energy generated is always reduced.

Figure 1 shows the pressure-energy relation at constant volume. All curves follow the vapour pressure law until they reach the saturation point, and then depart from it in a linear manner up to the maximum pressure shown in the figure; for the threshold-pressure-law calculations, the straight lines were extrapolated to zero pressure. The empirical curves agree well with the calculated points, the worst fit being near the transition points.

The temperature-energy relation at constant volume, which is required for the calculation of the Doppler reactivity feed-back, is shown in Fig. 2, together with

FIG. 1. Pressure and Energy Density at Constant Volume for UO_2 .

the empirical curves which were fitted to the calculated points. Two features are worthy of note. First, the empirical relations agree well with the calculated points, the worst departures again being at the transition points. Secondly, the additional curve displayed at low energies and temperatures up to $5,000^\circ\text{K}$ was calculated by extrapolation of measured specific heat data in the region from $1,000$ to $2,000^\circ\text{K}$; the agreement at low temperatures with the corresponding-states theory is remarkably good.

Adiabatic relations were required for the calculation of energy release, and Fig. 3 shows the adiabats of the vapour pressure equation of state on a pressure-energy diagram.

Following Jankus,⁽⁷⁾ the reactivity change due to core motion satisfies the equation

$$d^2k/dt^2 = - \iiint (\text{grad } p \cdot \text{grad } w) dV, \quad (1)$$

where p is the pressure and w is the worth of unit mass

of core material. The pressure can be expressed as a sum of two terms:

$$p = P + p', \quad (2)$$

where P is the pressure calculated from the energy density via the constant-volume equation of state, and p' is the pressure perturbation due to the waves of compression and rarefaction originating at the core boundary. The reactivity effect of the perturbation pressure can be expressed as an integral over the core boundary, and similar surface integrals arise when the expression (1) is integrated by parts. However, numerous calculations have shown that these surface integrals are relatively small and have little effect on the energy release in typical examples. The PHOENIX programme therefore neglects the surface integrals and calculates the reactivity change due to core motion from the expression

$$d^2k/dt^2 = \iiint P \nabla^2 w dV. \quad (3)$$

FIG. 2. Temperature and Energy Density at Constant Volume for UO_2 .

For a homogeneous spherical core of radius b ,

$$d^2k/dt^2 = 4\pi \int_0^b P(d/dr)(r^2 dw/dr) dr, \quad (4)$$

and the integral is evaluated in the PHOENIX programme by an 11-point Gaussian quadrature formula at each step in time. The material worth function $w(r)$ is estimated from a detailed neutronics calculation and approximated by a fourth-order polynomial, from which appropriate values of $(d/dr)(r^2 dw/dr)$ are calculated and supplied to the programme as input data. A similar numerical procedure could be devised for other not too complicated geometries, such as a homogeneous finite cylinder.

The method used to incorporate a fairly realistic equation of state in the calculation of energy release has been described in some detail because of its novelty, but the same detail is not necessary elsewhere. The Doppler reactivity coefficient has been assumed to be of the form δ/T , where δ is called the Doppler constant and T is a suitably weighted average of the temperature

throughout the core; a straightforward uniformly weighted average core temperature is in fact used in PHOENIX. The calculation is started at delayed critical and the reactor kinetic equation is taken to be⁽⁸⁾

$$l dy_1/dt = (1 - \beta)k(t) - 1 + \beta \exp(-y_1), \quad (5)$$

where

$$y_1 = \ln(n/n_0) \quad (6)$$

and n is the reactor power density at the core centre, n_0 is the corresponding power density at delayed critical, l is the prompt-neutron lifetime, β is the delayed-neutron fraction, and $k(t)$ is the reactivity, allowing for the imposed step or ramp rate and for feedback due to core expansion and the Doppler effect. The distribution of reactor power is expressed as a fourth-order polynomial, fitted to a preliminary neutronics calculation. Other equations are as in orthodox versions of the Bethe-Tait theory,^(1, 2) and their numerical integration presents no serious problems.

FIG. 3. Pressure and Energy Density at Constant Entropy for UO_2 .

To illustrate the effects of the Doppler reactivity coefficient and the equation of state on the energy release, and in particular on the work done during an adiabatic expansion of the core debris, a series of calculations of energy release have been performed for a spherically symmetric system simulating a fairly typical oxide-fuelled fast power reactor. The basic neutronics data were calculated assuming complete loss of core sodium, and, in particular, the Doppler constant δ was estimated to be -0.24% . However, in view of present uncertainties in Doppler estimates, the energy release was calculated for a series of values of δ in the range from 0 to -0.5% . Similarly, in view of the uncertainties in our knowledge of core-collapse mechanisms, several rates of reactivity addition over the range from 0 to 200%/sec were considered. The distributions of power and material worth were taken from detailed neutronics calculations and were fairly flat over the core. Other significant parameters used in the calculations of energy release were:

Prompt-neutron lifetime	$= 10^{-7}$ sec
Threshold energy density in the threshold equation of state	$= 3.35$ kjoules g^{-1} of oxide fuel
Initial peak power density	$= 100$ W g^{-1}
Equivalent spherical core radius	$= 65$ cm
Actual core mass	$= 4$ tonnes of oxide fuel.

The results of these calculations are summarized in Table 1 which gives the peak values of the energy density, temperature, and pressure at the core centre, and also the total work done by adiabatic expansion of the whole core, expressed both in energy units and in the mass of TNT of equivalent work capability (1 lb TNT ~ 2 Mjoules). It must be stressed however that the comparison with TNT is very questionable when the peak pressure is only a few atmospheres, as in many cases with vapour pressure included in the equation of state. Also, when the total work is small, significant

TABLE 1. CALCULATIONS OF ENERGY RELEASE FOR A FAST POWER REACTOR

Reactivity Ramp Rate, % sec ⁻¹	Doppler Constant, %	Vapour Pressure Law					Threshold Pressure Law				
		Central Core			Total Effective Energy		Central Core			Total Effective Energy	
		Energy Density, kjoule g ⁻¹	Temper- ature, °K	Pressure, atm	Mjoule	lb TNT	Energy Density, kjoule g ⁻¹	Temper- ature, °K	Pressure, atm	Mjoule	lb TNT
25	0	3.52	7,790	3,850	1,375	725	4.58	12,900	31,100	558	294
	-0.05	2.35	5,600	342	116		3.64	8,380	7,410	24	23
	-0.1	1.81	4,370	21	10		3.60	8,180	6,360	15	8
	-0.2	1.71	4,140	10	4		3.57	8,050	5,710	9	5
	-0.3	1.58	3,830	3	1		3.57	8,020	5,550	8	4
50	0	4.11	10,600	19,100	1,955	1,030	4.96	14,700	40,600	877	462
	-0.05	3.00	7,090	2,200	676	356	4.07	10,400	18,100	122	64
	-0.1	1.99	4,780	64	32		3.81	9,170	11,500	60	30
	-0.15	2.16	5,170	156	64		3.59	8,130	6,120	13	7
	-0.2	1.90	4,560	37	19		3.67	8,520	8,170	30	16
	-0.3	1.82	4,390	22	11		3.60	8,140	6,160	13	7
	-0.4	1.70	4,120	9	4		3.62	8,280	6,910	20	10
100	0	4.85	14,200	37,800	3,150	1,660	5.48	17,200	53,800	1,630	860
	-0.1	2.82	6,680	1,470	570	300	3.79	9,080	11,100	56	30
	-0.2	2.39	5,450	265	95		3.71	8,670	8,960	37	20
	-0.3	1.90	4,560	36	19		3.67	8,520	8,150	30	16
	-0.4	2.02	4,830	73	36		3.65	8,410	7,610	26	14
	-0.5	1.64	3,960	5	2		3.64	8,330	7,180	22	12
200	0	5.75	18,600	61,100	4,680	2,470	6.23	20,800	72,610	2,850	1,500
	-0.1	3.82	9,250	11,960	1,660	870	4.56	12,800	30,600	542	286
	-0.2	2.26	5,400	243	89		4.02	10,200	16,800	106	56
	-0.3	2.64	6,300	900	290	152	3.68	8,560	8,380	32	17
	-0.4	2.14	5,120	139	59		3.84	9,330	12,400	67	35
	-0.5	1.79	4,300	17	7		3.76	8,940	10,350	49	26

pressures are only generated near the core centre, and most of the fuel makes no contribution to the work done.

As the Doppler constant increases, the work done, which we propose to call the effective energy release, varies in the damped oscillatory manner which has been reported and explained in earlier papers.^(4, 8) It is clearly not realistic to rely on such a phase-sensitive mechanism to limit the explosive energy release, so the detailed calculations have been used to construct approximate upper-bound envelopes of the effective energy as a function of Doppler constant and reactivity ramp rate; results for the vapour pressure equation of state are shown in Fig. 4. Earlier calculations with a threshold-type equation of state showed that the effect of the Doppler coefficient on the effective energy release could be expressed in terms of a critical Doppler constant δ_0 at which the effective energy first becomes relatively small, let us say an order of magnitude less than the value for $\delta = 0$; a simple approximate analytic formula for δ_0 is quoted by R. D. Smith *et al.*⁽⁸⁾ The present calculations have indicated that a similar con-

cept is valid for the vapour pressure equation of state and that the same equation for δ_0 still roughly holds. It would appear an adequate overall rule-of-thumb approximation to assert that the effective energy release is negligible if $(-\delta) \geq 2(-\delta_0)$.

To demonstrate more clearly the differences between the excursions with the two types of equation of state, a series of graphs have been drawn showing the time variation of particular parameters during the excursions produced by a reactivity ramp rate of 25 %/sec. Figure 5 shows the excess reactivity; the vapour pressure causes a steady reduction in excess reactivity after the pressure becomes significant, whereas the threshold-type equation of state causes a much sharper reactivity reduction when the threshold energy density is reached. The effect on reactor power is very similar.

The temperature at the core centre is plotted as a function of time in Fig. 6. The temperature corresponding to the threshold energy is about 7,500°K, and all "threshold" excursions must reach temperatures exceeding this value. In contrast, temperatures around

FIG. 4. The Effective Energy Release.

7,500°K only occur with a vapour pressure equation of state if the Doppler constant is small, and are generally much lower; in some cases the core barely reaches the boiling temperature of UO_2 . These relatively low temperatures are, of course, associated with correspondingly low core pressures.

The broad physical picture illuminated by these calculations would appear quite clear. The threshold-type equation of state generally leads to core pressures which remain zero until appreciable excess reactivity has been built up and then increase rapidly; thus the core is dispersed and the excursion terminated by fairly large pressures acting for quite short times. In contrast,

the vapour pressure becomes significant while the power is varying much less rapidly, and core dispersion is then due to much lower pressures acting for much longer times. The difference is particularly marked with a large Doppler constant, which increases the duration of the excursion and so allows greater time for a relatively low pressure to produce significant dispersion of the core.

Autocatalytic Power Excursions in a Two-zone Core

A super-prompt critical energy release in a fast reactor, of the type described by the Bethe-Tait theory, is

FIG. 5. The Excess Reactivity during Some Typical Power Excursions.

FIG. 6. The Core Central Temperature during Some Typical Power Excursions.

terminated by the motion of the core material as a result of pressures created by the energy generation. In its simplest form, the theory assumes that this motion always tends to reduce reactivity, but the converse is physically possible in special circumstances. This effect, known as autocatalysis, could in principle greatly increase the energy released during the excursion.

Within a homogeneous region of the core, the pressure increases with energy generated and hence with the flux. Thus, except possibly near interzonal boundaries where additional considerations apply, the motion of the core material is down the flux gradient. In the one-group approximation, the flux and neutron-importance distributions coincide so that motion down the flux gradient always tends to reduce reactivity. More generally, it is possible that importance can increase along directions of decreasing flux in limited parts of a core configuration, so giving a locally autocatalytic contribution to the total reactivity change.

A second autocatalytic effect can occur at the boundary between two core zones. Suppose that, at this boundary, both flux and importance decrease with increasing radial distance, but that the power density is discontinuous and is greater in the outer zone. Then the energy generation tends to cause a pressure discontinuity which relieves itself as a wave of compression spreading inwards through the inner zone and a wave of rarefaction spreading outwards through the outer zone. Both waves cause inwards motion of core material near the interzonal boundary, that is, in a direction opposed to the motion due to flux gradient.

In each type of autocatalysis, the autocatalytic effect is due to the motion of only a limited part of the core and is opposed by the bulk of the core motion. The net effect may thus be either to reduce or to increase reactivity. If the former, the transient will develop in a manner qualitatively similar to a normal Bethe-Tait excursion, and autocatalysis merely increases the energy release. If the latter, the excursion would continue with ever-increasing violence and would only be terminated by effects which are not at present taken into account in the calculation. In all cases computed to date, the overall effect of core motion has always been a reactivity reduction.

Calculations to investigate the autocatalytic effect due to the boundary between the two core zones of a fairly typical fast power reactor were based on a slightly modified version of the American digital computer programme ASTRA 417—3.0, in which the Doppler reactivity effect is not taken into account. The ASTRA programme calculates the energy release during a super-prompt critical excursion in a finite cylindrical assembly consisting of either one homogeneous zone or two zones of differing composition. For the present

calculations, these zones were taken as the inner and outer core zones, that is, an inner solid cylinder and a surrounding annular cylinder of the same length. The flux and worth distributions in the core are supplied as input data to the programme, and can thus be derived from a calculation which allows correctly for the surrounding blanket material.

On the other hand, the neglect of the blanket from the ASTRA calculation implies that the dynamical boundary conditions at each core-blanket interface are not correctly simulated. The programme, in fact, allows the option of one of two simple boundary conditions at each core-blanket interface, and general considerations indicate that these alternatives usually bracket the correct condition. Moreover, it is found in practice that the choice of core-blanket boundary condition makes little difference to the energy release, thus justifying the approximation *a posteriori*. Similarly, the programme does not use the physically correct boundary conditions at the interface between the two core zones but allows the same option as at the other boundaries. In this case, however, the difference between the resulting energy releases may not be negligible, and is in fact a measure of the autocatalytic effect of the interzonal boundary.

Generalization of eq. (1) to a reactor composed of several homogeneous zones leads to the following expression for the reactivity change due to core motion:

$$\begin{aligned} d^2k/dt^2 &= -\sum_n \iiint (\text{grad } p \cdot \text{grad } w_n) dV_n \\ &= -\sum_n \iiint (\text{grad } P \cdot \text{grad } w_n) dV_n \quad (7) \\ &\quad -\sum_{i,j} \iint p' (\text{grad}_i w_i) dS_{ij}, \end{aligned}$$

where the volume integrals are summed over all zones n , and the surface integrals are evaluated and summed for all boundaries of every zone; $\text{grad}_i w_i$ is the component of $\text{grad } w_i$ at a boundary in the direction normal to the boundary from zone i to zone j . The ASTRA programme assumes that all surfaces are free, so that the total surface pressure is zero and p' is replaced by $-P$ in each surface integral. More accurately, the common interface pressure is given by

$$p_{ij} = (P_i \rho_j c_j + P_j \rho_i c_i) / (\rho_i c_i + \rho_j c_j), \quad (8)$$

where ρ and c are the densities and acoustic speeds, respectively, in the two regions. Thus, the contribution of the common surface S_{ij} of regions i and j to the rate of reactivity change is

$$d^2k_{ij}/dt^2 = \iint [P_i (\text{grad}_i w_i) + P_j (\text{grad}_j w_j)] dS_{ij} \quad (9)$$

in the ASTRA approximation, and

$$d^2k_{ij}/dt^2 = \iint (P_i - P_j)[\rho_i c_i (\text{grad}_j w_i) - \rho_j c_j (\text{grad}_i w_j)] dS_{ij} / (\rho_i c_i + \rho_j c_j) \quad (10)$$

exactly. These two expressions are equal if the gradient of the worth function is continuous across the interface, that is, if $\text{grad}_i w_j = -\text{grad}_j w_i$, but in general they are different.

The only important difference between the two zones of a typical mixed oxide-fuelled fast power reactor is a greater plutonium enrichment in the outer zone, and thus the two zones have essentially the same density and acoustic velocity. The pressure in the outer zone, say P_j , is greater than P_i . The worth distribution is such that $\text{grad}_i w_j$ is positive and $\text{grad}_j w_i$ is negative. Thus formulae (9) and (10) both give a positive contribution to d^2k/dt^2 , that is, an autocatalytic effect. Moreover, if $(\text{grad}_i w_j + \text{grad}_j w_i)$ is positive, it is easy to show that the ASTRA approximation gives the larger value, and so overestimates the autocatalytic effect; this was found to be the case in the calculation reported below.

As an illustration, we have calculated the energy release from a two-zone core, a central solid cylinder surrounded by an annulus of roughly equal volume, in the form of a cylinder of overall diameter of 56 in. and height, after partial core collapse, of 25 in. The composition of the core was taken to be: oxide fuel (density 11 g/cm³) 35 v/o, steel (density 8 g/cm³) 25 v/o, and void 40 v/o. The plutonium fractions were about 18% and 23% in the inner and outer zones, respectively. Other parametric values used in the calculation were:

Rate of reactivity addition	= 50 %/sec
Prompt-neutron lifetime	= 5×10^{-7} sec
Threshold energy density in the threshold equation of state	= 2×10^{10} erg/g of oxide fuel
Initial peak power density	= 750 W/g at prompt critical
Core mass	= 4 tonnes oxide fuel.

The distributions of power generation and material worth throughout the core were determined by a multi-group calculation for the assumed cylindrical configuration of core and blankets.

The energy release during the power excursion was calculated by the ASTRA programme and converted to an equivalent mass of TNT as described above for the threshold equation of state. The results obtained were as follows:

Example	Explosive Yield (lb TNT)
(a) Including all surface effects	1124
(b) Excluding interface surface effects	925

In both examples the outer surfaces of the core were treated as free surfaces; this neglect of the blanket-tamping effect reduces the explosive yield, but experience has shown that the effect is small in cores of similar size. Example (b) ignores the integrals over the interface between the two core zones in the expression for reactivity reduction, and may thus be considered as ignoring the autocatalytic effects of this boundary. Example (a) treats the interface as a free surface for each core zone and thus overestimates the autocatalytic effect, which therefore increases the explosive yield by no more than 20%. It is considered that this result is indicative of the effect to be expected in any large fast reactor, namely, that this type of autocatalysis is unimportant.

The Residual Energy

It would seem quite clear from the calculations reported above and elsewhere⁽⁹⁾ that the immediate effective energy release will be small in a large fast power reactor, provided, of course, that the Doppler reactivity coefficient is of the order currently estimated. In contrast, the total energy release is always likely to be substantial, at least equal to the energy required to boil part of the oxide fuel, say 1 kJoule/g. Thus the energy generated in a core containing 4 tonnes of fuel is at least about 4×10^3 MJoule; if the whole of this energy could be converted into work in an explosive manner, the explosive effects would be comparable to those from the detonation of 2,000 lb TNT.

One can visualize a mechanism which would allow part of the heat energy in the core debris to be converted into work. We suppose that the primary energy release is concentrated in the fuel and that the other core constituents, steel and residual sodium, remain relatively cool. The primary explosion may provide enough energy to disperse the fuel and increase the effective rate of heat transfer to the cooler materials, both in and surrounding the initial core. The cooler materials, in particular the sodium, then boil and the expanding vapour does work on the environment. If the fuel and sodium are sufficiently finely dispersed, the boiling process could produce pressures of explosive violence. The details of the process we have outlined are obviously very complicated, and we have only found it possible to make some simple calculations which illuminate a few particular points. The following discussion summarizes our work on three aspects of the overall problem which appear to be of general interest. First, how finely must the fuel and sodium be mixed to generate high

pressures? Secondly, is such a degree of intermixing energetically possible? Thirdly, what fraction of the heat energy in the fuel is converted into work?

Consider a sphere of liquid sodium, radius r_0 and initial temperature T_0 , surrounded by an infinite sea of liquid fuel at initial temperature $T_1 > T_0$. It is straightforward to calculate the subsequent temperature distributions and heat flow rates due to thermal conduction, assuming that the system remains at rest, and it is found that the sodium temperature increases about 80–90 % of the range ($T_1 - T_0$) in time

$$t \sim \rho_c s_c r_0^2 / 3k_f \sim 10 \tau_0^2 \text{ sec},$$

where ρ , s , and k are, respectively, the density, specific heat, and thermal conductivity of the sodium coolant (suffix c) and fuel (suffix f), and r_0 is in cm; in other words, $10 \tau_0^2 \text{ sec}$ is a measure of the time scale of the process of heat transfer from the fuel to the sodium. The vapour pressure of sodium is quoted as⁽¹⁰⁾

$$\log_{10} p(\text{atm}) = 4.521 - (5,220/T^\circ\text{K})$$

up to 1,135°C. Assuming that this relation continues to hold to much higher temperatures, we deduce vapour pressures up to about 1,000 atm at temperatures up to the boiling temperature of UO_2 . Thus pressures of this order are needed to prevent expansion of the sodium bubble; alternatively, pressures of this order will occur in the bubble if its expansion is small during time $10 \tau_0^2 \text{ sec}$.

The rate of bubble expansion can be estimated from a simple theory due to Rayleigh.⁽¹¹⁾ A sensible measure of the time scale of the expansion is the time taken to double the bubble radius, and is found to be about $10^{-3} \tau_0 \text{ sec}$ for an average driving pressure of 100 atm within the bubble. Thus the dynamical time scale is less than the thermal time scale if r_0 is less than about 1 micron. In the case of sodium bubbles of submicron radius, an appreciable temperature, and hence pressure, increase could thus occur before there is any significant bubble expansion. The pressure created, of up to 1,000 atm, would then be relieved in an explosive manner. These pressures are rather lower than those associated with conventional explosives, such as TNT, and would probably cause mechanical damage to the reactor structure somewhat less than a TNT explosion of the same effective energy release. In contrast, bubbles of order, say, 1-mm radius would merely boil vigorously at insignificant pressure.

The simple dynamical theory, which relates to the expansion of a single spherical bubble, can be extended rather elegantly to allow for interactions among a large number of bubbles.⁽¹²⁾ Consider a sphere of radius R_0 containing N sodium bubbles, each of radius r_0 , in a sea of incompressible UO_2 and surrounded by an effec-

tively infinite volume of UO_2 . The expansion of each sodium bubble is governed by Rayleigh's equation

$$p_b = p_0(t) + \rho(r_0 \ddot{r}_0 + 1.5 \dot{r}_0^2), \quad (11)$$

where ρ is the density of UO_2 , p_b is the (assumed constant) bubble pressure, and $p_0(t)$ is the pressure in the UO_2 well away from a bubble; p_0 is determined by a similar equation governing the expansion of the sphere of radius R_0 , namely,

$$p_0(t) = P_0 + \rho(R_0 \ddot{R}_0 + 1.5 \dot{R}_0^2), \quad (12)$$

where P_0 is the constant pressure at infinity. In addition the volume of UO_2 in the sphere $R_0(t)$ is constant, so that

$$R_0^3 = N r_0^3 + \text{constant}. \quad (13)$$

It is straightforward to integrate these equations numerically and determine the time variation of bubble radius and of UO_2 pressure. As is physically obvious, the bubble interaction increases the back pressure p_0 and so slows down the rate of bubble expansion. Typical calculations have shown that the effect is quite large and could, for example, reduce the rate of bubble expansion by about two orders of magnitude. Thus significant pressures can occur with sodium bubbles of radius up to several tens of microns.

We visualize that the relatively small sodium bubbles needed to cause a secondary explosion are formed by fragmentation of more massive sodium by forces due to the primary explosion. This process increases the surface area and hence the surface energy of the sodium, but the increase is not large. For example, the surface energy of 1 tonne of sodium in the form of bubbles of 0.1-micron radius is only of the order 10 Mjoule, equivalent to an explosion of 5 lb TNT. It would thus appear that more than adequate energy is available to fragment enough sodium, but this does not of course imply that large-scale fragmentation would actually occur. A realistic dynamical calculation does not appear possible at the present time.

An estimate, which is almost certainly pessimistic, of the thermal efficiency of the process of converting heat to work by sodium vapourization can be made on thermodynamic grounds. Suppose that a mass m of sodium at absolute temperature T_c mixes intimately with unit mass of fuel at absolute temperature T_f and that thermodynamic equilibrium is established effectively instantaneously. Suppose that the mixture then begins to expand adiabatically, remaining in thermodynamic equilibrium throughout the expansion. At a general time during the expansion, let the mixture contain unit mass of fuel, mass x of sodium vapour, and mass $(m - x)$ of liquid sodium, all at absolute temperature T and pressure $p_s(T)$, the vapour pressure of

sodium. The adiabatic relation can be written down for a quite general equation of state, and calculations using a sodium equation of state derived from the principle of corresponding states are planned. However, the following calculations, based on a simple approximate two-phase equation of state for sodium, are thought to give a reasonably adequate representation of the correct results even though they predict temperatures and pressures which lie within the critical region.

Assuming that liquids are incompressible and of negligible specific volume compared with the vapour phase, that sodium vapour behaves as a perfect gas, and that specific and latent heats are constant, the adiabatic relation for the fuel-sodium mixture is

$$(ms_l + s_f)(dT/T) + Ld(x/T) = 0, \quad (14)$$

where s_l and s_f are the specific heats of liquid sodium and fuel, respectively, and L is the latent heat of evaporation of sodium; this integrates to

$$Lx/T = (ms_l + s_f) \ln(T_m/T), \quad (15)$$

where T_m is the initial equilibrium temperature, given by

$$T_m = (ms_l T_c + s_f T_f) / (ms_l + s_f). \quad (16)$$

Equation (15) determines the sodium vapour content of the mixture as a function of temperature. The work done during the adiabatic expansion is

$$W = (ms_l + s_f)(T_m - T) - x(L - RT). \quad (17)$$

These results hold until $x = m$ at $T = T_1$, when all the sodium has been vapourized. During the further adiabatic expansion of the system, the appropriate adiabatic relation is

$$mR \ln[p_1(T_1)/p] = (ms_{gp} + s_f) \ln(T_1/T), \quad (18)$$

where s_{gp} is the constant-pressure specific heat of sodium vapour, and the additional work done is

$$W - W_1 = (ms_{gv} + s_f)(T_1 - T), \quad (19)$$

where s_{gv} is the constant-volume specific heat.

Numerical results for a range of values of m are given in Table 2 and Fig. 7. Table 2 gives corresponding values of temperature, pressure, and mass x of vapour, vapour volume (xRT/p), and the work function W , all per unit mass of fuel. Figure 7 shows how the work done in an adiabatic expansion to several specified final pressures varies with m . Parametric values used in the calculations were:

$$\begin{aligned} T_f &= 3,450^\circ\text{K}; & T_c &= 1,150^\circ\text{K}; \\ s_f &= 0.3 \text{ joules/g}\cdot^\circ\text{C}; & s_l &= 1.2 \text{ joules/g}\cdot^\circ\text{C}; \\ s_{gp} &= 0.9 \text{ joules/g}\cdot^\circ\text{C}; & R &= \frac{1}{3} \text{ joules/g}\cdot^\circ\text{C}; \end{aligned}$$

$$L = 4,000 \text{ joules/g}; \quad P_v = A \exp(-BIT);$$

$$A = 4.85 \times 10^5 \text{ lb/in.}^2,$$

$$B = 12,000^\circ\text{K in the vapour pressure equation.}$$

These values are not entirely consistent thermodynamically, but the discrepancies are not large and do not affect the broad conclusions to be drawn from the calculations.

Figure 7 shows that, as m increases, the work done first increases to a maximum and then decreases. The initial increase is due to the formation of an increasing volume of sodium vapour; the later decrease is due to the loss of energy in heating sodium which remains in liquid form. The maximum occurs at a value of m which increases with the final pressure from about 0.04 at 5,000 lb/in.² to 0.10 at one atmosphere, that is, the maximum work is done when the fuel mixes with a slightly smaller volume of sodium. Secondly, it is apparent that a substantial part of the work is done at relatively low pressures, say, below 1,000 lb/in.². In contrast, the permanent gases produced by a chemical explosive perform relatively much less work, at low pressures, let us say typically only about 25% of the total at pressures below 1,000 lb/in.². The total heat energy in the fuel is $s_f T_f \sim 10^3$ joule/g; thus the work done in an adiabatic expansion down to, say, 1,000 lb/in.² is only about 10% of the total energy.

The Equivalent Chemical Explosion

The direct calculation of the effects of a hypothetical nuclear explosive release of energy on the reactor structure and its environment is practically impossible on account of the complicated geometry. It is, therefore, common practice to estimate these effects by means of experiments with scale models of the reactor and the use of conventional high explosives to simulate the nuclear explosion. We are not here concerned with the validity of the modelling technique for chemical explosives, so we assume that the experiments determine the effects of a chemical explosion on the full-scale reactor. The effects on the reactor of a given nuclear excursion can therefore be deduced if one can determine an "equivalent" size of conventional explosion.

Previously we briefly commented on one difference between a chemical explosion and a SPERT-type sodium-boiling incident. The following discussion compares the chemical explosion with a nuclear excursion giving a fairly substantial primary effective energy release, say at least several hundred megajoules. Other calculations reported above have indicated the unlikelihood of such an excursion in a typical fast power reactor as currently conceived, but we considered the following comments were still of sufficient general interest to warrant inclusion in this survey.

TABLE 2. ADIABATS OF A MIXTURE OF SODIUM AND UO_2

Mass of Sodium per Unit Mass UO_2	Temp, °K	Pressure, atm	Mass of Sodium Vapour per Unit Mass UO_2	Volume of Sodium Vapour per Unit Mass UO_2 , $\text{cm}^3 \text{g}^{-1}$	Work Done in Expansion per Unit Mass UO_2 , joule g^{-1}	Mass of Sodium per Unit Mass UO_2	Temp, °K	Pressure, atm	Mass of Sodium Vapour per Unit Mass UO_2	Volume of Sodium Vapour per Unit Mass UO_2 , $\text{cm}^3 \text{g}^{-1}$	Work done in Expansion per Unit Mass UO_2 , joule g^{-1}
0.01	3,362	930	0	0	0	0.02	3,280	850	0	0	0
	3,231	805	0.01	0.132	11.6		3,023	624	0.02	0.319	23.4
	3,201	340		0.310	20.8		2,985	340		0.577	35.2
	3,170	136		0.766	30.2		2,929	136		1.416	52.7
	3,146	68		1.521	37.6		2,886	68		2.791	66.0
	3,069	6.8		14.84	61.1		2,750	6.8		26.59	108.4
	3,006	1.0	0.01	98.9	80.4		2,697	1.0	0.02	177.4	148.3
0.03	3,204	780	0	0	0	0.04	3,133	717	0	0	0
	2,823	470	0.03	0.593	36.2		2,631	345	0.04	1.004	49.8
	2,795	340		0.811	45.1		2,630	340		1.017	50.1
	2,718	136		1.971	69.5		2,536	136		2.452	80.5
	2,661	68		3.860	87.6		2,467	68		4.771	102.7
	2,480	6.8		35.97	144.9		2,252	6.8		43.55	172.1
	2,339	1.0	0.03	230.8	189.6		2,086	1.0	0.04	274.4	225.7
0.06	3,005	608	0	0	0	0.08	2,892	520	0	0	0
	2,623	340	0.0332	0.842	38.3		2,623	340	0.0253	0.642	27.4
	2,258	163	0.06	2.741	83.0		2,185	136	0.0606	3.201	81.7
	2,235	136		3.242	90.7		1,940	68	0.0767	7.504	119.8
	2,150	68		6.237	119.1		1,883	56	0.08	8.796	129.8
	1,887	6.8		54.74	206.9		1,618	6.8		62.58	228.4
	1,694	1.0	0.06	334.3	271.4		1,410	1.0	0.08	371.0	305.8
0.10	2,793	450	0	0	0	0.15	2,588	320	0	0	0
	2,623	340	0.0173	0.439	17.3		2,185	136	0.0444	2.345	48.2
	2,185	136	0.0563	2.974	71.2		1,940	68	0.0671	6.294	86.0
	1,940	68	0.0742	6.960	109.4		1,414	6.8	0.1026	70.14	201.5
	1,444	8.1	0.10	58.67	214.7		1,153	1.0	0.1119	424.4	284.2
	1,423	6.8		68.80	222.9						
	1,208	1.0	0.10	397.3	306.7						
0.20	2,428	235	0	0	0	0.25	2,300	179	0	0	0
	2,185	136	0.0311	1.643	29.5		2,185	136	0.0168	0.887	14.0
	1,940	68	0.0587	5.506	66.7		1,940	68	0.0497	4.662	49.3
	1,414	6.8	0.1032	70.55	183.4		1,414	6.8	0.1032	70.55	167.4
	1,153	1.0	0.1159	439.5	269.4		1,153	1.0	0.1194	452.8	256.5

The concept of an equivalent charge of high explosive is at best only approximate. Even if it were possible to match exactly the effects at one point in the reactor, it is far from obvious that there would be similar matching elsewhere. Moreover, matching in detail at even one point is not obviously possible since the response of a structure must in principle depend on the detailed time history of the applied load, which is unlikely to be completely matched. On the other hand, we are interested in practice mainly in broad effects, such as the energy absorbed by a particular structure, rather than in fine details. Experience with differing masses and types of chemical explosive has shown that such broad effects correlate well with a few simple overall properties of the explosion, in particular the peak pressure and impulse

of the blast wave, both of which depend largely on the energy released, the time scale of the release, and on the distance from the explosion. It has therefore appeared useful to compare and contrast the chemical and nuclear explosions in some detail, to try and assess the value of the concept of an equivalent charge of high explosive.

We first consider the effects of the explosion of a mass of chemical explosive, say several hundred lb TNT, placed at the position of the core in a geometrical arrangement which is otherwise the same as that of a large fast power reactor. The detonation of this explosive charge produces, in a time scale of order 10^{-4} sec, a mass of gaseous products occupying essentially the same volume as the solid explosive, at a temperature of

FIG. 7. The Work Done in a Spert-type Incident with Sodium and UO_2 .

the order of $3,000^\circ\text{C}$, a peak pressure of the order of 10^5 atm, and in motion at velocities of the order of 5,000 ft/sec. It should be noted that the precise values, and particularly the particle velocity, depend on the detonation procedure and would, for example, be very different for central and surface points of detonation; this difference would be reflected in quite different primary shock waves close to the explosive surface. British model-scale tests use approximately central detonation of a right cylindrical explosive charge, which would seem a reasonable simulation of a rapid nuclear energy release spreading outwards from the core centre.

The mechanical interaction of the products of detonation with the surrounding medium creates a wave of compression headed by a shock which propagates out into the medium. Very close to the explosion, the properties of this wave (such as peak pressure and duration) are very dependent on the pressure and velocity of the detonation products. It is well known, for example, that explosives of the same energy content can differ in their ability to cause damage at very short

ranges. At greater distances, however, this dependence on the detailed initial conditions dies out, and the strength and duration of the primary shock wave become ultimately dependent only on the total available energy. In our opinion, the pressure loading on, say, the roof of the reactor vault would not be very dependent on fine details of the energy release.

Whenever the shock wave meets an interface between different materials, part of the wave energy is transmitted, usually as a shock wave, and part reflected as either a shock or rarefaction wave. The wave energy is also steadily reduced by dissipation processes. The shock wave propagates at a velocity of 5,000–10,000 ft/sec, and the linear dimension of the reactor vault is, say, 50 ft, so that the wave transit time is 5–10 ms. Thus, after a time of, say, 0.1 sec, the original wave has been reflected at least tens of times from the multiple structural components within the vault. We think it probable that, by this stage, the shock-wave energy has very largely been dissipated as heat or as permanent structural damage.

After the initial shock wave has been emitted, about 50 % of the available energy remains in the bubble of high explosive gases. To fix orders of magnitude, consider a spherical bubble of gas, about 3 ft in diameter, of density 1.5 g/cm^3 , and weighing 1,000 lb. If the gas has an internal energy of 2,000 joules/g, and assuming a perfect gas with $\gamma = 1.25$, the bubble pressure would be about 7,500 atm. The bubble temperature depends on the gas composition; for air it would be about 3,000°K. Now suppose that the bubble expands adiabatically down to atmospheric pressure. The corresponding density would be 0.0012 g/cm^3 , temperature, say, 500°K, and internal energy 340 joules/g. The calculation has therefore shown that over 80 % of the total energy (about 90 % if the zero of energy is taken at 0°C instead of 0°K) is available to do work on the surrounding media; corresponding results for TNT detonation products are similar. The expanded volume is about $13,400 \text{ ft}^3$, corresponding to a spherical diameter of about 30 ft. Thus, if the reactor vault remains intact, the bubble would not expand fully but would oscillate about an equilibrium size determined by the available free volume. For example, if the free volume is $2,000 \text{ ft}^3$, the equilibrium bubble pressure would be of order 150 lb/in.² if all heat losses are neglected.

The pressure in the medium surrounding the expanding bubble can be calculated for a deep underwater explosion of 1,000 lb TNT and is quite large—for example, about 50 atm at a 15-ft range when the bubble has expanded to about a 10-ft diameter. The problem is much more complex in a reactor structure on account of the complicated geometrical arrangement, the confinement, and the free surface of the coolant, but model-scale experiments have shown that pressures of hundreds of lb/in.² will persist for at least several tenths of a second. It seems likely that these pressures make a greater contribution than the shock waves to the total impulse on the vault roof, and so are the main cause of plug jump and perhaps also of failure of the roof itself. On the other hand, they would seem too small to cause damage to structural members which have survived the initial shock wave.

A fast reactor nuclear excursion, of the type described in the Bethe-Tait theory, is effectively a nuclear detonation spreading out from the core centre in a manner qualitatively similar to a centrally detonated chemical explosion. The nuclear excursion creates a volume of high-pressure, high-temperature fluid, quite analogous to the ball of gaseous detonation products formed by a chemical explosion. In typical Bethe-Tait calculations, assuming that the Doppler reactivity coefficient is small, the peak pressure is of the order of 10,000–50,000 atm, and the particle velocity is estimated to be of the order of 10^3 ft/sec . The time scale of the bulk of the

nuclear-energy generation is also quite comparable with that of the chemical explosion. We therefore consider that the shock-damage characteristics of the nuclear excursion are comparable with, although probably less severe than, those of an equivalent high explosive charge of the same energy, except perhaps very close to the point of detonation. It would seem obvious that the only energy which is physically relevant in this context is the energy available to do work, so that the realistic comparison is between effective energies rather than total energies.

The most useful basis of comparison between the bubble oscillation phases of the nuclear and chemical energy releases would appear to be the pressure-energy relationships during adiabatic expansion of the bubbles, and a typical comparison is illustrated in Fig. 8. This shows the work done as a function of bubble pressure during adiabatic expansions of bubbles simulating the detonation products of the two events. As discussed above, the adiabats of UO_2 have been estimated from an equation of state deduced from the principle of corresponding states. The chemical products of combustion have been approximated by a perfect gas of initial density 1.5 g/cm^3 , initial pressure about 7,500 atm, and adiabatic constant $\gamma = 1.25$. The work done during an adiabatic expansion down to atmospheric pressure is 1,000 MJoule in each case.

In this particular example, the initial pressure is substantially lower in the nuclear detonation products, and Fig. 8 shows that much more work is done at lower pressures by the release of nuclear energy; for example, the conventional explosion does about twice as much work in expanding to a pressure of 1000 lb/in.². The corresponding effect would be less for larger energy releases, but even greater than a factor of two for the smaller effective energy releases which seem more probable. To sum up, the present comparison appears to show that there is a sufficiently close similarity between the shock and bubble expansion phases of the two types of energy release to justify use of the concept of an equivalent chemical explosion defined on an equal work basis. In general, the nuclear excursion will generate somewhat smaller pressures and will therefore be a rather less effective explosion; however, the magnitude of the difference in damage will depend on the type of structure under consideration.

A few seconds after the initiation of either the chemical or the nuclear energy release, the heavily damped bubble oscillations will have effectively ceased, and we are then concerned with the residual heat energy of the debris, together with a quasi-steady residual pressure if the explosion is still contained. The most important differences between the two types of energy release seem likely to be associated with the dissipation of these

FIG. 8. Comparison between the Adiabatic Expansion of UO_2 and Chemical Detonation Products.

residual pressures and temperatures. It is not the purpose of the present paper to discuss these longer time-scale phenomena in any detail. Rather, we propose merely to mention some of the factors which contribute to these differences.

In general, a very much larger proportion of the energy generated remains as residual heat in the nuclear debris, and the residual temperature of the nuclear debris is correspondingly higher. As discussed above, this allows the possibility of an additional explosive energy release as a result of sodium vapourization. In addition, continued sodium vapourization on a longer time scale may lead to further problems, such as an enhanced residual pressure in the reactor vault. The size of the increase will be determined by a complicated dynamic balance between the rates of sodium boiling at the hot core debris and of recondensation of vapour bubbles in the cooler bulk sodium. On an even longer time scale, a similar cycle of sodium boiling and recondensation will probably be necessary to remove the nuclear decay heat from the debris. These considerations indicate that the longer-term courses of events following the two types of energy release, and in particular the sizes of the residual pressures in the reactor vault, are affected by quite different phenomena, and that the concept of an equivalent chemical explosion is no longer applicable.

Concluding Remarks

Estimates of the magnitude and consequences of hypothetical maximum accidents in a large fast power reactor were for many years dominated by calculations based on various modifications of the Bethe-Tait model. Such calculations are still an important part of the work but are no longer of dominating importance. This has resulted from an appreciation of the role played by the Doppler reactivity coefficient in limiting the Bethe-Tait energy release to such an extent that the pressures generated are insignificant in comparison with those due to a conventional chemical explosion. The emphasis has consequently shifted to a consideration of the fate of the energy generated as heat in solid and liquid fuel, and which may be partially converted into work, possibly in an explosive manner, by such processes as vapourization of liquid sodium.

The possibility of such a secondary effective energy release cannot be ruled out on simple energy considerations because a quite small primary explosion, equivalent to perhaps only a few tens of lb TNT, would appear energetically able to bring the fuel and coolant into the necessary close contact. Fortunately, the explosive efficiency of the sodium-boiling process appears small. The ideal thermodynamic efficiency calculations show that no more than about 10% of the energy is converted into work at pressures of explosive magnitude, and heat

transfer time lags will almost certainly greatly reduce this value. On the other hand, the relatively large amounts of sodium vapour generated at lower pressures may be significant on a longer time scale, particularly in relation to the reactor vault pressure.

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Discussion

Mr. McCarthy (PRDC): You discussed one form of possible autocatalysis. Have you considered other cores, which are now getting to be very large in terms of surface area, and very, very thin? In the early days of the Bethe-Tait model, when cores were 2 ft or so in diameter, one could relatively sensibly think of the whole

core melting down at once. It seems to me much more likely that in these larger cores some smaller fraction of the core will melt down, some area of, say, a foot in diameter. Have you considered the possible influence on the unmelted part of the core of an explosion due to the melted part, say the compression of the fuel pins into a more compact geometry in the unmelted section?

Mr. Hicks: In detail, no. There have been a number of speculations about the possibility of compressing parts of cores and producing secondary critical masses. My personal opinion is that it is so extremely unlikely as to be totally incredible.

Mr. Wolfe (GE): It appears necessary to assume that a large amount of sodium remains in the vessel and that although in principle one could generate the high pressures, one would expect that the sodium bubbles themselves would condense in much the same way that the vapor suppression type of containment condenses steam that goes through subsaturated liquid. Have you considered this?

Mr. Hicks: Partly. I think that if the sodium vapor bubbles (I'm thinking of the longer time scale now) are formed underneath a substantial depth of subcooled liquid sodium, they would unquestionably recondense as they rose to the surface. We've done a certain amount of calculations along those lines. But where the core debris is at the end of the primary explosion, I for one would not like to say. I don't see how one can be absolutely certain that the vapor is produced with a substantial track in highly subcooled liquid before it comes out, let's say, in the cover gas.

Mr. Fenech (MIT): How did you establish the rate of reactivity insertion for the accident, and on what basis have you determined the prompt reactivity at which the explosion would start?

Mr. Hicks: The usual approach of a Bethe-Tait type of uniform collapse under gravity is the basis of rate of reactivity addition, just to get an order of magnitude, and then one does calculations for a range of values around the estimated value.

A Parameter Study of Large Fast Reactor Meltdown Accidents

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ABSTRACT

The energy release from a fast reactor meltdown accident was examined to assess the importance of the various parameters involved. Modified Bethe-Tait calculations for a spherical 1000-MWe oxide-fueled reactor have been performed as a function of Doppler coefficient, neutron lifetime, pressure-energy relationship, core composition, power flattening, and initial starting conditions. Calculations have also been performed for a cylindrical version of the Bethe-Tait equations.

It is shown that with a large Doppler coefficient, one becomes relatively insensitive to the reactor parameters except for the heat capacity of the fuel, and that for reactivity inputs of the order of tens of dollars per second, a large but attainable Doppler effect can limit the explosive energy release to a magnitude for which containment has been engineered in presently operating fast reactors.

I. Introduction

The fast reactors which have been built in the United States to date have been designed to accommodate a maximum hypothetical accident which predicates gross melting and consequential compaction of the core. As a result of the reactivity introduced by the core compaction, the reactor is put on a prompt critical period which is finally terminated by explosive disassembly of the core. In the cases of Fermi and EBR-II, the explosive energy release was calculated, under pessimistic assumptions, to be of the order of 1000 MW-sec. Since the energy release for a given set of assumptions is roughly proportional to core mass, the possibility of a meltdown accident poses a serious problem for the power reactors of the future which are expected to have more fuel in the core by over a factor of 10 than the present generation of experimental breeder reactors. In a paper presented at the last Argonne Conference⁽¹⁾ and in subsequent papers,^(2, 3) we have pointed out that the presence of a large Doppler effect can substantially reduce the energy release from a postulated meltdown accident. Similar conclusions have been reached by Nicholson⁽⁴⁾ and

by Smith *et al.*⁽⁵⁾ Indeed, it should be pointed out that at the second Geneva Conference, Stratton *et al.*⁽⁶⁾ showed that the energy release from a meltdown accident could be substantially reduced in the presence of a negative temperature coefficient. At the time, of course, it was not recognized that fast reactors could be designed with large negative Doppler coefficients and Stratton's work considered only prompt expansion coefficients. The basic mechanism is, however, the same.

In the present paper, the energy release from a meltdown accident is examined with reference to a 1000-MWe fast ceramic-fueled power reactor. The effect of the basic parameters on the energy release were examined. No attempt was made to find a series of events which would lead to a given reactivity ramp; instead, we have assumed various reactivity insertion rates or reactivity steps, and have investigated the energy release as a function of the reactor parameters to assess the importance of these parameters and the sensitivity of the results to the assumptions.

The basic conclusion of the study is that if the Doppler coefficient is made large enough, the energy release from a large power reactor can be made of the same order of magnitude as that for which containment has already been engineered in the present generation of experimental reactors. It should be noted, however, that the price of containment may be high for a power reactor which is measured by economic performance. Although it is certainly possible to contain accidents releasing explosive energy of the order of 1000 MW-sec, the necessary holddown structures may make operations (especially refueling) cumbersome. Also, although the explosive disassembly of the core terminates the meltdown accident, one must still provide heat-removal equipment which will function reliably after the disassembly to remove tens of megawatts of decay heat. Finally, the need for a large Doppler coefficient may, itself, require a core design which penalizes economic performance.

This paper does not touch on these latter points, and the authors wish to state that this paper is not intended to imply that an accident ending in explosive disassembly of the core is a realistic consideration for

a particular reactor design, or to imply a recommendation as to the safety philosophy which should be adopted.

II. Description of the Calculations

The calculations to be described retain the essential assumptions of the Bethe-Tait formalism, namely, that the pressure is a function only of the energy density in the fuel, and that the reactivity effects of fuel movement can be calculated by perturbation techniques. However, instead of the linear pressure-energy density relationship above a threshold which was used in early Bethe-Tait work, a consistent equation of state has been derived by use of the principle of corresponding states.⁽⁷⁾ The derived equation of state relates temperatures, energy density, and pressure, and also permits a calculation of the available work (explosive energy) for a given energy density assuming an isentropic expansion. The method which was used to obtain these equation of state relations for UO_2 is an extension of work originally performed by Brout.⁽⁸⁾ In the meltdown calculations, these relations were approximated by power series. The Doppler effect is included in the calculations, under the assumption that its temperature dependence is $1/T$, where T is the average fuel temperature in the core, i.e., no flux weighting was used. The reactor kinetics equations include the effects of delayed neutrons. Most of the calculations to be described involve a spherical reactor core. However, we have also derived the Bethe-Tait equations in cylindrical geometry and a number of calculations have been carried out for a cylindrically-shaped reactor core. The derivation of the equation of state and some of the methods used in the calculations have been described previously.^(1-3, 7) In this paper, attention will be directed primarily to the results of the calculations. A further, more detailed description of the calculational techniques is in preparation.

III. Calculations for a 1000-MWe Spherical Reactor

The reactor model chosen for these calculations is a spherical core with the same composition and same mass as that of the 1000-MWe fast power reactor described in GEAP-4418. Its characteristics are as follows:

Material Composition:	
$\text{UO}_2\text{-PuO}_2$	22 v/o
Steel	19 v/o
Sodium	47 v/o
Radius of equivalent sphere	113 cm
Reflector savings	20 cm
Neutron lifetime	5.7×10^{-7} sec
Peak-to-average power	1 71

In the calculations, it was assumed that the sodium

was not in the core and that the void left by the sodium was available for fuel expansion. The equation of state for the $\text{PuO}_2\text{-UO}_2$ mixture was assumed to be the same as that of UO_2 calculated on the basis of the law of corresponding states.⁽⁷⁾

In a given meltdown accident, the reactor is put on a fast period, and the power and energy density in the core rise until the point is reached at which there is a significant pressure buildup which tends to cause fuel movement and, ultimately, core disassembly and shutdown of the reactor. In the calculations it has been assumed that the clad is molten so that pressure tending toward fuel movement starts at the normal boiling point of the fuel. The course of the accident is completely determined if one gives the initial conditions at the time the boiling point of the fuel in the core is first reached. Prior to this time, it is assumed that the reactor behavior is described by the standard kinetics equations.

In order to gain an understanding of the behavior of the reactor in the disassembly process itself, a number of problems have been considered starting at the time that fuel boiled. In these problems (in order to start with conditions somewhat representative of a power excursion), the reactor was started from normal full power (2200 MWt) and a reactivity step or ramp was inserted, but no reactor feedback, i.e., Doppler effect, was included. The conditions existing at the time when the fuel first reached the boiling point were then used as the starting point for a number of parametric analyses described in the following sections.

1. Influence of the Doppler Effect

The curves on Figure 1 show the results of calculations in which various reactivity steps were inserted. The ordinate is the available work resulting from the excursion as a function of the Doppler coefficient. Since the Doppler coefficient $[T(dk/dT)]$ is assumed to vary as $1/T$, the parameter $T(dk/dT)$ is a constant (referred to as A_{Dop} throughout the paper). The results of Fig. 1 are consistent with previous analyses which have shown that a large Doppler coefficient can reduce the energy release by more than a factor of 10. For calibration, it should be noted that the calculated Doppler coefficient for the 1000-MWe reactor of GEAP-4418 is $A_{\text{Dop}} = -0.008$. Also, for calibration, it is worth noting that the reactivity steps (above prompt critical) of $0.001 \Delta k/k$, $0.0005 \Delta k/k$, and $0.00025 \Delta k/k$ would be approximately equivalent to ramps of 45\$/sec, 15\$/sec, and 6\$/sec in the absence of a Doppler coefficient.

The ability of the Doppler coefficient to reduce significantly the energy release during disassembly can be understood on the following basis. The pressure generated in the core ultimately shuts down the reac-

FIG. 1. Available work as a function of Doppler coefficient (A_{Dop}) for various reactivity steps above prompt critical (k_0). Excursions started from normal operating conditions (2200-MWt power) but without Doppler feedback until the peak temperature in the core reached the threshold for core disassembly. (Spherical meltdown model.)

tor. However, because of the inertia of the core, there is a significant time lapse between pressure buildup and core motion so that the power (and thus the energy density) continues to increase even after the pressure tends to move fuel out of the center of the core. On the other hand, when there is a strong Doppler effect, the transient is turned around and the power is reduced by the Doppler effect. This provides time for the core to be disassembled by relatively low pressure. This point is illustrated by Fig. 2, which is a plot of the reactivity feedback at the time that the power transient goes through its maximum. As can be seen in the case of zero or small Doppler coefficients, the major reactivity feedback at the time of the excursion turnaround is provided by the disassembly of the core. On the other hand, with a large Doppler coefficient, the major feedback is due to the Doppler coefficient, and the feedback due to the core disassembly occurs later in time.

2. Effect of Neutron Lifetime

For a given reactivity step, the period is decreased if the neutron lifetime is shortened. In this case, more energy will be generated during the period when the core is being disassembled. On the other hand, in the presence of a large Doppler effect, one would expect an insensitivity to the neutron lifetime since the reactivity feedback from Doppler depends only on the energy density in the core. That these speculations are, in fact, valid can be seen from Fig. 3, where the

available work is plotted for excursions initiated by a reactivity step at operating conditions as a function of the Doppler coefficient and the neutron lifetime. One notes that for A_{Dop} of the order of -0.005 and above, the energy release becomes essentially independent of the neutron lifetime.

3. Effect of Pressure-Energy Density Relationship

Although considerable effort has been expended in deriving the equation of state and thermodynamically consistent equations have been derived, one must point out that both the experimental data underlying the work and the theoretical assumptions do not have a very firm basis. To check the sensitivity of the results to the pressure-energy density relationship, a number of calculations have been performed in which the pressure-energy density relationship was arbitrarily varied. Again, one would expect that with the large Doppler coefficient, one might not be very sensitive to the pressure-energy density relationship. This point is made in Fig. 4 which shows the explosive energy release for a step excess reactivity input of $0.0005 \Delta k/k$ as a function of the Doppler coefficient for cases where the pressure-energy density relationship has been varied by factors of 10. With a Doppler coefficient of $A_{Dop} = -0.005$, the explosive energy release is only increased by 20% when the pressure is reduced by a factor of 100.

With zero Doppler coefficient, the energy release increases approximately inversely as the square root

FIG. 2. Doppler and disassembly reactivity feedbacks at the turn-around point in an excursion for various values of A_{Dop} for the $0.0005\text{-}\Delta k/k$ step excess reactivity input shown in Fig. 1. (Spherical meltdown model.)

FIG. 3. Available work as a function of A_{Dop} for excursions initiated by a $0.001\text{-}\Delta k/k$ step excess reactivity input at operating conditions with normal neutron lifetime (5.7×10^{-7} sec) and one-tenth lifetime. Doppler feedback was not included until the peak temperature in the core reached the threshold for core disassembly. (Spherical meltdown model.)

of the factor by which the pressure is reduced; reduction of pressure by a factor of 100 causes an increase somewhat less than a factor of 10 in explosive energy release. A brief discussion is in order, which we will find of use in a later discussion.

In the Bethe-Tait analysis, the disassembly reactivity can be written as

$$\ddot{D} = \frac{1}{\rho} \int P(\nabla^2 F) dV,$$

where $\ddot{D}$ is the second time derivative of the reactivity feedback D due to disassembly, P is the pressure at a point in the core, ρ is the smeared density of the core material, and F is a function which gives the impor-

FIG. 4. Effect of the pressure-energy density relationship on the available work obtained from an excursion for various values of A_{Dop} . Excursions initiated by a $0.0005\Delta k/k$ step excess reactivity input at operating conditions with no Doppler feedback until the peak fuel temperatures reached the threshold for core disassembly. (Spherical meltdown model.)

tance of a unit of fuel as a function of core position. In addition to this term, there are normally included boundary terms. However, in the case of the large reactors considered here, the excursion is terminated well before the fuel at the boundary reaches the boiling point, so that these boundary terms do not enter into the calculations. (An alternative way of stating this is that where the boundary terms enter in, the accident is of such a magnitude that the possibility of containment is remote.) The previous results (see Fig. 4) were obtained by multiplying the integral on the right by a factor which reduces the pressure and therefore the disassembly reactivity feedback by factors of 10.

If it is assumed for the moment that the pressure is constant in time, then a given reactivity feedback D would be proportional to t^2 , where t is the time. Thus reduction of P by a factor of 100 can be accommodated by an increase in t by a factor of 10. If the power were constant, this would result in an increase in the energy by a factor of 10. Actually, during the transient the power is increasing with time, as is the pressure and the volume of the core where the pressure is greater than zero. Our results indicate that, despite this complicated behavior, the energy release does depend approximately inversely as the square root of the pressure; in fact, for small changes in pressure, the dependence is not quite this strong. Figure 5, plotted for pressure reductions of 2 and 5, shows that a reduction of pressure by a factor of two increases the energy release by only about 30%.

4. The Effect of Power Flattening

In the calculations discussed above, the power shape was assumed to be characteristic of a uniform spherical reactor, 226 cm in diameter, with an extrapolation length of 20 cm. In order to increase the average specific power of the fuel in power reactors, it is sometimes proposed that either enrichment or compositions be varied so that the proper shape in the core is flattened. Such power flattening has an adverse effect on the energy release in a meltdown accident, i.e., it increases the energy release for a given accident. Figure 6 illustrates the physical reason for this. Shown is the power distribution during the excursion in the case where there is no power flattening. At the time when the core disassembly takes place, substantial pressures have built up in the central region of the core at the power peak; however, as indicated in the figure, the regions in the outer part of the core which make up most of the core mass have not reached the energy density characteristic of boiling. Thus, most of the core does not contain vaporized fuel which would generate explosive forces. On the other hand, when the core is flattened, a much larger percentage of the core will be in the vaporized state at the time that sufficient pressures have built up near the core center to cause disassembly of the core.

In principle, one would like very high pressures to be generated in a very small portion of the core in order to terminate the accident with a minimum amount of explosive energy release. Flattening the core does the

FIG. 5. Effect of the pressure-energy density relationship on the available work obtained from an excursion for various values of A_{Dop} . Excursions initiated by a $0.0005\text{-}\Delta k/k$ step excess reactivity input at operating conditions with no Doppler feedback until the peak fuel temperatures reached the threshold for core disassembly. (Spherical meltdown model.)

DISTANCE FROM CORE CENTER

FIG. 6. Typical energy-density distribution in a spherical core during an excursion terminated by core disassembly.

reverse of this. Figure 7 shows the results of calculations which were performed on cores with various assumed peak-to-average power distributions. It can be seen that decreasing the peak-to-average power from the unflattened value of 1.98 to a value of 1.33 increases the explosive energy release by a factor of 4 with no Doppler effect. Decreasing the peak-to-average further to a value of 1.03 increases the explosive yield by another factor of 9. Further, it can be seen that the Doppler effect is not as effective in reducing the energy yield for the flattened core. In the unflattened case,

the Doppler coefficient has the effect of reducing the power level while only a small part of the core is involved in the disassembly process. In the case of the flattened core, such a large portion of the core is already involved in the disassembly process that core disassembly occurs before the Doppler effect can exert much influence toward shutting down the reactor. In addition, the average temperature is higher in the flattened case during disassembly so that the Doppler feedback is lower for a given value of A_{Dop} .

It should be noted that the spherical case considered

FIG. 7. Available work as a function of A_{Dop} for various degrees of power flattening. Excursions started from operating conditions but without Doppler feedback until the peak temperature in the core reached the threshold for core disassembly.

FIG. 8. Pressure-temperature relation for three reactor core compositions.

overpredicts the effect of flattening since it implies flattening in all three dimensions. In actual reactor design, it is normally the practice to flatten only in the radial direction and not to attempt power shaping in the axial direction. This point will be discussed later with reference to the calculations on the cylindrical core.

5. Variation in Core Composition

Equations of state were developed for several core compositions. The pressure-temperature relationship is shown in Fig. 8 for three cases. The first corresponds to the 1000-MWe reactor, as described above (GEAP-4418), the second to a 1000-MWe reactor of composition different than the one of GEAP-4418, and the

FIG. 9 Available work as a function of β_{Dop} for various reactivity steps at one prompt critical for three reactors having different compositions but with equal fuel mass. Excursions started from operating condition but without Doppler feedback until the peak temperature in the core reached the threshold for core disassembly (Spherical meltdown model).

third to the Southwest Experimental Fast Oxide Reactor (SEFOR). The compositions for SEFOR and the alternate 1000-MWe reactor are shown on Fig. 8. In all cases, the first part of the curve corresponds to the saturated vapor pressure of the fuel. The breakaway from the saturated vapor pressure curve corresponds to the situation when there is one phase which completely occupies the available space. It should be pointed out that the use of the SEFOR equation of state above the break point is questionable, since the sharp rise in pressure is due to hydrostatic forces of the liquid. Thus only a small increase in volume would relieve these pressures and cause the pressure to revert back to the saturation curve. In the SEFOR hazards analysis, therefore, we did not use the break in the curve, but assumed that the pressure continued along the saturation curve.

However, in order to gain some insight into the effect of the different equations of state, we have performed calculations with the three curves of Fig. 8. Figure 9 shows the result of these calculations. As one would expect from the previous discussion regarding the pressure-energy density relationship, the energy release is not very sensitive to the equation of state. For low Doppler effect, one can see the effect of the sharp break in the SEFOR curve. On the other hand, the 1000-MWe reactors have virtually the same energy release for this reactivity step because the peak temperatures did not reach the point at which the

pressure-energy density curves for the two cases diverge. In performing these calculations, cores of equal mass were assumed. Thus the conclusion is that the energy release from a meltdown accident depends predominantly on the core mass and is relatively insensitive to the core composition for reactors using the same fuel material.

IV. Cylindrical Reactors

The equations for the reactivity effect of disassembly of the core have been derived in cylindrical geometry by means of the Bethe-Tait approximations, with the additional assumption that the flux is separable in the radial and axial directions. The derivation of the mathematical expressions will be described in detail in a paper now under preparation.

1. Reactors of Various Height-to-Diameter Ratios

One would expect that a cylindrical core with a diameter-to-height (D/H) ratio of 1 would give results similar to that for a spherical reactor with the same core volume. As a check on the cylindrical derivations, calculations were performed comparing a spherical reactor and a cylindrical reactor with D/H = 1. These calculations show good agreement between the two models. One example of such a comparison is shown in Figure 11 to be discussed shortly.

One might also expect that where the Doppler effect is controlling, the energy release from a pancaked

FIG. 10. Total energy release as a function of A_{Dop} for various degrees of pancaking calculated with the cylindrical meltdown model. Excursions started from operating conditions but without Doppler feedback until the peak temperature in the core reached the threshold for core disassembly.

cylindrical core would be little different from that of a spherical core of the same mass. That this is so is shown in Figure 10, which compares the total energy of an excursion for cylindrical cores of the same mass with various D/H ratios. One can see that for high Doppler coefficients, the total energy is very close. What is surprising is that the energy release is also very close in the case in which the Doppler coefficient is zero. In order to explain this rather unexpected result, we have looked at the disassembly equation and compared the value of $\nabla^2 F$ at the core center for the cases in which $D/H = 1$ and $D/H = 10$. It turned out that there is only about a 30% difference in these two cases, with that for the $D/H = 10$ being the larger.

In the previous discussion of the effect of reducing the pressure-energy density relation by large factors, we concluded that the energy release was relatively insensitive to a constant factor multiplying the disassembly equation. Thus the insensitivity to core shape can be explained from the mathematics involved. Physically, what appears to be the explanation is that the pressure and the reactivity worth function F are both spread out spatially by changes in core geometry such that the effect on reactivity by motion of the fuel caused by the pressure buildup is relatively insensitive to the core geometry.

2. Power Flattening in the Cylindrical Geometry

Figure 11 shows the total energy release for cases in which the core power distribution has been flattened in the radial direction. This is to be compared to the

power-flattening calculations previously performed for the spherical core. As one would expect, the adverse effect of power flattening is not as great in the cylindrical case as in the spherical case, since in the cylindrical case the peak-to-average ratio in the axial direction was not changed. Further, as can be seen, the Doppler effect is still quite effective in reducing the energy release for a power-flattened cylindrical core as compared to the spherical core, since it acts to shut down the reactor before a very large part of the core is above the boiling point, and at a lower average temperature as compared to the flattened spherical case.

V. Excursions Starting below the Threshold for Core Disassembly

The previous calculations have considered the meltdown accident from the point in time at which boiling of fuel first starts. This was done so that one could gain a physical understanding of the factors which influence the disassembly process. In addition, the behavior of the reactor (although complicated prior to the time when boiling begins) is determined by the standard kinetics equations which themselves are well understood. However, a number of calculations have been performed in which the course of the accident has been followed from operating temperature. The results are different from those previously discussed only in the case in which a large Doppler effect acts.

Where a prompt negative coefficient is present, there are two major effects. First, in going from a low energy density to that at which boiling begins, the negative

FIG. 11. Total energy release as a function of Δ_{Dop} for various degrees of power flattening. Curves show the difference in power flattening in one dimension (cylindrical meltdown model) and in three dimensions (spherical meltdown model). Excursions started from operating conditions but without Doppler feedback until the peak temperature in the core reached the threshold for core disassembly.

FIG. 12. Power and energy density as a function of time for excursions initiated by a ramp reactivity insertion from normal operating conditions with Doppler feedback from the beginning of the excursion

coefficient will reduce the reactivity so that the disassembly process will start with a smaller net reactivity than is the case without a negative coefficient. However, the more interesting behavior is the well-known oscillation in power which can occur with the presence of a large negative coefficient. This is illustrated in Fig. 12, which shows the power oscillations for a particular case. If these oscillations occur before fuel boils, the energy release during disassembly will depend

strongly upon the part of the power oscillation at which core disassembly first starts. For example, if disassembly should start near the peak of an oscillation, the reactivity at the time that boiling begins will be small and there will be time for the core to start disassembling before the reactivity increases on the next cycle. Thus there exists a sensitivity to the fine structure in the power trace and, in turn, to the magnitude of the Doppler effect, the reactivity insertion rate, and

FIG. 13. Available work as a function of Δ_{Dop} for excursions initiated by ramp reactivity insertions from normal operating conditions with Doppler feedback from the beginning of the excursion.

the lifetime of the core. Figure 13 is a plot of the explosive energy release for the spherical 1000-MWe reactor assumed to be operating at the steady-state power of 2200-MWt at the time of a reactivity insertion. Reactivity insertion rates of 40\$, 60\$, and 100\$ per second are considered. One notes from the figure that even small Doppler coefficients have a substantial effect prior to the point of core disassembly. However, it can also be seen that for high Doppler coefficients, where more than one oscillation in the power can take place, the energy release can be higher for a lower rate of reactivity insertion. Alternatively, one can see that the energy release for a given reactivity insertion can be higher in some cases for a higher Doppler coefficient.

Similar behavior is observed when one examines the effect of initial power level on energy release. Although reducing the initial power level generally results in an increase in energy release, one can see from Figure 14 that in the oscillatory situation with a large Doppler coefficient, a reversal of this behavior can occur.

Figure 15 shows results of calculations in which the average initial temperature of the fuel was changed. It can be seen that with no Doppler effect an increase of initial temperature at the start of the reactivity ramp will cause a decrease in energy release. This is because less reactivity is inserted before core disassembly occurs. On the other hand, with a large Doppler effect, a reduction in starting temperature reduces the energy release since the Doppler effect causes a larger reduction in the net reactivity as the

temperature is increased to the point of core disassembly. This more than compensates for the added reactivity inserted by the ramp. Again, when power oscillations occur, this behavior may be reversed.

Figure 16 shows results of calculations in which the neutron lifetime has been reduced by a factor of 10 for an accident initiated by a ramp reactivity input and starting from the 2200-MWt power level. In the case in which we considered the accident only from the time when core disassembly first occurred, a smaller neutron lifetime increased the energy release for small or zero Doppler effects, whereas with a high Doppler effect, there is an insensitivity to the neutron lifetime. In looking at the accident with Doppler feedback starting from a lower energy density in the core, one notes that there is a crossover. For zero Doppler effect, a small neutron lifetime increases the energy release. However, in the presence of a large Doppler effect, a short neutron lifetime reduces the energy release. A short neutron lifetime causes the reactor power to increase at a greater rate for a given reactivity insertion. Thus, at the time that core disassembly starts, the inserted reactivity is smaller; with a large Doppler coefficient, the resultant disassembly energy is reduced. On the other hand, for zero Doppler effect, the short period during disassembly results in an increased energy release. When one counts on a negative temperature coefficient to reduce the explosive energy release in a meltdown accident, as appears necessary for large power reactors, it is of no advantage to have

FIG. 14. Available work as a function of A_{Dop} for excursions initiated by ramp reactivity insertions from normal operating power (2200 MWt) and one-tenth power (220 MWt) with Doppler feedback from the beginning of the excursion. These calculations used a peak-to-average power ratio of 1.91.

FIG. 15. Available work as a function of A_{Dop} for excursions initiated by ramp reactivity insertions starting from various initial temperatures and normal operating power (2200 MWt) with Doppler feedback from the beginning of the excursion. These calculations used a peak-to-average power ratio of 1.91.

a long neutron lifetime. In fact, it is a disadvantage. Schemes involving coupled fast-thermal reactors, which claim benefit because of the longer resulting neutron lifetime, should therefore be looked at with suspicion. This point was made at the last Argonne Conference.⁽¹⁾

The preceding comments are general in nature and do not consider the fine structure which results from the oscillatory nature of the power when one starts excursions from low energy density in the presence of a large Doppler coefficient. As can be seen from Figure 16, in the region of oscillations, one can sometimes

FIG. 16. Available work as a function of A_{Dop} for excursions initiated by a ramp reactivity insertion with normal neutron lifetime and one-tenth lifetime. Excursions started from normal operating conditions with Doppler feedback from the beginning of the excursion.

get reversals such that a shorter neutron lifetime produces a greater energy release than a longer neutron lifetime.

VI. Conclusions and Observations

The calculations described were intended to permit an understanding of the meltdown accident under various conditions. Aside from the points made in the discussion above, the authors would like to conclude with the following observations:

1. For meltdown rates of the order of tens of dollars per second in large power reactors, the presence of a large Doppler effect can reduce the energy release to values for which containment capability has already been engineered in the present generation of experimental reactors.

2. The fine structure produced by power oscillations in the presence of a large negative temperature coefficient must be taken into account in an accident analysis. The designer should be sure that he has designed for the envelope defining the maximum energy release if a meltdown and compaction accident is chosen as a design basis.

3. We have shown that in the presence of a large Doppler effect, there is an insensitivity to most of the parameters involved in the meltdown calculations. However, there is a sensitivity to the heat capacity of the fuel. Doubling the heat capacity has about the same effect as reducing the Doppler effect by a factor of 2. Although it would be of value to have more defini-

tive information on the pressure-energy density relationship, it is the recommendation of the authors that attention first be applied to obtaining heat capacity information. Fortunately, this may be easier to obtain than the full equation of state. (In the calculations described above, a value of $13/2 R$ was assumed for the heat capacity of the fuel above the melting point.)

It should also be noted that, in terms of minimizing energy release from a meltdown accident, it is advantageous to have a fuel with a low heat capacity.

4. The energy release calculated in the examples given above involve the assumption of an isentropic expansion of the vaporized fuel. This is a very pessimistic assumption, of course, and calculations by Jankus⁽⁹⁾ have indicated that in some cases, the energy release would be overestimated by as much as a factor of 10. We have looked at the results of the one experiment performed in this field, the KIWI experiment. Based on the data which has been released and discussions with W. R. Stratton of Los Alamos, we estimate that the assumption of an isentropic expansion would have overestimated the energy release from KIWI by a factor of approximately 6. If this factor, or a similar factor, could be verified for situations appropriate to power reactors, this would greatly ease the design of containment.

5. As noted in the Introduction, one should not infer from this investigation a conclusion that the meltdown accident is necessarily a credible accident.

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Discussion

Mr. Schlechtendahl (Karlsruhe): You mentioned that a flat core and a compact core give about the same energy release for the same Doppler. I would presume that these calculations were made with the same peak-to-average value. Isn't it the case that with the flat core you have a lesser peaking of the power profile in the core than with the compact core, so that from this as a whole the flat core would look less favorable than the compact core?

Mr. Wolfe: Yes, I think in general that's right. Actually, we did these calculations with a constant reflector savings; thus, some of the effect you're talking about is already included. I don't know what the difference would have been if we had made the flux go to zero at the core boundary rather than an extrapolated boundary. But I think the interesting thing is that even from the 10 to 1 case, which is rather extreme, we didn't see very much difference; we do have a lower peak-to-average ratio due to the fact that we have the 20-cm extrapolation distance in the axial direction, so the vertical peak to average is not as big as it would be if we made it go to zero. I want to point out that these calculations only give the total energy and not the work for the cylindrical case.

Mr. Campise (AI): How did you handle the difference in the importance functions for the axial and

radial cases in the cylindrical model? What volume of the core was involved in this accident?

Mr. Wolfe: In the cylindrical case we made the assumption that axial and radial fluxes would be separated, and we did a calculation taking first-order terms in the Bethe-Tait analysis. Incidentally I might point out that if you go through previous developments in the Bethe-Tait equations, you find they are not correct to first order. But the first order correction is not very large.

Mr. Häfele (Karlsruhe): I would like to make a comment in this respect for coupled reactors. I think that in evaluating the maximum credible accidents in the present state of the art one does realize that a flat power distribution gives considerably larger energy releases and a flat power distribution exists in coupled reactors. Therefore, what originally started out to be a safe design could perhaps be a somewhat more dangerous design. Secondly, a longer lifetime might be perhaps disadvantageous. Therefore the coupled-reactor concept should be re-evaluated in the light of these two facts.

Mr. Okrent (ANL): In view of the relatively weak dispersal mechanisms which may exist in the presence of a large negative Doppler effect, and since the core just oscillates about a high power for some accidents, I wonder whether one has not the potential source for a new difficulty, namely, a rather long-term, relatively large heat source which provides a serious problem of pressure and heat removal.

Mr. Wolfe: I should point out that you don't get the oscillator behavior in the disassembly. Once enough energy is in above the threshold, disassembly occurs. It's just a question of whether that happens on the first pulse or the second pulse or in between. Secondly, I think it's very improbable that you can have the core cooking away at very low power. It seems to me there are two possibilities. If the sodium is lost, the power is limited because of melting. If melting occurs, another criticality accident is obtained, but usually in this kind of analysis, the first one occurs at tens of dollars a second and the second one would be very minor relative to that.

Mr. Okrent: There was one virtue to the high-pressure accident: there was only one and there was a finite amount of energy to contain. I wonder whether one is so confident that there isn't a longer-range heat source in this case.

Mr. Stachura (AI): At the time when the burst was turning over, about what range of dollars had been put in? If it's substantial, the question may be whether this insertion can be realistically placed in terms of control rod, whether there are some mechanisms to do

that, or whether it really requires material distribution. If it requires material redistribution, then the possibility is that flux shapes are not at all normal. With unusual flux shapes the pressure could be greatly altered and thus the mode of disassembly.

Mr. Wolfe: On the amount that gets involved in some of these accidents, my recollection is that something like 30 to 50% of the core gets above the melting

point in these accidents for reasonable power shapes in the core, that is, for a peak-to-average ratio of the order of 1.8 or so. Less than 10% of the core actually vaporizes.

Mr. Stachura: I was thinking in terms of dollars of reactivity.

Mr. Wolfe: The reactivity of 0.001 Δk is equivalent to \$50 a second or so.

A Modified Equation of State for Hydrodynamic Calculations in AX1 Numerical Program

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Introduction

An investigation of a hypothetical excursion in a fast reactor is concerned not only with the total energy yield but also with the fraction of it available for the damage of reactor containment structure. This fraction decreases quite rapidly with decreasing energy yield and usually is between one and ten percent for the accidents presently considered even remotely possible. Work available adiabatically and eventual kinetic energy yield in an uncontained reactor are often considered to be pessimistic and optimistic estimates of destructive effectiveness of an excursion. Both estimates depend strongly upon the behavior of the equation of state at large volumes. Although in a contained excursion large expansions are improbable, the damage to the reactor containment is expected to require consideration of larger specific volumes than are needed for the shutdown of power generation and calculation of total energy yield. With this point of view the equation of state presently used in AX1 program has been examined. To calculate both large and small excursions two analytical formulations are needed. For large pressures, the equation of state is assumed to be in generalized Bethe-Tait form. For lower pressures, utilization of equation of state for saturated vapor seems necessary.

1. Equation of State for Large Pressures

The calculation of energy yield with AX1 code usually is made with a simple equation of state proposed first by W. R. Stratton. This equation assumes that pressure p is a linear function of density ρ and temperature θ , given by

$$p = \tau + \beta\theta + \alpha\rho, \quad (1)$$

where τ is a negative quantity. Pressure is considered to be a positive quantity, so Eq. (1) is used only for sufficiently high values of θ and ρ . For lower values, when Eq. (1) gives a negative number, pressure is supposed to vanish, that is

$$p = 0 \quad \text{for} \quad \tau + \beta\theta + \alpha\rho < 0. \quad (1a)$$

In addition to pressure an expression for specific heat

is needed. It is assumed to be a linear function of temperature

$$C_v = A + B\theta, \quad (2)$$

both for positive and vanishing pressure. The latter assumption is logically inconsistent; the equation of state is overdefined.

From the second law of thermodynamics, we know that

$$\left(\frac{\partial e}{\partial v}\right)_\theta = \theta \left(\frac{\partial p}{\partial \theta}\right)_v - p. \quad (3)$$

Thus pressure given in terms of absolute temperature and specific volume determines also variation of internal energy e with specific volume. Differentiating Eq. (3) with temperature, we obtain

$$\frac{\partial^2 e}{\partial v \partial \theta} = \left(\frac{\partial C_v}{\partial v}\right)_\theta = \theta \frac{\partial^2 p}{\partial \theta^2}. \quad (4)$$

Thus variation of specific heat with volume is already prescribed by the "thermal" equation of state Eq. (1) and Eq. (1a). Equation (4), of course, is satisfied by Eq. (2) in each of the regions of positive and vanishing pressures. However, it is not satisfied at the boundary between them. If we disregard this and calculate energy in two regions separately by use of Eq. (1) with Eq. (2) for positive pressures and of Eq. (1a) with Eq. (2) for vanishing pressures, we would find a discrepancy in the internal energy at the intersection of those two regions. Now if we assume that Eq. (2) is correct for positive pressure, we must take

$$C_v = A + B\theta + \frac{\alpha\beta^2\theta}{(\tau + \beta\theta)^2} \quad (2a)$$

when $\tau + \beta\theta + \alpha\rho < 0$. This modification usually would have little effect on determination of total energy yield with AX1 program, if threshold energy density and other parameters are properly adjusted. However, numerical inconsistencies occur when the calculation is continued after the power burst and reactor material expands to the densities appreciably different from initial.

One certainly could modify Stratton's equations by

assuming that (2) is valid for vanishing pressure. Then specific heat is decreased by a proper amount for positive pressures. Now the expression for specific heat at vanishing pressure would have a simple form. However, specific heat at positive pressures becomes negative when $\theta \rightarrow -\tau/\beta$, and the equation of state departs from physical reality.

Although the adoption of Eq. (2a) when pressures vanish removes logical inconsistency from Stratton's equation, it still leaves us with an equation of state that is physically unsound when appreciable variation of density is allowed. The use of Eq. (2a) for vanishing pressures gives a threshold energy density Q^* that increases indefinitely with decreasing density. In addition, the rate of initial rise in pressure with excess energy is a constant (β/A) independent of the density. Thus the corresponding Bethe-Tait parameter, $\gamma - 1 = v(\beta/A)$, increases with increasing specific volume v contrary to simple common sense. These two physical reasons seem to demand further modification of Stratton's equation.

Consider a general situation in which specific heat depends on the temperature only (except at the threshold of pressure generation):

$$\left(\frac{\partial e}{\partial \theta}\right)_v = C_v = A'(\theta). \quad (6)$$

From Eq. (4) we see that pressure is a linear function of temperature:

$$p = \beta(v)\theta - f'(v), \quad (7)$$

where the coefficients β and f' may be any function of specific volume. Equation (7) is now quite general. It encompasses not only Stratton's equation but also the commonly used Mie-Grüneisen and van der Waals equations. Integrating (6) with respect to θ and recalling Eq. (3), we obtain in straight-forward fashion

$$e = A(\theta) + f(v). \quad (8)$$

If we eliminate θ by means of Eq. (7), we obtain energy expressed in terms of pressure and density. In particular, if specific heat is constant, $A(\theta) = A\theta$, energy becomes a linear function of the pressure:

$$e = \left(f + \frac{A}{\beta} f'\right) + \frac{A}{\beta} p. \quad (9)$$

Thus the connection between pressure and internal energy is in Bethe-Tait form

$$e_{BT} = Q^*(v) + \frac{A}{\beta} p, \quad (10)$$

where the threshold energy density Q^* is given by

$$Q^* = f + \frac{A}{\beta} f' \quad (11)$$

and the rate of pressure rise (β/A) may depend on specific volume. Given $Q^* = Q^*(v)$ from Eq. (11) we can determine f , though not uniquely:

$$\int \exp \int^v \frac{\beta}{A} dv = \int^v dv' Q^* \frac{\beta}{A} \exp \int^{v'} \frac{\beta}{A} dv''. \quad (12)$$

The entropy is also a relatively simple expression:

$$s = A \ln \theta + \int \beta(v) dv.$$

Thus, an isentropic process is described by the equation

$$A\theta \exp \int^v \frac{\beta}{A} dv = \text{const}, \quad (13)$$

or, if θ is eliminated by use of Eq. (8),

$$\begin{aligned} (e - f) \exp \int^v \frac{\beta}{A} dv \\ \equiv (p + f') \frac{1}{\beta} \exp \int^v \frac{\beta}{A} dv = \text{const}. \end{aligned} \quad (14)$$

The velocity of wave propagation, c , is given by

$$c^2/v^2 = (\beta/A) \{Q^{*'} + p[(A/\beta)' + 1]\}. \quad (15)$$

The equations of state (6) and (7) for positive pressures determine again the specific heat in the region of vanishing pressure. The computation of it, however, is even more complicated. Therefore, it seems advantageous to eliminate temperature from equation of state analytically, and let the numerical program do simpler calculations operating only with the relationship among pressure, internal energy, and density. After all, the temperature does not enter explicitly in isentropic wave propagation nor in shock conditions that are governed by the Hugoniot-Rankine equations. All that is necessary is the relationship among internal energy, pressure, and specific volume. Moreover, this relationship can be checked by experiments involving shock propagation, but not the temperature. However, having accepted Eq. (10) as an only equation of state, we cannot find the absolute temperature uniquely, and we acquiesce in the possibility that the specific heat may depend on volume and the relationship between pressure and temperature may be nonlinear.

2. Equation of State for Saturated Vapor

Some experimental measurements as well as theoretical calculations⁽¹⁾ seem to indicate that for uranium pressure is linear function of internal energy (at least if the specific volume is not too large). For larger specific volumes the linearity is valid only at larger pressure. Indeed, one should expect this behavior since at larger specific volume part of the uranium can be found in liquid phase, another in the vapor phase. For

a two-phase mixture the pressure depends on temperature only:

$$\theta = \theta(p), \quad (16)$$

and using Eq. (3) we obtain internal energy in terms of specific volume and pressure:

$$e_{sv} = v \left(\frac{\theta}{\theta'} - p \right) + F(p). \quad (17)$$

The expression for entropy in this system is quite simple:

$$s = (v/\theta') + \int dp F'(p)/\theta,$$

and an isentropic process is described by the equation

$$(v/\theta') + \int dp F'(p)/\theta \equiv (e - F)/(\theta - p\theta') \\ + \int dp F'(p)/\theta = \text{const.} \quad (18)$$

The velocity of wave propagation, c , is obtained from the expression

$$v^2/c^2 = (\theta'/\theta) F'(p) - v(\theta''/\theta'). \quad (19)$$

If one assumes (for moderately low temperatures) that the heat of vaporization is constant and the vapor behaves like an ideal gas, one can demonstrate that

$$\theta = (L/R) \ln (p_1/p), \quad (20)$$

where L is the heat of vaporization, R the gas constant, and p_1 an adjustable parameter. Experimentally this relationship is found valid over a relatively large range of pressures. For moderate specific volumes the energy is given essentially by the second term of Eq. (17). For "low" temperatures it can be assumed to be proportional to the absolute temperature, and the equation of state becomes

$$e_{sv} = vp \left(\ln \frac{p_1}{p} - 1 \right) + \frac{A^l L}{R \ln (p_1/p)}, \quad (21)$$

where A^l is the specific heat of the liquid at constant pressure. The velocity of wave propagation is calculated to be

$$1/c^2 = (A^l L/R) \ln^{-3} (p_1/p) (pv)^{-2} \\ + [1 - 2/\ln (p_1/p)] (pv)^{-1}. \quad (22)$$

For moderate specific volumes the first term is strongly dominant, and c is practically proportional to the pressure. The work done in an isentropic expansion, w , can be calculated in straight-forward manner by use of Eq. (18):

$$\frac{w}{A^l(\theta_i - \theta_f)} = \left[1 - \left(1 - \frac{R\theta_f}{L} \right) \frac{\theta_f \ln (\theta_i/\theta_f)}{\theta_i - \theta_f} \right] \\ + \frac{v_i p_i}{A^l \theta_i} \left[\frac{L}{R\theta_i} - 1 - \frac{\theta_f}{\theta_i} \right], \quad (23)$$

where the subscript i refers to the initial conditions and subscript f to the final temperature (and pressure). For uranium the first term is dominant. It has been obtained previously by Russell and Morris⁽²⁾ by use of physical arguments. We notice, however, that a very large expansion is required to achieve this efficiency. Thus the theoretical efficiency may be somewhat pessimistic.

Since for moderate specific volumes the first term is very small, pressure can be obtained as an explicit function of internal energy, i. e.,

$$p \approx B \exp [-A/(e + e_0)].$$

Using this relationship Nicholson⁽³⁾ has calculated energy yield by generalized Bethe-Tait method, i.e., neglecting the propagation of pressure wave. Compared with threshold equation of state, these calculations have shown a significant decrease of energy yield for low reactivity input. A disadvantage of this equation of state is that it is clearly valid only for a limited range of pressures. According to this equation, internal energy blows up when pressure approaches p_1 . Actually, one can hardly trust this equation for pressure that makes $(\partial^2 e/\partial p^2)$ negative, i.e., $p > p_1 \exp (-2)$. Moreover, the number of parameters used in Nicholson's equation is small, and when one adjusts parameters to represent a two-phase region, one is unable (in general) to fit behavior of uranium in the Bethe-Tait region, where the relationship between pressure and internal energy depends strongly upon specific volume. Numerical programs using this equation of state,⁽⁴⁾ however, can be easily amended by introducing linear dependence of pressure upon internal energy after the voids have been filled. Although the introduction of saturated vapor pressure yields smaller total energy, this is partially compensated by a larger efficiency of conversion of this energy into the isentropic work. For the threshold equation of state at small yield, available work is given by $w = p^2 v^2 / 2c^2$, where the velocity of wave propagation is a fairly large constant. The efficiency of conversion in the saturated vapor system is at least $R\theta_f/L$ [see Eq. (23)] or significantly larger. In addition, we know that Bethe-Tait method usually is less valid for the excursions of lower initial period.

Speaking qualitatively, a large deviation is caused by the ability of the pressure wave to traverse the core during the initial period of the excursion. Although the velocity of wave propagation in saturated vapor is small compared with the wave velocity in the threshold

equation of state, it increases with increasing pressure. Thus, in some cases complete numerical calculation may result in a significant increase also of total energy yield. Such coupled neutronic-hydrodynamic calculations have been done in connection with reactor VERA.⁽²⁾ However, in these calculations the first term in the equation of state (21) has been neglected. This has resulted in an effective velocity of wave propagation, c_{eff} , given by

$$1/c_{\text{eff}}^2 = (A^l L/R) \ln^{-2} (p_1/p) (pv)^{-2},$$

several times smaller than calculated by means of Eq. (22). Thus full deviation from the Bethe-Tait type of calculation has not been realized.

To approximate the behavior of uranium during power bursts of medium size and after, we need both the Bethe-Tait and saturated vapor equations of state. Each gives an internal energy as an explicit function of pressure and specific volume. Equation (17) should be used when it gives lower energy than Eq. (10). We have seen, however, that Eq. (21) deviates strongly from reasonable behavior when the pressure is large. Indeed, comparing Eq. (21) and Eq. (10) for large specific volumes, we find a region, at fairly high pressures, when expression Eq. (21) yields lower values than Eq. (10). To eliminate this fictitious region in order to use Eq. (17) we must have not only $e_{BT} > e_{sv}$ but also $\partial e_{BT}/\partial p < \partial e_{sv}/\partial p$ and $\partial^2 e_{sv}/\partial p^2 > 0$. The last condition can be replaced by a stricter requirement $\ln (p_1/p) > 2$.

To use our equations of state in the numerical program we need only a minor modification of the calculational scheme. Instead of using e and v as independent variables, as has been done in COOT,⁽²⁾ we use p and v as independent variables. Instead of computing the change in energy, we compute the change in pressure:

$$dp = \left(\frac{\partial p}{\partial e} \right)_v dQ - \frac{c^2}{v^2} dv, \quad (24)$$

where $(\partial e/\partial p)_v$ and

$$\frac{c^2}{v^2} = \left[\left(\frac{\partial e}{\partial v} \right)_p + p \right] / \left(\frac{\partial e}{\partial p} \right)_v \quad (25)$$

are now computed by use of either Eq. (10) or Eq. (17). Each of these equations still contains two explicit functions. For the saturated vapor region we could use approximations:

$$\frac{\theta}{\theta'} - p = p \left[\ln \frac{p_1}{p} - 1 + \frac{c_1}{\ln (p_1/p)} \right] \quad (26)$$

and

$$F = \frac{A^l}{R} \ln \frac{L}{(p_1/p)} \left[1 + \frac{c_2}{\ln (p_1/p)} \right]. \quad (27)$$

However, only the parameters A^l , L/R , and p_1 usually are known quite accurately from "low" temperature measurements. Similarly, two functions in the Bethe-Tait equation of state could be approximated by the use of several parameters:

$$\frac{A}{\beta} = \frac{v - b_1}{\gamma_\infty - 1} \left[1 + \frac{\gamma_\infty - \gamma_s}{\gamma_s - 1} \frac{b_1}{v} \right]. \quad (28)$$

The approximation of threshold energy density over the whole range seems to require more parameters. The expression

$$Q^* = L(1 - b\rho)[\alpha_0 + \alpha_1(1 - b\rho) + \alpha_2(1 - b\rho)^2 + \alpha_3(1 - b\rho)^3], \quad (29)$$

with $\alpha_0 + \alpha_1 + \alpha_2 + \alpha_3 = 1$, may be of sufficient complexity but still manageable from a practical point of view. Here also only the value of $\alpha_0 L$ is known quite well from measurements at moderate temperatures. There are indications that α_1 is quite small. Other parameters could be inferred from shock measurements with the help of some educated arguments about corresponding states. The thermodynamic arguments, however, should be carried out to numerical evaluation of our four functions $\theta/\theta' - p$, $F(p)$, A/β , and Q^* , ignoring approximation by our forms (26), (27), (28), and (29). We find that the critical point is quite sensitive to minor changes in these functions. Thus our approximations usually will shift their location. This, however, is not expected to influence significantly either the total energy yield or the work available. For calculation of power burst we believe that the leading terms in Eq. (28) and Eq. (29) with parameters adjusted for initial density are adequate. For calculation of subsequent pressure wave other terms will have some influence.

3. The Absolute Temperature

The hydrodynamics calculations can be performed if we know the relationship among internal energy, pressure, and specific volume. In large reactors, however, shutdown is accomplished not only by bulk motion of core material. In a large reactor the Doppler effect is appreciable, and a small excursion may be shutdown mainly by changing effective cross sections which depend mainly on the temperature of the material. Since the temperature does not come into hydrodynamic calculation, we may postulate it separately as a function of internal energy and specific volume. Some arbitrariness in the choice of this function may be unavoidable if there is no thermodynamic equilibrium among various reactor materials.

However, if we assume existence of a two-phase region, the temperature there is determined presumably

by Eq. (16). If we consider the Bethe-Tait region alone, formally, the temperature (or rather $1/\theta$) there can be defined as an integrating multiplier in the second law of thermodynamics. The determination is not unique, and temperature can be varied by an "arbitrary" factor on each adiabat without formal contradiction. From our Eq. (13) and (14) we see then that the temperature obtained from relation

$$\theta \exp \int \frac{\beta}{A} dv = \mathfrak{F} \left\{ (e - f) \exp \int \frac{\beta}{A} dv \right\}, \quad (30)$$

where $\mathfrak{F}$ is an "arbitrary" function, will be consistent with Eq. (9). This arbitrary function can be found at the boundary line with another region where the temperature is known. In our case the temperature at the boundary of two-phase region is given, say, by Eq. (20), and the arbitrary function in Eq. (30) can be evaluated.

When the Bethe-Tait region is joined with a saturated vapor region even the pressure on the boundary line (p^*) cannot be found analytically as a function of specific volume, and analytical determination of $\mathfrak{F}$ in Eq. (30) is hard to contemplate. For any given value of v expansion of $\mathfrak{F}$ in power series is straightforward if $p^*(v)$ is known. If one limits this expansion to two terms, the resulting thermal equation gives a linear relationship between internal energy and temperature. In general, this relationship is valid only in the vicinity of the adiabat considered. The corresponding specific heat A varies with the particular adiabat chosen. It is relatively easy to obtain this specific heat for low temperatures. If one assumes that Eq. (20) is valid for low temperatures and considers large volumes, one finds that the specific heat for a gas is given by

$$A_g = \frac{Q^*(\infty)}{L} \frac{R}{\gamma_\infty - 1},$$

where

$$(\gamma_\infty - 1) = \lim_{v \rightarrow \infty} [v(\beta/A)].$$

If the gas is ideal the specific heat should be

$$R/(\gamma_\infty - 1).$$

Thus $Q^*(\infty)$ should be equal to the heat of vaporization. This requirement certainly can be compromised somewhat in order to obtain a better fit of the equation of state at more important specific volumes; likewise, one finds that the specific heat of the liquid is

$$A_l = A'_{\gamma_l},$$

where

$$\gamma_l - 1 = \lim_{v \rightarrow b} [(v - b)(\beta/A)]$$

and b is the value at which the threshold energy density vanishes:

$$Q^*(b) = 0.$$

The specific heat A along each adiabat can be calculated quite in general. However, the resulting expressions are rather complex. Let us suppose we have expanded Eq. (30) and used the first two terms:

$$\theta = \mathfrak{F}_0 \exp \left(- \int^v \frac{\beta}{A} dv \right) + \frac{1}{A} (e - f). \quad (31)$$

The two unknown coefficients can be determined for any given v by realizing that along the boundary between saturated vapor and the Bethe-Tait region both the temperature and its first variation coincide. The temperature in a saturated vapor region is a function of pressure alone and is given by Eq. (20) or, in general, by Eq. (16). Thus, along the boundary both pressure $p^* = p^*(v^*)$ and temperature $\theta^* = \theta^*(v^*)$ are functions of specific volume v^* . Substituting these values into Eq. (31), we obtain

$$\theta^* = \mathfrak{F}_0 \exp \left(- \int^{v^*} \frac{\beta}{A} dv \right) + \frac{1}{A} (e^* - f^*). \quad (32)$$

From Eq. (31) and Eq. (32) we can eliminate one unknown constant, say $\mathfrak{F}_0$, obtaining as an equation of state in the vicinity of the adiabat, and going through the point $[v^*, p^*(v^*)]$,

$$(e - f - A\theta) = (e^* - f^* - A\theta^*) \cdot \exp \int_v^{v^*} \frac{\beta}{A} dv, \quad (33)$$

where the specific heat A is still to be determined. Given $Q^* = Q^*(v)$, then f is not determined uniquely, as we have seen in Eq. (12). Equation (33), however, is not affected by this arbitrariness (as it should) if the same definition of f is used on both side of the equation.

The specific heat A now is determined by considering the variation of Eq. (32) along the boundary line. If energy e is eliminated from Eq. (32) by means of Eq. (9), one has

$$\theta^* = \mathfrak{F}_0 \exp \left(- \int^{v^*} \frac{\beta}{A} dv \right) + \frac{1}{A} \left(\frac{A}{\beta} \right)^* (p^* + f'^*). \quad (34)$$

Differentiating this with respect to v^* , we have

$$\begin{aligned} \frac{d\theta^*}{dp^*} \frac{dp^*}{dv^*} = & -\mathfrak{F}_0 \frac{\beta}{A} \exp \left(- \int^{v^*} \frac{\beta}{A} dv \right) \\ & + \frac{1}{A} \left\{ \frac{d}{dv^*} \left[\left(\frac{A}{\beta} \right)^* f'^* \right] + p \frac{d}{dv^*} \left(\frac{A}{\beta} \right)^* \right. \\ & \left. + \left(\frac{A}{\beta} \right)^* \frac{dp^*}{dv^*} \right\}. \end{aligned} \quad (35)$$

Eliminating $\mathfrak{F}_0$ from Eq. (34) and (35), we have (omitting the asterisks for simplicity)

$$\begin{aligned} \left[\frac{d\theta}{dp} \frac{dp}{dv} + \frac{\beta}{A} \theta \right] = & \frac{1}{A} \left[f' + \left(\frac{A}{\beta} f' \right)' \right. \\ & \left. + p + p \left(\frac{A}{\beta} \right)' + \frac{A}{\beta} \frac{dp}{dv} \right]. \end{aligned} \quad (36)$$

Here we are considering various quantities only along the boundary line of two regions. The variation of pressure with volume along this boundary is obtained simply by differentiation of the relationship defining the boundary line ($e_{BT} = e_{sv}$):

$$\begin{aligned} \left[\frac{A}{\beta} + v \frac{\theta\theta''}{\theta^{12}} - F' \right] \frac{dp}{dv} = & -f' - \left(\frac{A}{\beta} f' \right)' \\ & - \left(\frac{A}{\beta} \right) p - p + \frac{\theta}{\theta'}. \end{aligned} \quad (37)$$

Now substituting (dp/dv) from this expression into Eq. (36) and combining the terms, we obtain an expression for the specific heat A :

$$\begin{aligned} A\theta^* \left[\frac{c_{BT}^2}{c_{sv}^2} \left(\frac{\partial p}{\partial e_{sv}} \right)^2 + \left(\frac{\partial p}{\partial e_{BT}} \right)^2 \right. \\ \left. - 2 \left(\frac{\partial p}{\partial e_{sv}} \right) \left(\frac{\partial p}{\partial e_{BT}} \right) \right] = \frac{c_{BT}^2}{v^2} - \frac{c_{sv}^2}{v^2}. \end{aligned} \quad (38)$$

The quantities involved in this expression are routinely obtained during regular hydrodynamics calculations of

dp . The Doppler effect can be expected to exert the greatest importance at the earliest stages of the excursion. Thus it seems sufficient to evaluate specific heat A at the first time core material reaches the Bethe-Tait region and keep it the same for the remainder of the power burst.

The temperature can now be calculated from Eq. (33). It is strictly correct only along the adiabat going through the point (v^*, p^*) . This is quite sufficient

A Model Investigation of Explosion Containment in Single Tank Fast Reactors

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2. Introduction

The possibility of an accidental super-prompt critical excursion causing the core of fast reactor to disintegrate with explosive violence and the subsequent effect on the reactor structure has to be considered. Such an explosion may breach the containment structure, and so the ultimate safety of the reactor system will depend on the adequate design of the system to withstand the anticipated explosive effects. Thus the magnitude of the nuclear explosion must be related to its explosive effects on the reactor under consideration.

The explosion of fast reactor cores has been examined in excursion calculations by Hicks,⁽¹⁾ which gave the energy required to expand the fuel to fill all the voids in the reactor core and the energy released in excess of this during the excursion, together with the times taken. The direct calculation of the mechanical effects of this type of nuclear explosion would be prohibitively complicated, and its simulation by the detonation of a charge of chemical high explosive is discussed by Samuels.⁽²⁾ He concludes that if the excess energy of the nuclear excursion is equal to the energy of detonation of the chemical explosion, then in each type of explosion the mechanical effects will be very similar up to the end of the first expansion of the explosion cavity.

In this paper a series of model experiments is described which provide data for containment design in a single-tank, pool-type fast reactor. The models represent a simple reactor geometry, in which the core and breeder are supported in the middle of a cylindrical pool. This arrangement has been examined to provide both an understanding of the physical processes occurring and quantitative information about the pressure loading produced on the walls and roof of the model reactor tank. Piezo-electric pressure gauges have been used to measure the dynamic pressure loading (from a bare charge of explosive) on the walls, roof, and core-access plug of an over-strong model tank partly filled with water. The influence on these parameters of enclosing the explosive charge in a model core and breeder structure, of varying the air gap between the roof of the model tank and the

water surface, and of replacing the overstrong tank by a weak one, have all been examined.

3. Scale Models and Their Limitations

3.1 Scale Models

A mechanical system and a model of it are said to be dynamically similar when scaled forces are exerted at scaled times on similar portions of the system and the model. If the model has been properly designed and constructed, then dynamic measurements of physical parameters made with the model will have simple scale relationships to those that would be measured on the full-scale system. However, models by their very nature must contain a number of approximations, and due consideration of these should be given when interpreting model results for full-scale conditions.⁽³⁾

Dynamic similarity will be maintained between the model and the full-scale event if the linear dimensions of explosive charge and the mechanical system are divided by a factor S and the model constructed of similar materials. Thus, if M is the mass of the

TABLE 1. PARAMETERS ON FULL SCALE AND ON A MODEL SCALE OF $1/S$

Physical Quantity	Dimensions	Full-scale Value	Model-scale Value
Charge Mass and Other Masses	M	Z	Z/S^3
Shock-wave Pressures	$ML^{-1}T^{-2}$	p	p
Length, e.g., Distance from Charge	L	R	R/S
Time, e.g., Duration of Shock Pressure	T	t	t/S
Area	L^2	A	A/S^2
Impulse per Unit Area	$ML^{-1}T^{-1}$	I	I/S
Force	MLT^{-2}	F	F/S^2
Velocity	LT^{-1}	V	V
Acceleration (Except Gravity)	LT^{-2}	a	aS
Stress	$ML^{-1}T^{-2}$	σ	σ
Strain	—	ξ	ξ
Rate of Strain	T^{-1}	$\dot{\xi}$	$\dot{\xi}S$
Energy	ML^2T^{-2}	E	E/S^3

TABLE 2. COMPARISON OF WATER AND HOT SODIUM COOLANT

	Water at 20°C	Sodium at 400°C	Sodium at 600°C
Velocity of Sound C_0 , m sec ⁻¹	1484	2420	2310
Density ρ , g cm ⁻³	0.998	0.856	0.808
Acoustic Impedance ρC_0 , g cm ⁻² sec ⁻¹	148×10^3	206×10^3	186×10^3

TABLE 3. PUBLISHED PROPERTIES OF STAINLESS STEEL TYPE EN58B AND MILD STEEL EN2

Steel	Temp, °C	0.5% Proof Stress, tons/in. ²	Maximum Stress, tons/in. ²	Elongation, %
Stainless Steel EN58B	20	16.7	42.6	53.4
	100	13.7	34.0	48.8
	200	13.0	30.7	40.7
	300	12.2	30.0	38.4
	400	12.4	30.2	40.7
	500	11.2	27.9	38.3
Mild Steel EN2	20	13.2	20.1	36

TABLE 4. SERIAL NUMBERS OF EXPERIMENTS PERFORMED IN CYLINDRICAL TANKS, 24 IN. IN DIAMETER AND 24 IN. DEEP

Explosive Charge Weight, 2 oz RDX/TNT 60/40

Air Gap between Water and Roof of Tank, in.	Depth of Charge Centre below Surface of Water, in.	Overstrong Tank		Weak Tank	
		Bare Charge	Charge Enclosed in Core Support Structure	Bare Charge	Charge Enclosed in Core Support Structure
None	16.6	19			
1.1	15.5	3*, 4*, 5, 10*, 11, 16	6, 7	12*, 13*	14*, 15*
4.0	12.6	17, 18			
8.3	8.3	8, 9			

* Tank severely damaged.

full-scale charge, R is a full-scale length, and t a full-scale time, then the corresponding model scale values are M/S^3 , R/S , and t/S ; shock-wave pressures on the full and model scales will be the same.

If p is the peak shock overpressure and E the energy released by detonation of the explosive, then

$$p = f \left\{ \frac{R}{E^{1/3}}, \frac{t}{E^{1/3}} \right\}. \quad (1)$$

Similar shock pressures will be observed at scale distances from two charges of different energy release; the shock will arrive at a scaled time and will have a scaled time duration. Thus, for the same type of explosive and constant ambient conditions in the transmission medium, the shock pressure is completely defined by any two of the three parameters E , R , and T , where T is a time characteristic of the explosion such as shock arrival time or duration. Some physical parameters that are commonly measured on full and model scales have been derived and are shown in Table 1.

Certain parameters cannot be correctly scaled in a model which has been derived from a mechanical system by simple linear scaling. The acceleration due to gravity is an important quantity which does not change between the model and full scales. Thus, in a

linearly scaled system, although the masses will be correctly scaled, the model weight will not. In these experiments only the free flight of the model core plug is controlled by gravity. The impulsive explosion forces acting on the plug are large and of short dura-

FIG. 1. Cross Section of Model Reactor Tank, Showing Positions of Pressure Gauges and the Bare Charge.

tion; thus the plug quickly acquires a speed V . Since this quantity will be the same on the full and model scales, and the acceleration due to gravity is the same for both, the full- and model-scale trajectories will be the same.

3.2 Simulation of Sodium Coolant

In fast reactors the usual coolant is liquid sodium. Sometimes sodium-potassium eutectic mixture (NaK), which is liquid at room temperature, is substituted, but both are difficult liquids to handle experimentally. In work using explosive charges, safety problems become important and are difficult to solve when hot liquid sodium or NaK are used as the coolant in the model. Also in the case of hot sodium the safe introduction of a temperature-sensitive explosive charge into the model reactor would require a major modification of the model structure around the charge. The dynamic similarity of the model and the full-scale structure might be seriously impaired by this, and it is usual to simulate the hot sodium coolant by water at room temperature. This is not an exact simulation, however, since the important parameters: density ρ ,

FIG 3 Model Reactor Tank of Minimum Strength.

speed of sound c , and acoustic impedance ρc , do not exactly match for the two liquids (see Table 2). The effect of replacing hot sodium coolant by water on the pressure loading produced in small models of fast reactors has been examined separately.⁽⁴⁾

3.3 Properties of Reactor Materials

It is clear that the energy absorbed from the shock wave by the breeder and core support structure will have an important effect on the strength of the shock reaching the outer parts of the model, such as the primary sodium tank. Thus the parts of the model that will be deformed by the explosion should be made of a steel having a similar (stress-strain) curve as that of the steel of the full-scale structure at its operating temperature. Unfortunately, suitable dynamic (stress-strain) curves for stainless and other steels are not currently available, and the best that can be done is to match the statically measured yield point, ultimate tensile strength, and the permanent elongation at the ultimate tensile stress. The substitution of cold water for the hot sodium coolant in the

FIG 2 Strong Model Reactor Tank.

FIG. 4 Cross Section of Model Core and Breeder Structure.

model means that the model structure will now be at 20°C instead of approximately 400°C. If the model reactor were constructed of stainless steel, then it would be substantially stronger at room temperature and have a greater elongation before maximum stress occurs (see Table 3). The properties of mild steel EN2 at 20°C are very similar to a commonly used stainless steel (EN58B) in the temperature range from 300 to 500°C and the latter was used to make the model core support structures used in the experiments. The overall density of the breeder section was simulated by lead shot. A graphite cylinder was used to represent graphite shielding blocks and sand to represent the filling of the upper graphite reflector.

4. Experimental Method

In all, nineteen experiments have been performed in cylindrical model tanks. The experimental conditions for 17 are given in Table 4. The closed, cylindrical model tanks were 24 in. in internal diameter and 24 in. deep; explosive charges of 2 oz RDX/TNT 60/40 were detonated inside them (see Fig. 1). The overstrong model reactor tank was used to measure the maximum shock reflexion pressure to be expected. The first model tanks with 1-in.-thick walls and 1½-in.-thick base and roof were not strong enough to withstand more than one explosion and were replaced by the reinforced type shown in Fig. 2. Here the base and roof were 3 in. thick, and the 1-in.-thick walls were reinforced by 1.5-in.-thick bands, 3 in. wide. This reinforcement enabled the tank to be used for three experiments before serious damage occurred. A number of weak vessels (see Fig. 3) made of thin (0.022 in. thick) sheet steel were also used to give the minimum containment of the explosion, to examine the effect of this on the core-plug pressure loadings.

A model of a hypothetical core support structure (see Figures 4 and 5) was made of suitable mild steel sheet. This was used with the explosive charge in the position of the core. The air gap between the surface of the water and the roof of the vessel was varied, but the bare charge was kept at the same height above the floor of the tank. All the possible combinations of the above parameters were not examined; the actual experiments performed are summarised in Table 4.

The experiments with the 24-in.-diameter cylindrical tank were supplemented with experiments at half this scale by use of a square section tank (see Fig. 19 below) with Perspex windows to enable the growth of the explosion products bubble to be examined by high-speed photography.

Most of the pressure measurements were made with use of piezo-electric transducers type MQ20 designed and made at Foulness. The gauge has a natural

FIG. 5. Model Reactor Core with Part of Graphite Shield.

resonance at about 300 kc/sec and is intended to have a good high-frequency response; it is 0.44 in. in diameter, 0.88 in. long, and contains a quartz crystal 0.25 in. in diameter by 0.125 in. thick. The transducer is well damped and is not prone to spurious oscillation. The output of this gauge, which is about 0.5 picocoulomb/psi, was fed to a recording system consisting of amplifiers and display oscilloscopes; the time sweep was provided by a rotating drum camera, and the system had an overall frequency response of about DC-20 kc/s. The impulse per unit area of the measured (pressure, time) curve is defined as

$$I = \int_{t_1}^{t_2} p(t) dt, \quad (2)$$

where t_1 and t_2 are the times between which the impulse is measured.

In all the experiments the movement of the plug was observed by means of a high-speed cine camera operating at about 700 frames/sec. In the half-scale experiments, the expansion of the explosion products bubble was photographed at 12,000 half-frames/sec by means of a Fastax camera.

5. Results

5.1 Initial Experiments

In the first experiment a model tank without a roof was used so that the ejection of the water spray from the tank could be observed by cine cameras. No pressure records were taken. The initial spray thrown upwards by reflexion of the shock wave at the water surface had a speed of 175 ft/sec; this was followed 70 msec later by a more solid body of water moving at 20 ft/sec. No pressure records were obtained from

the second experiment in a model tank with roof, plug, and a bare charge (see Fig. 1). Useful results were, however, obtained from 17 subsequent experiments in the 24-in.-diameter cylindrical tanks and the three half-scale experiments to observe the bubble growth of the explosion products.

During the early experiments (3 and 4) it was clear that very high stresses were being produced in the

walls of the model reactor tank, since the tanks were extensively damaged after only one or two explosions. In order to see whether large spurious signals were propagated from the tank wall and gauge mount to the body of the gauge, an MQ20 pressure gauge was put in a blind mount for two experiments. The gauge was supported in the vessel wall in the same way as the other gauges, but the sensitive surface was pro-

TABLE 5. FIRST PEAK PRESSURES MEASURED IN OVERSTRONG TANK WITH AN AIR GAP OF 1.1 IN. BETWEEN WATER SURFACE AND TANK ROOF

Location of Pressure Gauge in Model	Position Number of Pressure Gauge	First Peak Pressure (psi) (Peaks A or E of Figures 6, 7, and 8)									
		Bare Charge							Charge Enclosed in Core Support Structure		
		Rd 3	Rd 4	Rd 5	Rd 10	Rd 11	Rd 16	Mean, psi	Rd 6	Rd 7	Mean, psi
Tank Side Wall	1	12,500	13,000	15,100	12,400	15,500	15,300	13,900	3,480	3,440	3,460
	2	11,800	15,200	9,630	19,100	22,400	21,200	16,600		4,220	4,220
	3	12,600	13,600	12,000	17,400	22,700	23,100	16,900	2,890	3,480	3,180
	4	Blanked	16,700	9,530	12,400	21,800	20,400	16,200	2,260	3,140	2,700
	5	12,100	Blanked		>6,410	>9,950	12,900	12,500		2,200	2,200
	6	9,850	9,080	7,460	6,780	10,500	7,380	8,510		1,040	1,040
Tank Roof	7		4,360	>4,060	6,060	7,710	11,700	7,460	3,390	6,690	5,040
	8				>6,440	15,900	>14,900	15,900	5,890	5,870	5,880
	9			>4,530	>4,980	>7,490	13,100	13,100			
Core-access Plug	10					>11,100	14,200	14,200		15,000	15,000
	11				>5,110	>12,800	14,900	14,900	>5,650	15,100	15,100
	12			>6,420	>5,410	>12,800	12,500	12,600	9,410		9,410

TABLE 6. IMPULSE MEASURED BY PRESSURE GAUGES IN OVERSTRONG TANK WITH AN AIR GAP OF 1.1 IN. BETWEEN WATER SURFACE AND TANK ROOF

Location of Pressure Gauge in Model	Position Number of Pressure Gauge	Impulse from Pressure-gauge Records, psi-msec									
		Bare Charge							Charge Enclosed in Core Support Structure		
		Rd 3	Rd 4	Rd 5	Rd 10	Rd 11	Rd 16	Mean, psi-msec	Rd 6	Rd 7	Mean, psi-msec
Tank Side Wall	1	1,420	1,490	2,580		1,200	2,030	1,740	1,190	880	1,040
	2	1,350	1,320	1,690	908	1,820	2,180	1,540			
	3	1,180	1,540	1,630	1,090	1,700	1,840	1,500	1,100	1,060	1,080
	4		1,390	1,240	701	983	1,540	1,170	723	742	732
	5	917			639	908	807	818		490	490
	6	194	197		319	348	182	248			
Tank Roof	7		2,430	>1,490	Over 1,000	1,450	2,090	1,990	>1,800	2,740	2,740
	8				>850		2,340	2,340		>1,610	>1,610
	9			1,010	>700	>1,160	2,290	1,650			
Core-access Plug	10					>1,300*	1,540	1,540		2,220	2,220
	11						1,900	1,900	>1,300		>1,300
	12					>1,100*	1,830	1,830	>1,500		>1,500

* Estimated.

tected from the pressure pulse. Only very small oscillations of negligible amplitude were recorded from this gauge; the pressure records obtained are therefore thought to be free from spurious interference of this kind.

5.2 Results from Experiments in an Overstrong Tank with an Air Gap of 1.1 In. between Water Surface and Tank Roof

The measurements of peak pressure and impulse obtained are given in Tables 5 and 6. (Summaries of the mean peak pressures and impulses of core plug and tank roof, and the core-plug ejection velocities are given in Table 10 below.) The results of Round 3, 4, and 10 are given although the tanks were severely damaged, allowing venting of the internal pressures early in the experiment. The impulses measured by the pressure gauges in these rounds are low compared with those from Rounds 5, 11, and 16. The plug-ejection velocities measured in Rounds 3, 4, and 10 are also low.

The shock-reflexion and liquid-impact effects are clearly illustrated by the pressure records; a complete set of records was obtained from Round 16, for which a bare charge was used. Photographs of these are shown in Figures 6, 7, and 8; the direct shock wave

from the explosion (A), the reflected shock from the base of the model (B), and the rarefaction from the free surface (C) can be seen. These shock waves were propagated at a velocity of about 6,100 ft/sec. The first direct shock (A) striking the tank produced a shock which was transmitted through the steel to the gauges in the roof in gauge positions 7, 8, and 9, producing the small oscillations due to body signal which start at (D). These oscillations were not recorded in positions 10, 11, and 12 in the plug, due to the attenuation of the shock in the wall of the tank by the small clearance gap around the plug. The main pressure loading on the roof starts at (E), and this was probably due to a layer of water or spray thrown up by the reflection of (A) at the water surface. This pressure pulse occurred 0.62 msec after the arrival of the initial shock (A) at the water surface, and the water or spray would have to travel 1.1 in. in this time, giving a velocity of 149 ft/sec; this agrees approximately with the initial spray velocity of 174 ft/sec measured photographically in Round 1. There is a second pulse (G) recorded by the gauges in the plug, and this may be due to the impact of a second layer of water thrown up by the reflection of (B) at the water surface. The shock-pressure loading on the roof which starts at (E) appears to be reflected back

FIG. 6. Records from Round 16 at Positions 9-12.

FIG 7 Records from Round 16 at Positions 5-8.

through the contents of the vessel, producing a second pulse (F) on the gauges in the side of the model. This pulse has an apparent velocity of about 1,600 ft/sec between gauge positions 6 and 5, falling to 1200 ft/sec between positions 5 and 4; at position 3 the amplitude of the pulse is very small.

The effect of enclosing the explosive charge in a model core support structure (see Figures 4 and 5) on the shock waves transmitted to the walls and roof of the tank is shown by Round 7 (see Figures 9, 10, and 11).

The initial direct shocks measured at positions 1 to 6 lost their classical shape after passing through the simulated breeder and reflector as compared to the results from Round 16. The peak pressures and the impulses were lower, except for the plug gauge positions. There was also a second pressure pulse some time after the first, probably caused by the bursting core support structure after it had been distorted and expanded by the explosion products.

Some of the charge detonation energy has been used up in disrupting the core structure; the propor-

tion of charge energy imparted to the plug has fallen from 1.8% for a bare charge to 1.4% for the enclosed charge (see Tables 10 and 11 below).

5.3 Results from Experiments in an Overstrong Tank with Air Gaps from Zero to 8.3 In.

The measurements of peak impact pressures and impulse are given in Tables 7 and 8 and summarised together with plug ejection velocities in Table 10 below. As would be expected, the highest peak pressures and impulses and the highest core-plug ejection velocity were measured in Round 19, in which no air gap was used.

The effect of increasing the air gap is shown in the pressure records from Round 17 (see Figures 12, 13, and 14). In the gauge positions below the water surface (1 to 4) the records are similar to those from Round 16. At position 5 the rapid cutoff due to the rarefaction wave from the free water surface is seen. In the case of the 8.3-in. air gap, there is a long delay in the arrival of the spray at the roof, though the pressure records are of similar shape.

Fig. 8. Records from Round 16 at Positions 1-4.

Fig. 9. Records from Round 7 at Positions 10 and 11.

FIG. 10. Records from Round 7 at Positions 5-8.

FIG. 11. Records from Round 7 at Positions 1-4.

TABLE 7. FIRST PEAK PRESSURES MEASURED IN OVERSTRONG TANK USING A BARE EXPLOSIVE CHARGE FOR DIFFERENT AIR GAPS BETWEEN WATER SURFACE AND TANK ROOF

Location of Pressure Gauge in Model	Position Number of Pressure Gauge	First Peak Pressure (psi) (Peaks A or E of Figure 6, 7, and 8)													
		No Gap	1.1-in. Air Gap							4-in. Air Gap			8.3-in. Air Gap		
			Rd 19	Rd 3	Rd 4	Rd 5	Rd 10	Rd 11	Rd 16	Mean, psi	Rd 17	Rd 18	Mean, psi	Rd 8	Rd 9
Tank Side Wall	1	20,300	12,500	13,000	15,100	12,400	15,500	15,300	13,900	13,100	14,600	13,850	19,800	20,700	20,200
	2	9,740	11,800	15,200	9,630	19,100	22,400	21,200	16,600	17,100	24,700	20,900	12,300	7,110	9,700
	3	19,800	12,600	13,600	12,000	17,400	22,700	23,100	16,900	25,900	24,100	25,000	13,700	13,200	13,400
	4	23,700	Blanked	16,700	9,530	12,400	21,800	20,400	16,200	21,800	21,800	21,800	8,500	13,800	11,200
	5	10,100	12,100	Blanked		>6,410	>9,950	12,900	12,500	11,600	10,400	11,000		4,520	4,520
	6	13,100	9,850	9,080	7,460	6,780	10,500	7,380	8,510	5,280	6,160	5,720	1,310	3,010	2,160
Tank Roof	7	10,000		4,360	>4,060	6,060	7,710	11,700	7,460	6,390	8,750	7,570	7,260	4,590	5,920
	8	11,000				>6,440	15,900	>14,900	15,900	8,000	13,900	10,900		2,120	2,120
	9	10,600			>4,530	>4,980	>7,490	13,100	13,100	10,300	8,970	9,635	1,580	2,080	1,830
Core-access Plug	10	20,200					>11,100	14,200	14,200	6,770	7,410	7,090	3,210	4,630	3,920
	11	17,500				>5,110	>12,800	14,900	14,900	4,000	7,020	5,510	4,060	3,600	3,830
	12	15,800			>6,420	>5,410	>12,800	12,500	12,600	7,770	>8,850	>8,310	4,440	4,420	4,430

TABLE 8. IMPULSE MEASURED BY PRESSURE GAUGES IN OVERSTRONG TANK USING BARE EXPLOSIVE CHARGES FOR DIFFERENT AIR GAPS BETWEEN WATER SURFACE AND TANK ROOF

Location Pressure Gauge in Model	Position Number of Pressure Gauge	Impulse from pressure-gauge records, psi-msec.													
		No Gap	1.1-in. Air Gap							4-in Air Gap			8.3-in. Air Gap		
			Exp 19	Exp 3	Exp 4	Exp 5	Exp 10	Exp 11	Exp 16	Mean, psi-msec	Exp 17	Exp 18	Mean, psi-msec	Exp 8	Exp 9
Tank Side Wall	1	1,810	1,420	1,490	2,580		1,200	2,030	1,740	1,880	1,830	1,855	1,240	1,840	1,540
	2	1,340	1,350	1,320	1,690	908	1,820	2,180	1,540	1,740	2,080	1,910	961	1,140	1,050
	3	1,330	1,180	1,540	1,630	1,090	1,700	1,840	1,500	2,030	1,740	1,885	539	1,050	794
	4	1,290		1,390	1,240	710	980	1,540	1,170	1,090	1,200	1,145	159	385	272
	5	2,150	917			639	908	807	818	264	272	268		176	176
	6	3,620	194	197		319	348	182	248	773	597	685	877	1,900	1,390
Tank Roof	7	2,000		2,430	>1,490	>1,000	1,450	2,090	1,990	1,740	2,150	1,950	1,820*	1,320*	1,570*
	8	2,300				>850		2,340	2,340	1,900	2,200	2,050		2,140*	2,140*
	9	1,800			1,010	>700	>1,160	2,290	1,650	1,700	1,900	1,800	1,220*	1,810*	1,520*
Core-access Plug	10	>630					>1,300*	1,540	1,540	1,650	>1,130	1,650	1,900*	1,000*	1,450*
	11	>1,250						1,900	1,900	2,250	1,570	1,910	600*	1,750*	1,170*
	12	>1,990					>1,100*	1,830	1,830	2,280	1,600	1,940	1,600*	1,680	1,640

* Estimated.

FIG 12 Records from Round 17 at Positions 9-12

FIG 13 Records from Round 17 at Positions 5-8.

FIG. 14. Records from Round 17 at Positions 1-4.

The mean peak impact pressures on the core plug and tank roof are plotted in Fig. 15 versus air gap and the mean impulse per unit area versus air gap in Fig. 16. The peak pressures decrease rapidly with increasing air gap, though the impulse decreases only slowly. The measured plug ejection velocities also decrease with increasing air gap.

5.4 Results of Experiments with a Weak Tank and 1.1-in. Air Gap

The peak pressures and impulses measured on the core plug are given in Table 9. The plug ejection velocities given in Table 10 are much lower than the values obtained using the strong tank. The plug pressure records obtained are shown in Fig. 17 for the bare charge and Fig. 18 for the enclosed charge. The shocks incident on the side walls and base of the weak tank would be reflected as rarefactions, and since

these are rapidly attenuated by cavitation the main pulse is shorter (see Fig. 17A for effect of explosion on weak-model reactor tank). The second pulse (G) seen in Round 16 does not appear in the bare-charge experiment. In the enclosed-charge case the pressure records are similar to those obtained in Round 7, though again of smaller amplitude.

Some of the explosion energy released was absorbed in distorting and bursting the core support structure. However, the velocity of plug ejection was increased, and it appears that of the remaining energy more was channelled upwards than before.

5.5 Results of Half-scale Experiments to Examine Bubble Growth

Cross sections of the observation tank are shown in Fig. 19 and a photograph in Fig. 20. Two preliminary experiments (Rounds 20 and 21) to establish

FIG. 15. Mean Peak Impact Pressure on Core Plug and Tank Roof versus Air Gap between Water Surface and Tank Roof.

suitable experimental methods were carried out. One experiment (Round 22) was done with a half-scale charge (6 g RDX/TNT plus a No. 8 detonator), after which the observation tank was severely damaged.

Prints from the Fastax cine film taken of Round 22 are given in Fig. 21, and the growth of the bubble plotted in Fig. 22 together with similar curves for two smaller charge weights. The impact pressure measured at the roof (positions A and C) is shown in Fig. 23. Frame 3 of Fig. 21 shows the cavitation of the water produced just below the surface by the reflected rarefaction; by Frame 5 this was propagated

to the base of the tank, and the cavitation bubbles became visible all the way down. In Frame 5 small jets of water can be seen striking the underside of the roof; this corresponds to the first large peak on the pressure record at 0.45 msec after firing. The spray has reached the roof between Frames 6 and 7, and this corresponds to the second peak in the pressure record at 0.6 msec. In Frame 7 cracks in the Perspex window can be seen; the cavitation has started to clear upwards from the base of the tank and has completely gone by Frame 10. This corresponds to the arrival of the third and highest peak in the pressure

FIG. 16. Mean Impulse per Unit Area on Core Plug and Tank Roof versus Air Gap between Water Surface and Tank Roof.

record at 0.8 msec. This suggests that the cavitation was cleared by a compression wave travelling up from the base of the tank.

The expansion of the explosion bubble (see Fig. 22) halts at about 1.1 msec and does not restart until 1.7 msec. A small pressure rise occurs at 1.1 msec at gauge position A (see Fig. 23) and at 1.5 msec at position C; the extensive cracking of the model windows is completed about this time, and large losses of water from the model begin. It seems probable that the pressure in the air gap becomes equal to the bubble pressure at about this time.

6. Discussion of Results

The experimental results of the pressure loading on the tank roof and core plug are summarised in Table 10. This pressure loading was caused by the impact of the water thrown upwards when the direct shock was reflected at the free surface. The impact-pressure loading on the roof and plug was large, and a pulse appears to be reflected and propagated back down into the contents of the vessel.

TABLE 9. FIRST PEAK PRESSURES AND IMPULSES MEASURED ON CORE PLUG USING A WEAK REACTOR TANK WITH AN AIR GAP OF 1.1 IN. BETWEEN WATER SURFACE AND TANK ROOF

Position Number of Pressure Gauge	First Peak Pressure (psi) (Peaks A or E of Figs. 6, 7, and 8)					
	Bare Charge			Charge Enclosed in Core Support Structure		
	Rd 12	Rd 13	Mean, psi	Rd 14	Rd 15	Mean, psi
10	3,730	2,840	3,280	1,940	1,350	1,640
11	2,530	2,550	2,540	2,170	816	1,490
12	2,790	3,590	3,190	2,320	962	1,640

Position Number of Pressure Gauge	Impulse from Pressure Records					
	Bare Charge			Charge Enclosed in Core Support Structure		
	Rd 12	Rd 13	Mean, psi-msec	Rd 14	Rd 15	Mean, psi-msec
10	360	180	270	280	180	230
11	300	240	270	330	190	260
12	300	220	260	220	200	210

TABLE 10. SUMMARY OF EXPERIMENTAL RESULTS

Experiment Number	Type of Tank	Type of Explosive Charge	Air Gap, in.	Mean Peak Pressure p , psi		Mean Impulse I , psi-msec		Mean Time of Arrival of Shock on Plug after Detonation, msec	Transit Time of Spray in Air Gap, msec	Spray Velocity, u , ft/sec	p/u , lb ft ⁻² sec ⁻¹ $\times 10^5$	Velocity of Plug Calculated from Mean Impulse, $V_c = (IA/M)$ ft/sec	Measured Velocity of Plug V_m , ft/sec	V_m/V_c	% of Charge Energy Imparted to Plug
				Core Plug	Tank Roof	Core Plug	Tank Roof								
19	Strong	Bare	0	17,800	10,500	1990*	2030	0.41	—	—	2.97	>60	74	1.23	2.09
3	Strong	Bare	1.1	—	—	—	—	—	—	—	—	—	51	—	0.99
4	Strong	Bare	1.1	—	4,400	—	—	0.70	0.48	191	—	—	62	—	1.47
5	Strong	Bare	1.1	6,400	>4,500	—	>1490	0.81	0.59	155	1.91	—	71	—	1.92
10	Strong	Bare	1.1	>5,400	>6,300	—	—	0.98	0.75	122	>2.05	—	40	—	0.61
11	Strong	Bare	1.1	>12,800	11,800	>1300*	1450*	0.80	0.60	153	>3.90	>39	about 66	1.69	1.66
16	Strong	Bare	1.1	13,500	>13,200	1760	2240	0.85	0.62	149	4.20	53	70	1.32	1.87
17	Strong	Bare	4.0	6,200	8,200	2060*	1780	2.17	2.04	163	1.76	62	68	1.10	1.76
18	Strong	Bare	4.0	7,800	11,900	1590	2080	2.13	1.97	169	2.14	48	65	1.35	1.61
8	Strong	Bare	8.3	3,900	4,400	1370*	>1820*	2.79	2.69	257	0.704	>41	60	1.46	1.38
9	Strong	Bare	8.3	4,200	2,900	1720*	>1760*	3.32	3.22	215	0.904	>52	63	1.21	1.52
6	Strong	Enclosed	1.1	9,410	4,640	>1400	>1800	1.28	1.06	87	5.01	>42	60	1.43	1.38
7	Strong	Enclosed	1.1	14,100	6,280	2200	2740	1.45	1.22	75	8.73	67	62	0.93	1.47
12	Weak	Bare	1.1	3,020	—	320	—	0.82	0.60	153	0.915	9.7	21	2.17	0.17
13	Weak	Bare	1.1	2,990	—	213	—	0.82	0.60	153	0.906	6.5	24	3.70	0.22
14	Weak	Enclosed	1.1	2,140	—	277	—	2.29	2.07	44	2.26	8.4	41	4.9	0.64
15	Weak	Enclosed	1.1	1,040	—	190	—	3.08	2.86	32	1.51	5.8	36	6.2	0.50

* Estimated. C = Speed of sound in water — 4760 ft/sec.Total Energy of charge taken as 203×10^3 ft-lb.

FIG. 17. Records from Round 13

The spray impact pressure p is given by the equation

$$p = \rho c u, \quad (3)$$

where ρ is the density of the spray, c is the velocity sound in the spray, and u is the spray velocity. The product ρc is known as the acoustic impedance of the medium. For water this is $2.97 \times 10^5 \text{ lb ft}^{-2} \text{ sec}^{-1}$. The spray velocities have been calculated from the observed transit times between the water surface and the plug, and values of the ratio p/u are given in Table 10. For Rounds 11 and 16 this ratio p/u exceeded the acoustic impedance of water, so it would appear that either p is high or u is low, or both.

Disregarding these two results, the mean value is $1.98 \times 10^5 \text{ lb ft}^{-2}$ for a 1.1-in. air gap and bare charge; for a 4.0-in. air gap the value is 1.95×10^5 , falling to $0.804 \times 10^5 \text{ lb ft}^{-2} \text{ sec}^{-1}$ for an 8.3-in. air gap. In the last case the impact pressure is reduced though the spray velocity is higher; this implies that the density has been reduced, possibly by the venting of the explosion bubble.

The ratio p/u calculated for the spray in Rounds 6 and 7 is very high; this may have been caused by the debris of the core support structure striking the plug. Low values of p/u were obtained for the weak-vessel experiments of Rounds 12 and 13; this was again increased when the explosion was enclosed in the core support structure.

The velocity of plug ejection, V_e , has been calcu-

lated from the equation

$$V_e = IA/M, \quad (4)$$

where A is the area of the plug, I the mean impulse per unit area acting on the core plug, and M is the plug weight. The area of the stepped plug (see Fig. 1) corresponding to its smallest diameter of 7.75 in. was used to calculate the values of V_e given in Table 10. With the exception of Round 7, these values are all smaller than the velocities measured from the cine film, and the ratios V_m/V_e are given in Table 10. The mean value of the ratio for experiments with a bare charge in a strong vessel is 1.34, and assuming the values of I are correct this implies that the impulse was acting over a larger area than the value of 47 in.² used in the calculations. The increased area corresponds to a circle of 9-in. diameter, which equals the intermediate plug diameter (see Fig. 1). The low value for Round 7 may have been caused by the plug jamming in the plug guide and increasing the effective value of M , the plug weight. The large values for Rounds 12, 13, 14, and 15 were probably caused by ejection of the plug, guide, and vessel roof as one unit, thereby increasing the ratio A/M .

The conditions in the overstrong tank seem to have a limited effect on the percentage of the explosion energy imparted to the plug (see Tables 10 and 11). The variation was from 1.4% in the case of the enclosed charge to 2.1% in the case of a large charge with no air gap. Increase of the air gap decreased the

TIME BETWEEN FRAMES IS 1.32 ms

FIG. 17A. Round 13. (Bare Charge) Effect of Explosion in Weak Model Reactor Tank.

FIG. 18. Records from Round 14.

imparted energy but only to 1.5% (see Fig. 24). Premature failure of the overstrong tank allowed early venting of the explosion pressures and decreased plug energy to 1.0%. This was confirmed by experiments in weak tanks. However, the effect of a core support structure in a weak tank was to direct the energy released upwards to the tank roof and core plug, resulting in higher plug-ejection velocities. Similar effects on the plug total impulse were observed.

The methods and results reported here are included in a general discussion of fast reactor containment design by Samuels.⁽²⁾

7. Acknowledgments

The author wishes to thank T. P. O'Brien, D. O. Wells, and Mrs. H. A. Taylor for carrying out the experiments reported here.

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FIG. 19. Cross Sections of Half-scale Tank for Bubble Photography Showing Pressure-gauge Positions.

FIG 20 Before Firing Round 22 Half scale Model Used to Photograph Expansion of Bubble of Explosive Products.

TIME BETWEEN FRAMES IS 0.083 ms

FIG. 21 Round 22 Expansion of Gas Bubble

FIG. 22 Rounds 20, 21, and 22 Radius time Measurements of Gas Bubbles in Half scale Reactor Models

FIG. 23 Records from Round 22.

TABLE 11. SUMMARY OF MEAN PERCENTAGE OF CHARGE ENERGY AND MEAN TOTAL IMPULSE GIVEN TO PLUG UNDER DIFFERENT EXPERIMENTAL CONDITIONS

Total impulse of core plug = MV_m lb-sec.

Total Energy of charge 203×10^3 ft-lb.

Air Gap between Water Surface and Roof of Tank, in.	Mean % of Charge Energy Imparted to Core-access Plug			
	Overstrong Tank		Weak Tank	
	Bare Charge	Charge Enclosed in Core Support Structure	Bare Charge	Charge Enclosed in Core Support Structure
None	2.09	1.43	0.20	0.57
1.1	1.84*			
4.0	1.70			
8.3	1.45			
Air Gap between Water Surface and Roof of Tank, in.	Total Impulse of Core-access Plug from Measured Plug Velocity, lb-sec			
	Overstrong Tank		Weak Tank	
	Bare Charge	Charge Enclosed in Core Support Structure	Bare Charge	Charge Enclosed in Core Support Structure
None	115	95.2	35.1	60.1
1.1	109*			
4.0	104			
8.3	96.0			

* Results of Experiments 3, 4, and 10 omitted, since overstrong tank severely damaged, allowing venting before plug-ejection process complete.

FIG. 24. Percentage of Charge Energy Imparted to Core Plug; Also Total Impulse Given to Core Plug versus Air Gap between Roof of Tank and Water Surface.

Discussion

Mr. McCarthy (PRDC): Considerable work on this same problem has been done in the United States, but which has not been reported widely in the reactor community. I refer to work that was done by the U. S. Naval Ordnance Laboratory on the response of the Enrico Fermi reactor to 1000-lb-TNT releases. A report, NOLTR-62-207, has been published. I was struck by the coincidence of results between Mr. Rees' paper and that in the NOLTR-62-207. In particular, the fraction of the energy released by the explosive, which appeared as kinetic energy of the plug, was identical in both of those studies which were done completely independently—in both cases being about 2%, which is a remarkable agreement. The NOL work was done, as a matter of fact, with both hot sodium and with hot water, and some of their results indicated that the jump when sodium is used is somewhat higher than with water.

Mr. Wolfe (GE): Do I understand that the major energy imparted in plug jump is not due to the shock wave but due to the pressure of the gas which remains after the initial shock wave hits the plug?

Mr. Rees: The actual pressures measured on the plug are due to waves that are rather longer than the classical shock wave from an explosion of this size. If you take the impulse measured in the pressure loading on the plug and work out the plug velocity, then this does agree reasonably well with the velocities measured in the ciné records.

Mr. Wolfe: If I understand it correctly, the pressures that are important are the so-called quasi-static pressures that remain after the initial shock, the pressure of the expanding gas. If that viewpoint is right, how relevant are the TNT cases to the actual case in which the gases may condense?

Mr. Rees: The results that we give are probably correct up to the end of the first expansion phase of the bubble, which means that they are probably reasonably correct up to about perhaps 2 msec on this type of scale. After that there are probably very considerable deviations because of the different nature of expanding gases. This will be discussed rather more fully in Samuels' paper.

Mr. Denielou (CEA, France): I did not understand clearly whether or not the type of explosive charge was varied, especially with respect to the yield of gaseous products from the explosion.

Mr. Rees: The explosive charge was the same throughout, 2 oz RDX TNT.

Mr. Schikarski (Karlsruhe): Can you comment on the problem of shock waves travelling along pipe lines attached to the reactor tank? Did you study those effects?

Mr. Rees: A shock wave in a pipe usually breaks the pipe at the first bend. It depends very much on what strength of shock you get in the pipe in a particular arrangement, but in general this is the normal effect.

Mr. Fistedis (ANL): Is it actually the sodium momentum which follows the shock wave, time-wise, which causes the plug to accelerate?

Mr. Rees: It's the pressure loading caused by the spray acting on the surface, which in this case can take some time. After all, there is the original shock, the expansion, and the explosion from its bubble; in some cases this is modified by the breakup of the core structure, so that there is an initial shock followed by a pressure loading which will vary due to the circumstances of origin.

Mr. Fistedis: What do you think, as far as the overall containment is concerned, if we somehow depart from the accepted, or shall we say, prevailing, pressure-volume computation? Can we start working directly with energy to be contained against strained energy available in the material which is to do the containing, provided now that the time intervals have been investigated sufficiently to satisfy that plasticity can occur? In other words, the strain rates are such that the material can flow. Don't you think that this will be a neater approach that will give us much more reliable factors of safety?

Mr. Rees: It depends on where the strain energy will be absorbed. If the absorption is very near the charge, then I think you have a very difficult design problem. If the energy is absorbed further from the charge, that is, if you only aim at producing a leak-tight containment structure at some distance from the charge, then I think you probably would be able to do this.

Mr. Fistedis: Containment generally comes in two steps: a primary and a secondary containment. The secondary containment is intended to prevent the escape of gases to the surrounding atmosphere. Now, why not make primary containment capable of absorbing the entire explosive energy through plastic deformation in material sufficiently far from the primary vessel or adjacent in such a way that the strain rates can exhibit this plasticity?

Mr. Rees: I think this is quite reasonable.

Mr. Fistedis: Do you think this is possible?

Mr. Rees: Yes.

Mr. Klickman (APDA): Was the selection of explosive made because of convenience or availability, or because it was satisfactory for a simulant for uranium or UO_2 ?

Mr. Rees: We chose it as an arbitrary representa-

tion of energy release. This particular mixture was used because it was readily available and known to be reliable; TNT as such is a very tricky explosive to detonate completely and properly, whereas RDX TNT is completely detonable.

Mr. Schlechtendahl (Karlsruhe): Is there any indication from your experiments that the impulse that is generated by the explosion does not propagate in a symmetrical way around the charge? It would be very difficult to provide blast shielding and to analyze blast shielding in the design if this were not the case.

Mr. Rees: Are you referring to the bare charge experiments?

Mr. Schlechtendahl: Yes. In one of the pictures taken with a high-speed camera it appeared that the

vessel ruptured on one side. I wonder whether this effect could propagate further out.

Mr. Rees: The reason why the weak tank ruptured on one side was because it was constructed of thin sheet metal welded on one side and the weld-affected zone had not been treated. Of course, this failed rather sooner than the unaffected zone, so you have a tearing occurring down the welded seam. It wasn't because the explosion was unsymmetrical.

Mr. McCarthy: I might add a point, too, that in a shell which doesn't fail, the damage is almost spherical. If it's a marginal failure, it fails in one place obviously, and that makes it look asymmetric. I might again refer to the NOL report for more data on that particular point.

Comparison of Pressure Loading Produced by Contained Explosions in Water and Sodium

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sure transients and further measurements of dynamic strain. The experiments were performed at the C.E.N. de Caradache in three separate series. This was done so that the results from one series could be processed and their trend analysed, enabling any necessary modifications to be made to the programme and to the experimental rig prior to the commencement of the succeeding series.

2. Experimental Details

The intention in designing the apparatus was to maintain, as far as possible, a typical fast reactor geometry within the core vessel and also to permit an explosive charge to be placed in the centre of the core after the model had been assembled. The essential features of the apparatus are shown in Fig. 1. The simulated core vessel, of 250-mm diameter and 424-mm height, was made of stainless steel (type 316), and the base of the vessel contained a re-entrant cylinder into which a charge could be inserted remotely. The radial and upper axial blankets were simulated by closely packed uranium and steel rods to give an average density of 12.7 g/cc. Surrounding the breeder were a number of steel rods and six concentric stainless steel cylinders of 2.5-mm wall thickness which represented the neutron reflectors; surrounding these were four longer concentric cylinders (1 mm thick) simulating the heat shields. There were 1-mm spaces between each adjacent cylinder. The core vessel was mounted inside a strong containment vessel. Bolted to the thick flange at the top of the outer vessel was a heavy lid in which were mounted four type MQ.20 piezo-electric pressure transducers. These are single quartz crystal transducers developed by the UKAEA to measure high-pressure transients with a response up to 250 kc/s. It was necessary to mount the transducers in cooled holders since they were not designed to operate above about 100°C. Cooling was effected by pumping liquid paraffin through the holders. It was possible to keep the temperature of the body of the transducers below 90°C by this method.

The thick lid was held down to the flanges of the strong containment vessel by five stainless steel bolts which had pairs of Microdot weldable strain gauges (type SG120) mounted on them. One strain gauge was mounted on the side of the bolt facing the centre of the lid and the other was diametrically opposite, on the outside of the bolt. An additional strain gauge was mounted on the upper face of the thick lid. The nuts at each end of the hold-down bolts had a hemispherical seating which mated onto the hemispherically shaped spacing washers. The seatings were lubricated with molybdenum disulphide. The purpose of this method of mounting the nuts was to reduce

the amount of bending in the hold-down bolts resulting from flexing of the lid. In order to obtain a uniform loading of the hold-down bolts, they were all pretensioned by the same amount by use of a torque spanner.

For each experiment a core vessel was filled either with water or with the appropriate volume of sodium so that when heated to the required temperature the liquid surface was 5 cm below the lid of the containment vessel. The vessels were filled with sodium at 150°C, the volume of sodium required in each vessel being calculated from the results of a subsidiary experiment in which the differential expansion of core vessels and liquid sodium was measured. To prevent oxidation of the surface of the sodium during the experiment, the free volume above the surface was swept continuously with argon at atmospheric pressure. To allow for differential thermal expansion there was a 3-mm gap between the top of the core vessel and the underside of the thick lid when the apparatus was at ambient temperature.

Three different charge weights were used in these experiments: 15-, 30-, and 60-g of Hexogene, corresponding to energy releases of $2 \cdot 10^4$, $4 \cdot 10^4$, and $8 \cdot 10^4$ cal.

The procedure in each experiment was to heat the liquid to the required temperature by means of the heating coil wrapped around the core vessel. Temperatures were recorded continuously by means of sheathed thermocouples immersed in the liquid. When steady-state conditions had been obtained, the explosive charge (which was enclosed within a weak asbestos container) was mounted on a plinth which stood on top of an hydraulic ram. The ram was operated remotely to raise the charge into the cavity in the base of the core vessel, and the plinth was then rotated, locking it into the base of the outer containment vessel. The ram was then lowered, leaving the charge and plinth in position in the model. This last operation triggered a relay which initiated the firing sequence: starting the recording apparatus, inserting calibration pulses, detonating the charge, and switching off the apparatus at the end of the recording period. An ionisation probe was placed on the detonator fuse to give a time-zero pulse on all the recording traces obtained with the UK equipment. A correlating pulse was inserted on the UK and the French recording traces so that zero time could be determined on all records.

The UK recording apparatus consisted of three four-channel oscillograph recorders equipped with high-speed rotating drum cameras, enabling a writing speed of up to 2 in./ms for about 15 ms after time zero. The output from each of the pressure trans-

NOT TO SCALE

Fig. 1. Sketch Showing Main Components of the Model Reactor Assembly.

ducers was fed through two parallel amplifying channels, one set on a low gain to observe the highest impact pressures and the second on a higher gain to observe the lower pressure levels. The re-

maining four channels were used to record strains from two of the hold-down bolts. The remaining seven strain gauges were connected to Wheatstone bridge networks and mirror-galvanometer recorders

which were operated by the CEA team. This enabled a comparison to be made of the two strain-gauge systems. The nominal useful frequency range of the UK recording system was from DC to 15 kc/s, but it is intended to carry out a frequency test of the equipment at a later date to check this data. The CEA equipment had a useful frequency range from 10 c/s to 8 kc/s, and a writing speed of 0.4 in./ms.

The programme as planned was to measure the impact loading on the lid of the containment vessel for three different charge weights in water and in hot sodium, covering temperatures in the range from 150 to 550°C. Strain measurements were made over the whole range, but the highest temperature at which impact pressures were recorded was 450°C. Of the twenty-seven experiments, seventeen were performed with the 30-g charges and five each with the other charge weights. More experiments were performed with 30-g charges because the effects of liquid temperature upon the impact loading were investigated with this weight of charge. Columns 1, 2, and 3 of Table 1 summarise the experimental conditions.

3. Results

In presenting the results it is convenient to consider the pressure and strain-gauge records separately. They will be considered jointly in the discussion towards the end of the paper together with the observations of vessel damage.

3.1 Pressure Measurements

Figure 2 shows a pressure-time curve obtained with the 15-g charge in sodium at 150°C and one from 15 g in water, chosen at random to indicate the shape of individual signals from the pressure transducer. Salient points were measured on the oscillograph traces and the data were recorded on punched cards suitable for processing in an IBM 7030 'Stretch' computer. The curves in Fig. 2 are specimens of the computer output. Time zero on these curves corresponds to the signal from the ionisation probe on the detonator.

It will be seen in Table 1 that the experiments have been arranged in groups. Most of the analysis was performed on the average results of each group. The mean peak pressures quoted in Table 1 are the arithmetic means of the maximum pressures recorded by each of the four pressure transducers in each experiment. Figure 3 shows graphically the variation of mean peak pressure with charge weight. For the 15-g charge the mean peak pressures in sodium were about 1.7 times higher than in water, whereas they were only 1.5 times higher with the 60-g charges. It can be inferred from Fig. 3 that within the range of charge weights and for this particular

TABLE 1. SUMMARY OF EXPERIMENTAL CONDITIONS AND MEASURED PEAK PRESSURES

Expt No.	Group Title	Actual Temp, °C	Mean Peak Pressure (psi)	Mean Peak Pressure of Group ^a	
				Pressure (psi)	Standard Error (psi)
3.1 3.7	Water 15 g	20 20	3,200 5,100	4,050	±810
1.2 1.5 1.8 2.1 2.2	Water 30 g	20 90 20 26 90	6,060 7,700 6,770 6,460 6,910	6,780	±300
3.2 3.9	Water 60 g	20 20	10,890 ^b 14,460 ^b	12,930	±990
3.10 3.12	Sodium 150°C 15 g	160 170	8,050 7,210	7,630	±820
3.6	Sodium 400°C 15 g	310	6,430	6,430	±1140
1.1 1.3 1.7 2.4 2.7 3.11	Sodium 150°C 30 g	160 175 160 138 150 220	10,380 12,480 11,020 10,090 ^b 12,450 ^b 13,610 ^b	11,580	±530
1.4 2.3	Sodium 250°C 30 g	230 260	10,390 11,150	10,770	±460
2.5 2.6 2.8	Sodium 400°C 30 g	350 450 450	13,240 ^b 12,620 ^b 11,400 ^b	12,420	±680
2.9	Sodium 550°C 30 g	530	No Measurements Obtained ^b		
3.3 3.8	Sodium 150°C 60 g	160 160	17,220 ^b 17,200 ^b	17,210	±1250
3.5	Sodium 400°C 60 g	410	19,250 ^b	19,250	±1240

^(a) The group mean peak pressures have been calculated from the individual pressure records and not from the mean peak pressures of each experiment.

^(b) Indicates that the core vessel ruptured.

geometry the peak pressure produced by a charge in sodium can be obtained by using twice the weight of explosive in water.

a) *Calculation of Group-averaged Pressure-Time Curves.* An average pressure-time curve was obtained for each experiment by calculating the arithmetic mean of the pressures measured by the four gauges. This was done at intervals of 0.01 ms, starting at the common time zero. There was

FIG. 2. Typical Pressure-time Curves for a 15-g Charge.

FIG. 3. Variation of Mean Peak Pressure with Charge Weight.

some small random scatter in the arrival times of the high peak at each gauge position in any single experiment, indicating that the liquid surface was not plane when it impacted upon the lid. There was also some variation in the arrival times of the peak pressures between experiments performed under the same conditions. This suggests that the liquid levels were not consistent from model to model. This fault was corrected after the first few experiments.

In order to produce a valid mean pressure-time curve for each group, it was decided to introduce a shift of the time scales of the average curves of each experiment so that the steep leading edge of the pressure peaks coincided in time. This was done in the computer programming by making the time at which the pressure first reached 600 psi common to all curves in the group and equal to the average time of all the experiments in that group.

Figure 4 shows the average pressure-time curve

for the water experiments and for the group of experiments using sodium at 150°C, each with a 30-g charge. The peak pressures shown in Fig. 4 are lower than the average of the peak pressures quoted in Table 1 because of the slight scatter in arrival times of the peaks within any one experiment. The averaging process has produced a lower peak pressure of longer duration containing the correct impulse. The averaged curves represent the forcing pressure integrated over the whole lid, but it must be remembered that any single point on the lid experienced peak pressures as high as those quoted in Table 1.

The averaging process has also produced pressure-time curves in which random fluctuations of pressure on individual traces have become smoothed out whilst systematic fluctuations have become more apparent. Figure 2 shows random fluctuations whilst the averaged curves in Fig. 4 show periodic oscillations. The average pressure-time curves of each

FIG. 4. Average Pressure-time Curves for 30-Charge.

group of experiments were similar in shape to those shown in Fig. 4.

In programming the analysis of the pressure measurements, the loading on the lid of the core vessel was considered to have two components: a high-pressure phase of 0.2–0.4-ms duration, superimposed upon a more slowly rising pressure pulse which proceeded to oscillate with a period of 0.6–0.8 ms. It was assumed that the peak pressure phase was caused by the impact of the surface layer of liquid which was accelerated upwards by the shock wave from the

charge, whilst the oscillatory pressure phase was due to the oscillation of the bubble of detonation products and vapourised liquid.

In order to compare the oscillatory pressure loadings, the peak pressure regions were subtracted from the average pressure-time curves for each group. This was achieved by referring to the original pressure-time data from the gauge records and extracting the data points between the initial lead-in on the steep front of the peak and the pressure minimum after the high peak. The computer programme was

FIG. 5. Unit Impulse for 15-g Charge without Impact Peaks

FIG. 6. Unit Impulse for 30-g Charge without Impact Peaks.

FIG. 7. Unit Impulse for 60-g Charge without Impact Peaks.

TABLE 2. IMPULSE CONTAINED IN PEAK PHASE
(WITH STANDARD ERROR)

Experimental Conditions		Impulse (I_p), psi-ms, and Standard Error		230W ^{1/3} (W in g)
Charge, g	Liquid			
15	Water	465	±148	570
	Sodium 150°C	750	±198	
	Sodium 400°C	662	±182	
30	Water	670	±50	715
	Sodium 150°C	729	±38	
	Sodium 250°C	737	±126	
	Sodium 400°C	691	±44	
60	Water	1,093	±136	900
	Sodium 150°C	848	±51	
	Sodium 400°C	1,235	±79	

then re-run with a linear interpolation to cover the gap made in the pressure-time curve. The range of data to be extracted was chosen so that the linear interpolation was tangential to the lead-in of the complete pressure-time curve. The revised curves are indicated by the dotted lines in Fig. 2.

It was found convenient when making comparisons to consider the sodium and water impulse curves, impulse being defined as

$$I(t) = \int_0^t P dt.$$

The impulse-time curves from the re-run of the computer programme (using the data without the peak pressure phase) are shown in Figures 5, 6, and 7 for the 15-, 30-, and 60-g charges, respectively. The sodium and the water curves are shown, together with the water curve multiplied by a factor of 1.5 in Figures 5 and 6 and by a factor of 1.4 in Fig. 7. The factors were chosen to give the best agreement with the sodium curves.

Table 2 shows the values of impulse subtracted from the pressure records in making the above comparisons (i.e., the impulse contained in the peak pressure phase above the linear interpolation). The standard errors are large, but the trend of these figures suggests that the impulse in this phase is the same for sodium and water for a given charge weight. This is shown particularly by the 30-g-charge results for which most information was obtained. The impulse does not vary significantly from the relationship $I_p = 230W^{1/3}$, values of which are included in Table 2.

3.2 Strain Measurements

Figure 8 shows typical strain-gauge records from one bolt in experiment 3.11 with a 30-g charge in sodium at 220°C. Some bending of the hold-down bolt occurred, since the maximum strain measured by the gauge on the inside face was greater than that measured by the outer gauge. The shape of the strain-gauge signals was not at all like that of the

FIG. 8. Strain-gauge Records from One Hold-down Bolt, Experiment 3.11, 30-g Sodium at 220°C.

pressure-time records. The dynamic response of the model can be regarded as analogous to the response of a mass-spring system. The strain gauges are mounted on a spring attached to a mass (i.e., the lid of the containment vessel) to which is applied the forcing function (as defined by the pressure-loading curves). The analogy is further complicated, however, by the fact that the whole experimental assembly could lift, so that the spring cannot be regarded as being attached to an infinite mass.

One thing common to all the strain-gauge records was that the first strain signals occurred before the first liquid impact was recorded upon the lid. In experiment 3.11 the maximum pressure was recorded 1.25 ms after time zero, whereas the first maximum strain in this experiment (as shown in Fig. 8) arrived at 0.8 ms. The probable explanation for this is that the base of the containment vessel received an initial downward impulse from the detonation, and this was transmitted by stress waves through the walls to the flange of the containment vessel which, in turn, received a downward impulse.

Table 3 summarises the peak values of the forces measured on the hold-down bolts, and Fig. 9 is a plot of these peak forces versus temperature of the sodium for experiments with 30-g charges.

The strains measured by the U.K. team were generally greater than the strains measured by the C.E.A.

team, but the general shapes of the signals were always very similar. The discrepancy has yet to be resolved, but it is thought that the difference in frequency response and writing speeds of the two recording systems may account for the differences in

TABLE 3. MEAN VALUES OF THE MAXIMUM UPWARD FORCE MEASURED IN THE HOLD-DOWN BOLTS FOR EACH EXPERIMENTAL CONDITION

Group Title	Group Mean Value of Maximum Force, 10 ³ lb	
	CEA	UKAEA
Water 15 g	81	92
Water 30 g	101	158
Water 60 g	97.8	148
Sodium 150°C 15 g	110	149
Sodium 400°C 15 g	144	152
Sodium 150°C 30 g	148	183
Sodium 250°C 30 g	112	173
Sodium 400°C 30 g	140	168
Sodium 550°C 30 g	177	195
Sodium 150°C 60 g	169	263
Sodium 400°C 60 g	141	227

Note. The maximum force measured in each experiment differed from the above mean values by no more than the 5% accuracy which the manufacturers quote for the strain gauges.

FIG. 9. Maximum Force Measured in Hold-down Bolts versus Sodium Temperature—30-g Charge.

measured strain levels. This hypothesis has to be checked.

3.3 Damage to Vessels

Detailed measurements were made of the distortions to all vessels after each experiment. It is not intended to include all this information here, however. Figure 10 is a photograph of the damaged vessel from Experiment 1.3 (30-g charge in sodium at 175°C) compared with an undamaged vessel. The damaged vessel has been reduced in height, and a series of corrugations has been formed in the walls, including one below the level of the heating coil. The cap was also torn off the top of the re-entrant cylinder, and the base of the core vessel dished upwards. This was the pattern of damage to all vessels.

For a given charge weight the vessels containing sodium were more severely distorted than those containing water and the distortions increased with increasing temperature. In all experiments with 60-g charges and in some with 30-g charges the vessels split vertically; the initiation of the split appears to have been at the point where the thermocouple insert was welded into the vessel wall.

4. Discussion of Results

From the analysis of the pressure-time records, as described above, an empirical method has been derived for constructing an approximate pressure-time curve for sodium from the averaged pressure-time curve obtained with the same weight of charge in water. The water pressures are multiplied by a fac-

FIG. 10. Typical Core-vessel Damage from 30-g Charge in Sodium.

FIG. 11. Comparison of Pressures from Sodium at 150°C with the Curves Derived from the Water Results.

TABLE 4. CALCULATED AND MEASURED IMPACT PRESSURES

Experimental Conditions	Transit Time across 5 cm, ms	Calculated Impact Pressure, psi	Average Measured Impact Pressure, psi ($=\rho cv$)
Sodium 15 g	1.83	8,800	7,000
30 g	1.00	16,200	11,600
60 g	0.77	21,000	18,200
Water 15 g	2.30	4,700	4,000
30 g	1.22	8,800	6,800
60 g	0.81	13,200	13,000

For Sodium ρ at 250°C = 0.891 g/cm³ (reference 3)
 c at 250°C = 2,507 m/s (reference 4)

For Water ρ at 20°C = 0.998 g/cm³ (reference 5)
 c at 20°C = 1,484 m/s (reference 5)

tor of 1.5 and the duration of the impact peak is divided by the same factor (to keep the impulse constant in this phase). This has been done for the average results of each charge weight as shown in Fig. 11 where the calculated results are compared with the results in sodium at 150°C. This method appears to be accurate (at least for this geometry) within the scatter of the results.

Ideally, we would like to have obtained from these experiments a method of estimating the weight of charge that would have to be used in water to simulate a given size of charge exploded in sodium, since in our scaled-model investigations we generally use water in place of the sodium coolant. Figure 3 shows that there is a simple factor of two in the weights of charge necessary to produce a given peak pressure in sodium and in water. There is no simple factor connecting any of the other explosive effects, however. For example:

1. The duration of the impact-loading phase is charge-dependent, so that doubling the weight of charge in water, although producing the same peak pressure as a charge in sodium, will produce a greater impulse. This can be seen from Table 2.

2. It appears that the distortion produced by a given weight of charge in sodium is less than that produced by twice the charge weight in water.

The pressure loading on the lid of the containment vessel is relieved by venting of the core vessel. The failure of the vessels with some of the 30-g and 60-g charges was probably caused by the initial shock wave from the charge so that venting occurred in the early stages of the loading phase. Further venting could have occurred around the top of the core vessel when the top flange was forced out of the locating

recess in the lid by the downward thrust on the flange.

In Fig. 5, the water result times 1.5 diverges from the sodium curves after the first bubble oscillation, indicating a higher pressure level in sodium. This may be due to reaction of sodium with the water vapour in the explosion products. If this is so, one would expect to observe that the impulse curves for sodium and water diverge at later times with the larger charges. Unfortunately, the period for which the pressures were recorded was terminated by the uranium rods above the charge impacting upon the transducers. The period of the oscillation does not vary significantly with charge weight and is probably a function of the model configuration. In an infinite medium the bubble period increases with charge weight, but the period is reduced by increased confinement.⁽²⁾ In this particular arrangement the two effects appear to be self-cancelling.

Examination of the results of the experiments with 30-g charges (see Fig. 6) shows that the total impulse is slightly higher at 400°C than at 150°C in sodium. Additionally, the strain-gauge results showed a minimum impulse at 250°C (see Fig. 9), but in both cases the differences are small compared to that between sodium and water. There was no observable variation of pressure loading with temperature in the water experiments. Nor was there any apparent variation of pressure loading in the few experiments for which the level of the sodium was not correctly adjusted. The early arrival of the first impact pressures on the lid suggests that the space above the liquid in these experiments was only about 3 cm instead of the intended 5 cm.

The delay times between detonation and the first impact have been measured, and the transit times of the detonation and shock waves through the explosive and liquid have been estimated. The difference between the two times is the transit time of the liquid surface from its rest position to the lid. If the surface layer of liquid (which is accelerated by the shock wave) acquires its terminal velocity almost instantaneously (i.e., in a few μ sec) and travels at uniform velocity, then an expected impact pressure can be calculated from the relation

$$P = \rho cv$$

where ρ is the liquid density, c the acoustic velocity of liquid, and v the impact velocity of liquid.

Average values of the transit time and the calculated impact pressures are shown in Table 4 together with the average values of the measured pressure. The calculated pressures are higher than the measured pressures. The ratio

$$\frac{\text{peak pressure measured in sodium}}{\text{peak pressure measured in water}}$$

is less with 60-g charges than with 30-g and 15-g charges (see Fig. 3), and the ratio of impact velocities (and hence calculated pressure ratios) shows the same trend. Hence we can assume that the pressure transducers functioned correctly. Individual pressure-time curves show a random variation of arrival time for the first impact signal in any single experiment, indicating that the liquid surface is not plane. There is also some scatter in the height of the impact pressure peaks, so it is likely that the surface irregularities are comparable with the diameter of the sensitive face of the pressure transducers (namely, 0.26 in.). This could explain why the measured impact pressures are less than the calculated values. Alternatively, an inadequate response of the amplifiers to high frequencies would reduce observed peak pressures.

5. Conclusions

It has been established that the forces transmitted to the lid of the core vessel are significantly higher with sodium than with water. By comparison, varying the temperature of the sodium has a much smaller effect.

It was not possible to obtain a simple formula relating the effects in sodium to those in water because the forces on the top closure depend upon the deformation produced in the core vessel as well as upon the physical properties of the coolant. However, the physical processes involved in producing the forces in a fast reactor geometry are now better understood. This should enable us to derive the equations of motion which describe the response of the model to explosive loading, thus providing a direct comparison of the measurements of pressure and force. We should then be able to resolve the differences in the two methods of strain recording and also check the validity of the equations of motion.

In order to interpret model experiments using water in other reactor geometries, one needs to know the extent of the deformation in these systems, since any pressure relief resulting from deformation has a marked influence upon the loading of the top closure in the bubble-oscillation phase.

The initial impact loading is unlikely to be influenced by this deformation, so the early loading phase in sodium may be obtained directly from the results presented in this paper.

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Discussion

Mr. McCarthy (PRDC): What were the temperatures of the sodium and the water?

Mr. Walford: The sodium temperatures were within the range of 150 to 550°C. The temperature of the sodium seemed to affect the pressure loading very little. Much more important is the liquid that is used. We did water experiments at 20 and 90°C, and we could detect no significant difference at all.

Mr. McCarthy: The NOL work (see discussion in preceding Paper) showed quite a substantial difference which we attributed to the proximity of the higher-temperature water to the boiling point and easier generation of steam by the heat from the explosion.

Mr. Walford: This is what we expected. This was really half of the object of the experiment, and we were rather surprised to find that this didn't happen.

Design Factors Influencing the Containment of Internal Explosion in Fast Reactors

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SYNOPSIS

In recent years the Foulness Establishment of the U.K.A.E.A. has been engaged in a continuing investigation of the containment of core explosions in fast reactor systems. In this work small charges of chemical high explosive have been detonated in scaled models of the reactor structure. Beginning with the investigation of the containment potential of existing reactors, the work progressed to basic studies of different containment geometries (the conference paper by N. J. M. Rees is an example), and the model approach has now become an integral part of the design process for large fast reactor systems. Geometrical arrangements are studied in instrumented, overstrong models, while information on modes of deformation and energy absorption is obtained from models having correctly scaled structural strength.

The aim of this work has been to establish the sequence of events following a core explosion and to identify the loading and response mechanisms occurring in the structure. Processes studied include the plastic flow and rupture of metals, shock-wave interactions, venting and pressure rise in gas spaces, crushing of porous materials, and the impact of shock-driven liquids. This has led to the possibility of adjusting the balance between these processes by appropriate design so as to achieve the necessary structural integrity with the least economic penalty or interference with normal working.

Introduction

Although it is likely that large fast reactors of the future can be designed so that a core meltdown is impossible, in the present state of knowledge such an event cannot be entirely ruled out. We must, therefore, consider the ability of the reactor system to contain the effects of an explosion in the core which, pessimistically, may be equivalent to several hundred pounds of TNT. It is not sufficient to surround the system by a continuous outer containment strong enough to withstand the immediate forces of the explosion; the radioactive and corrosive debris must be contained for many years. A large reactor will con-

tinue to produce several megawatts of fission product decay heat for a period of hours or days after an explosion, and it is necessary to provide some means of removing this heat so that the core debris does not melt its way through the containment. The designer, therefore, needs to know in some detail the loads imposed on different parts of the reactor structure by explosions of various magnitudes, and also the damage produced by these loads. A design which is balanced for normal operational loads is unlikely to be properly balanced for the containment of explosions. The early failure of components close to the core can markedly affect the loading of more outlying parts of the structure, and some geometrical arrangements can effectively channel the explosion energy and produce the hazard of local failure.

In this paper I consider the various mechanisms by which forces can be transmitted from the explosion to the structure and ways in which the energy of the explosion can be absorbed within the structure. A proper understanding of these mechanisms should enable designers to produce an economical balanced design from the point of view of explosion containment. This discussion is based on a number of model investigations of the kind described by Rees⁽¹⁾ and by Drevon, Falgayrettes, and Walford.⁽²⁾

I shall restrict my discussion to liquid-cooled fast reactors. For the sake of definiteness I shall consider oxide fuel contained in stainless steel pins supported in stainless steel fuel boxes and cooled by liquid sodium. I shall assume that the sodium is covered by an inert gas blanket and is held in a steel tank within a concrete vault which has a rotatable shield plug in the roof. These features are not essential, but will provide a framework within which to discuss explosion containment.

Comparison with Chemical Explosion

Before we can generalise the results obtained in model experiments, we must decide how closely chemical high explosive simulates a meltdown excursion. The gravity-controlled collapse model has already been discussed in detail by Hicks and Menzies.⁽³⁾ At

the start of the power excursion, the core is partially molten, and consists of solid and molten uranium and/or plutonium oxide and stainless steel. The sodium will have been vapourised and expelled from the core region. The breeder is assumed to be intact. The energy release in the excursion is so rapid (it is complete in less than 1 ms) that some fraction of the oxide fuel is vapourised and superheated to a high temperature and pressure, with virtually no heat transfer to the stainless steel or the remainder of the fuel. At this stage the core consists of high-pressure oxide vapour in which molten and solid fuel and stainless steel are dispersed. It is surrounded by the breeder structure and liquid sodium in their pre-excursion state. As the fuel vapour expands it drives a shock wave through the breeder assembly into the coolant, converting some of its internal energy into compression and kinetic energy of the surroundings. This is qualitatively similar to the situation when chemical high explosive detonates. The detonation products, which consist mainly of gaseous oxides of carbon and nitrogen and of water vapour, replace the fuel vapour. The initial pressure and temperature are similar in the two cases. The main difference is that in the case of the chemical explosive virtually all the energy released in the detonation is transferred to the surrounding medium as compression and kinetic energy, whereas in the nuclear excursion a large proportion (perhaps more than 90%) of the total energy remains in the core materials when these recondense. Heat transfer rates from the liquid phase are so low that this energy (denoted by Q^* by Hicks and Menzies) plays no part in the explosion process. When performing model experiments with chemical explosive, therefore, it is necessary to relate the detonation energy of the charge to the excess energy, $Q - Q^*$, of the nuclear excursion.

Damage Mechanisms

For successful containment of a meltdown explosion, all the energy has to be degraded into heat in the solid and liquid phases without breaching the outermost containment, essentially through processes of internal friction. Any medium is irreversibly heated by the passage of an intense shock wave through it. The amount of heat lost is a function of the shock strength; below a certain strength the loss is negligible, and the shock is said to have become acoustic. For high explosive in water this occurs at about ten charge radii, and within this distance about 25% of the detonation energy is deposited in the water. As the acoustic properties of liquid sodium are similar to those of water, a similar proportion of the excursion energy will be absorbed in a fast reactor having a sufficiently large volume of coolant. A similar amount

of energy is carried to large distances by the acoustic wave. The remainder of the energy is associated with the oscillatory flow near the explosion cavity and appears in subsequent pressure pulses.

If a shock wave reaches a discontinuity in the medium through which it is travelling (either a change of phase or of material), a partial reflection occurs. If the acoustic impedance (the product of density ρ and sound velocity, ρc) increases at the discontinuity, the pressure at the interface is increased, the transmitted shock is increased in strength, and a reflected shock wave travels back into the region already traversed by the incident shock. Conversely, if the acoustic impedance decreases, as at a liquid-gas interface, the transmitted shock is weakened, and an expansion wave travels back into the shocked region. This expansion wave may set up tensile forces in the material which exceed its tensile strength, producing the phenomenon of scabbing in solids, or of cavitation in liquids. Momentum trapped in the separated layer causes it to fly off. A similar process may accelerate a membrane, such as a tank wall, but this would be restrained by tensile forces in the direction of the membrane surface, set up by curvature. Such tensile forces are the major cause of structural failure.

When the separated layer just described, be it liquid sodium or a portion of the reactor vessel, reaches a rigid boundary—the concrete vault or roof plug—a high impact pressure will be produced. For impact on a completely rigid boundary the instantaneous pressure P is equal to the product of the acoustic impedance ρc and the velocity u of the impacting material:

$$P = \rho c u.$$

If the impact is localised, this pressure will be rapidly relieved by sideways expansion. If the reactor vessel fails under the shock loading, the contents will vent into the concrete vault, and unless the free volume available in the vault is large a substantial pressure rise will result. On the other hand, if the vessel is strong enough to survive the shock loading, the kinetic energy of the coolant will be re-directed upwards, lifting the coolant and compressing the blanket gas. Further impact pressures may be produced by this process.

The Sequence of Events

A number of different processes have been described which can lead to damage: shock compression, scabbing and cavitation, membrane rupture, impact, and compression of vault and blanket gases. These are transient processes and they do not all occur simultaneously. Also, they are interdependent; for example, the vault gas is compressed only if the reactor vessel

FIG. 1. Schematic Representation of Reactor System.

wall fails. It is the task of the reactor designer to adjust the geometry of his layout and the strength of components of the structure to produce a planned sequence of failures which will ensure that the energy of the explosion is dissipated with the least hazard to containment. Both the time of action and the intensity of the forces are important to proper containment design. It is, therefore, important to establish the sequence of events following an explosion in the particular system being considered.

A typical sequence is given below. It corresponds to a core explosion of excess energy equivalent to 250 lb TNT in the reactor system shown schematically in Fig. 1. The core is immersed to a depth of 12 ft in sodium within a reactor vessel 9 ft in diameter. This is surrounded by a graphite-block neutron shield and is connected to heat exchangers by ducts. The whole is contained in a concrete vault 40 ft in diameter and 24 ft high internally. The top of the reactor vessel is closed by a rotatable plug weighing 70 tons, the upward movement of which is restrained by an energy-absorbing device. There is a gas blanket 1.5 ft deep above the sodium in the reactor vessel and heat ex-

changers. The following sequence of events was derived from measurements in $\frac{1}{20}$ -scale models.

0	End of excursion
2 ms	Shock at sodium surface
9 ms	Reactor vessel fractured
17 ms	Sodium impact on core plug (3,000 psi)
24 ms	Beginning of pressure rise on vault wall
30 ms	Pressure rise in heat exchangers, pressure decays on core plug
100 ms	Vault pressure maximum (3.5 psig)
140 ms	Heat exchanger pressure maximum (10 psig)
180 ms	Core plug brought to rest (1.5 ft displacement)

The model analogy cannot be carried much beyond this time as the residual heat in the core debris and heat effects from fission product decay predominate at later times. The figures can be scaled to larger or smaller reactor systems of similar geometry. The time scale will vary in proportion to the linear dimensions, the pressure values being unchanged. The events will correspond to an equivalent weight of TNT which is proportional to the cube of the linear scale ratio.

FIG. 2. Effect of Peak Impact Pressure on Tank Roof due to Variation of Air Space.

Examples from Model Tests

The sequence of events and relative magnitude of the loads will be influenced by the following design features:

- (1) core confinement (mass and rigidity of the breeder structure);
- (2) quantity of sodium in core vessel (distance to vessel wall);
- (3) height of sodium above core;
- (4) depth of gas blanket;
- (5) strength of thermal shield;
- (6) strength of core vessel;
- (7) presence of graphite neutron shield within core vessel and crushability of shield;
- (8) free volume in vault.

The effect of varying several of these factors has been illustrated by Rees.⁽¹⁾ In one series of tests he varied the water level in his tank, keeping all other factors constant. Thus, both the height of water above the charge and the depth of air space were varied. The effect on the peak impact pressure on the roof of the tank is shown in Fig. 2, in which the length scale has

been multiplied by 12 to correspond to an explosion of 250 lb TNT in a tank 24 ft in diameter and 24 ft high. In addition to the steady decrease of impact pressure with increasing air space, the time between detonation and impact was increased. Rees also demonstrated the effects of surrounding the explosive charge by a model breeder structure, and of replacing rigid tank walls by a light-gauge tank. The breeder structure, although it was ruptured, considerably reduced the shock pressure on the side wall of the tank and channelled the energy upwards towards the core plug. The light-gauge tank was ruptured by the shock wave, relieving the pressure within the tank. This cut short the duration of loading on the roof and core plug, considerably reducing the plug jump.

The effect of having a crushable barrier within the reactor vessel has been demonstrated in models of the assembly shown in Fig. 3, which is similar in principle to the EBR-II reactor. The graphite neutron shield was enclosed between two steel cylinders. In one model the graphite was in the form of well-compacted powder and chips, while in another model it con-

FIG. 3. Schematic Representation of Reactor Vessel with Crushable Barrier.

sisted of closely fitting solid blocks. The explosive change was equivalent to 250 lb TNT full scale. The results of the two tests are compared below.

	Form of Graphite	
	Compacted Powder	Solid Blocks
Max circumferential strain:		
Neutron shield inner wall	20%	5%
Neutron shield outer wall	3%	5%
Peak impact pressure:		
Core plug	4,600 psi	8,200 psi
Outer roof	140 psi	370 psi
Core plug stopping distance	1.8 ft	2.4 ft

The 70-ton core plug was restrained by a yielding device which exerted an average force of 1,300 tons.

The more rigid neutron shield deformed much less than the other and so absorbed less energy of the explosion. It acted as a gun barrel, directing the coolant flow upwards towards the core plug, and downwards into the outer part of the reactor vessel, compressing the gas blanket and producing more than twice the pressure rise on the outer roof.

The dependence of the pressure rise in the concrete vault on the free volume available for expansion has been demonstrated in the system shown in Fig. 1. In one model the graphite neutron shield was enclosed in a strong inner containment which reduced the free volume around the reactor vessel by a factor of 14. A rise in pressure of 50 psig was measured in this containment, compared to 3.5 psig formerly. It can, therefore, be said that over quite a wide range of volumes, the pressure rise in the vault is inversely proportional to the expansion volume. Other tests have shown that, in a given geometry, the pressure rise in

the vault is also directly proportional to the explosion energy.

Finally, the interdependence of different effects of the explosion has been demonstrated, also in models based on the system shown in Fig. 1. In these tests the reactor vessel was only half filled with coolant. Two sizes of explosive charge were used, equivalent to 200 lb and 400 lb TNT. With the smaller charge the reactor vessel was distorted but did not rupture; with the larger charge the vessel was split wide open and the neutron shield demolished. A smaller core plug was used for these tests, having a full-scale weight of 20 tons. It was secured to the reactor vessel by bolts which failed in tension, and for both sizes of explosion the plug rose to a height of 80 ft. Although the peak pressure acting on the base of the plug was much greater for the larger explosion, the rapid failure of the reactor vessel wall cut short the load, and the total impulse applied to the plug was the same in both cases.

Conclusion

The main thesis of this paper is that, in order to design a reactor system capable of containing a melt-down explosion, one must have a clear understanding of the sequence of events and structural failure resulting from such an explosion. By having a clear qualitative picture of the pattern of events in mind, one can ensure there is no direct path by which the explosion energy can be conducted to the outer boundaries of the containment. By this means one can outline a structural system which is potentially capable of absorbing all the explosion energy, and which can be modified, refined, and optimised in a series of small-scale model tests.

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Discussion

Mr. Merckx (Battelle-Northwest): We established a design nightmare the other day; in all cases we voided the core. If we're now establishing the design explosion, shouldn't we start from some sort of voided type of material, with the first wave impulses having to go through some sort of vapor phase instead of the liquid phase? What are your considerations on this as far as design?

Mr. Samuels: We did assume here that the sodium

had been expelled from the core, but not from the whole vessel, by the start of the excursion. The driving material was vaporized fuel and not sodium. Mr. Hicks has looked in some detail at the comparison of the initial effects. He finds that after the end of the first bubble expansion, TNT or other high explosive does follow quite closely what we would expect from expanding fuel vapor.

Mr. Wolfe (GE): I'm still confused. In the American work to which Mr. McCarthy referred before, they calculated the plug jump for Fermi on the basis of the maximum pressure which the vessel could contain before it ruptured, and then assumed that that pressure accelerated the plug. In the British papers, both yours and the ones before, I gather that that's not the mechanism which you assume for acceleration of the plug. Am I right or wrong on this?

Mr. Samuels: It's partly that. I think that in the calculations before Wise's work it is assumed that the driving pressure was just the unsteady pressure you get by putting that much energy into the gas in the vault. When you have a small core vessel in a very large volume, this is not very far wrong. If the arrangement is such that there is direct impact on the plug by the sodium, more than half the total particle energy comes from that very impact and not from the unsteady pressure. The balance does come from the unsteady pressure. Both are important. The arrangement of the system determines which is more important.

Mr. Denielou (CEA, France): Has the same type of study been conducted anywhere in the case of a reactor having a fuel-handling cell overhead?

Mr. Bump (ANL): I don't know whether this is directly connected or not. In the FARET design at the moment we think it will not be necessary to hold down the cover on the reactor vessel. Assuming that the explosion occurs and that the energy is absorbed by the roof of the fuel-handling cell, the problem for FARET is that the cover has poison control rods attached to it; thus, we have to worry about what happens following the maximum accident if the poison rods are possibly pulled out. In our present approach we're trying to show that the core will have to be permanently disassembled, if enough energy is released in order to raise the cover.

Mr. Klickman (APDA): Could you elaborate on the plug-stopping distances that you gave? How did you arrive at them?

Mr. Samuels: This was in our small model. We used a very simple system, using plastic deformation of mild steel to fight the force. The 70-ton core plug was restrained by this yielding device, which exerted an average force of 1300 tons. Study of our results showed

that this corresponded to the particular case where we had different neutron shields; the one was 1.8 ft and the other 3.4 ft. Coming back to the question of Dr. Wolfe, these plugs are not normally solid structure. They are fairly uncrushable and deformable themselves, and it seems quite possible in some reactors to absorb the energy in the plug structure and to bolt the top of the plug down firmly to the containment. This will provide a method of complete containment inside the primary system.

Mr. Wolfe: On both your point and Mr. Denielou's, at SEFOR we took the latter course of action and bolted the head down; we then provided the bolts with enough energy absorption in the stretch to absorb the energy that the head would see from shock. Going back to the other point, in Wise's work he found that the shock energy imparted to the head in at least the small type of vessel, say the Fermi type of vessel, is only a small part of the energy from the expansion of the gases, even when sodium is right in contact with the plug. When an air gap exists between the sodium

and the plug, he found that even less energy was imparted in the initial shock. So I'm still not sure whether or not there is a difference between his work and yours.

Mr. Samuels: I'm not sure whether you're referring to the experimental measurements by Wise or to the earlier calculations for Fermi. There seems to be a difference in our results. I suspect it does depend on the geometry and how much venting into the vault there is in cutting short of the time which the impact lasts.

Mr. Nims (APDA): How much stretching can the vessel withstand before rupture?

Mr. Samuels: This depends very much on the uniformity of loading, and in any practical reactor the structure just isn't all that uniform. We have our bits and pieces stuck in various places. We have measured permanent deformations up to 20% in the event of the rupture. On the other hand, in certain places we've only measured 1-3% stretch before the thing splits. It does depend on uniformity of construction and the uniformity of molding.

Response of a Helium-cooled Fast Reactor to Changes in Coolant Flow and Reactivity*

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ABSTRACT

The ORNL design of a helium-cooled fast reactor was studied with respect to its response to various types of accident conditions. These accident conditions included step and ramp reactivity insertions, and loss of coolant pressure and flow. The objective was to determine what kind of accidents this reactor could withstand before fuel melting began, without regard to questions of accident credibility.

A Doppler coefficient $T(dk/dT)$ of -0.0053 and a total helium loss coefficient of $+0.0051 \delta k$ were calculated for this reactor. These coefficients proved adequate to prevent melting for a step reactivity addition of less than 29 cents, if no scram occurs, and a step change of \$1.35 if a power-level scram occurs. For reasonable rates of coolant loss (half-life in the vicinity of 20 sec), the reactor would not approach prompt criticality at any time during the transient. The ability of the reference reactor to remove delayed fission product heat in the event of two loss-of-coolant accidents was determined.

Interest in gas-cooled fast reactors as a possible alternative route to successful plutonium breeding is due in part to differences in physics characteristics between sodium-cooled and gas-cooled reactors. One would expect, for example, that the reactivity effect of the coolant would be much smaller with gas, so that the core geometry could be optimized without regard to void or coolant-loss coefficients of reactivity. One would also expect a harder neutron spectrum in the gas-cooled case, resulting on the one hand in a higher breeding ratio, but on the other hand in a lower Doppler coefficient. In this paper we point out first that the reactivity effect of a gas coolant may not in fact be negligible and should be considered in the reactor design. Secondly, we inquire what degree of protection may be provided by the smaller Doppler coefficient that does exist. Finally, we examine the

power and temperature excursions to be expected in the event of loss of coolant flow and pressure.

We take as our reference design a 1000-MWe helium-cooled fast reactor design prepared by ORNL in a recent study.⁽¹⁾ Some of the principal features of the reactor are given in Table 1. This design was obtained by assuming a maximum linear heat rating of 40,000 Btu/hr-ft (12 kW/ft), a maximum cladding surface temperature of 1500°F, and a maximum pumping power of about 10% of the net electrical output. The coolant pressure was limited to 1000 psi on the basis that higher pressures would be likely to introduce unknown problems in component technology. Dimensions of the fuel pin, the core, and the blanket were obtained by optimizing with regard to fuel-cycle costs.

We used one-dimensional multigroup-diffusion calculations for the static analysis of the core physics. We found it important to do both axial and radial calculations and to iterate the transverse buckling until consistent results were obtained. We checked one such result against a two-dimensional multigroup calculation and found excellent agreement in k_{eff} and power distribution. The 16-group cross sections were obtained by averaging, for most nuclides, over the spectrum for a composition similar to that of the reference core. The isotopic content of the fuel was determined by doing equilibrium recycle burnup calculations and choosing the average core composition during a 100,000 MWd/T cycle as representative. The isotopic composition of the plutonium came out to be 61:31:5:3 (^{239}Pu : ^{240}Pu : ^{241}Pu : ^{242}Pu).

The isothermal Doppler coefficient was estimated by the use of Doppler-broadened cross sections for several different temperatures in diffusion-theory calculations. The Doppler-broadened cross sections were obtained with the General Electric Rapture⁽²⁾ code and also with the General Atomic GAM-II⁽³⁾ code which uses somewhat different approximations. The two codes gave essentially the same result as shown in Figure 1. We obtained the effective neutron lifetime and delayed-neutron fractions by first-order pertur-

* Research sponsored by the U. S. Atomic Energy Commission under contract with the Union Carbide Corporation.

TABLE 1. ORNL HELIUM-COOLED FAST REACTOR DESIGN

Fuel Material	(Pu, ²³⁸ U)O ₂
Blanket Material	²³⁸ UO ₂
Cladding Material	Stainless steel
Total Thermal Power, MW	2500
Gross Electrical Power, MW	993
Coolant Pressure, psi	1000
Effective Core Length, in.	72
Effective Core Diameter, in.	133
Radial Blanket Thickness, in.	18
Axial Blanket Thickness, in.	15
Fuel-pin Diameter, in.	0.250
Cladding Thickness, in.	0.015
Pin-spacing Triangular Pitch, in.	0.432
Method of Pin Spacing	Helical fins
Average Core Fuel Enrichment, %	10
Average Core Power Density, W/cm ³	145
Average Core Specific Power, kW/kg Fissile	761
Internal Breeding Ratio	0.92
Total Breeding Ratio	1.39
Doubling Time, yr	11

FIG. 1. Reference Reactor Doppler Coefficient.

bation-theory calculations. Direct calculations of reactivity with and without helium gave the value for the reactivity associated with helium loss. The values of these quantities for the reference reactor are shown in Table 2.

The helium-density reactivity coefficient deserves some further comment. It has two components. The transport component, which is always negative, depends principally on the core size and shape, and on the core coolant fraction and density. The down-

scatter component is likely to be positive unless a considerable amount of moderating material is present in the core. In our reference reactor the transport component was $-0.06\% \delta k$ while the downscatter component was $+0.57\% \delta k$, giving a net reactivity gain of $0.51 \delta k$ for complete loss of helium. Since the transport component is so much smaller in magnitude than the downscatter component, it appears that changing the shape of the core would have little effect on the net coefficient. This is in contrast to the situation of liquid metal-cooled reactors for which the core shape in many of the designs has been selected so that a large transport component of the coolant coefficient would be obtained. The downscatter component, which is the important part of the net coefficient, can vary considerably from one helium-cooled core design to another. It can obviously be decreased by having a smaller core-coolant fraction. It also depends very sensitively on the shape of the neutron-energy spectrum and on the energy dependence of the absorption and fission cross sections of both the fuel and structural materials in the core. In calculations of somewhat different helium-cooled cores we have found helium-loss coefficients lower by as much as a factor of three than in the reference core. Thus the magnitude of the net coefficient is at least partially under the control of the reactor designer. The dependence of the helium coefficient on the exact high-energy spectrum and cross sections means that there is probably considerable uncertainty in our calculations of this quantity.

We have also considered CO₂ as a fast reactor coolant. In our design we obtained the same fuel heat rating as for the helium-cooled reactor with a slightly lower fraction of coolant volume (0.59 vs 0.67 for helium) and slightly higher pressure (1300 psi vs 1000 psi for helium). The downscatter and

TABLE 2. KINETICS PARAMETERS FOR HELIUM-COOLED REACTOR

Doppler Coefficient at 965°C, $\delta k/^\circ\text{C}$		-0.43×10^{-5}
Reactivity Gain for Complete Coolant Loss, δk		0.0051
Effective Neutron Lifetime, sec		0.8×10^{-6}
Delayed-neutron Yield Fractions:		
Group	λ, sec^{-1}	Yield ($\times 10^{-3}$)
1	0.0129	0.0833
2	0.0311	0.7093
3	0.1335	0.6425
4	0.3316	1.2517
5	1.262	0.5843
6	3.208	0.1909
Total		3.462

absorption component of the coolant-loss coefficient was then $+0.81\% \delta k$ while the transport component was $-0.52\% \delta k$. With different core design a CO_2 -cooled fast reactor in this size range could have a negative coolant coefficient.

We have studied the response of the reference reactor to a variety of reactivity changes and to coolant flow and pressure changes. The computer program that we used solves the point kinetics equations and the hot-channel heat transfer equations in alternating successive finite time steps in order to determine the axial and radial temperature distribution in the hot-channel fuel pin and the axial temperature distribution in the coolant gas. We take into account the reactivity feedback from the fuel Doppler coefficient and the reactivity effect of any changes in coolant density. For convenience it was assumed that $\delta k/dT$ varies as T^{-1} , whereas our calculations indicate an actual variation as $T^{-1.12}$. An importance-weighted effective Doppler coefficient to be used with the hot-channel temperature was obtained by modifying the actual Doppler coefficient to take into account the ratio of hot-channel temperature to core-average temperature and the initial ratio of hot-channel temperature rise to core-average temperature rise. The fission product heating was computed as a function of time by integrating the data of Shure.⁽⁴⁾ We did not include the negative reactivity feedback from fuel or structural expansion.

We defined as the "failure" point for the reactor the occurrence of either a UO_2 temperature above 6500°F or a stainless steel cladding temperature above 2250°F . Our basis for these choices was that the UO_2 vapor pressure at 6500°F is high enough so that rupture of the cladding is likely,⁽⁵⁾ and that this condition or the failure of the cladding at about 2250°F would cause damage to the core.

The effect of the size of the Doppler coefficient in limiting the excursion resulting from a step reactivity addition without control action is shown in Fig. 2. In this figure the maximum allowable reactivity insertion is the one which produces temperatures just short of our failure point. The maximum step insertion for our reference reactor (without a scram) is \$0.29. If the coolant-loss coefficient were zero, the allowable step insertion would be increased to \$0.46. Hence, in this type of transient, the positive reactivity effect associated with the decrease in helium density in the core is about 17 cents.

Figures 3, 4, and 5 are plots of the heat-deposition rate and peak temperatures in the fuel and cladding as a function of time after a step reactivity insertion. Figure 3 shows the response of the reference reactor to a step insertion of slightly less than that

FIG. 2. Maximum Step Reactivity Insertion as a Function of $T \cdot dk/dT$.

FIG. 3. Response of Reference Reactor to a \$0.25 Step Reactivity Addition without Control Rod Action.

required for fuel-pin failure. Here the peak power was approximately 1.9 times steady-state power.

We repeated this set of calculations but with a much

FIG. 4. Response of Reactor with $T dk/dT = 0.0235$ to a \$1.85 Step Reactivity Addition without Control Rod Action.

higher Doppler coefficient in order to show the effect of the magnitude of the Doppler coefficient. Figure 4 shows the response with a Doppler coefficient of about four times the calculated Doppler coefficient, to a step reactivity insertion of \$1.85. The prompt period caused the power deposition rate to rise over three orders of magnitude. The fuel temperature rose very fast during the power peak. This temperature rise caused the excess reactivity to fall due to the strong Doppler coefficient. The prompt period was terminated when the excess reactivity fell below one dollar. The power level then returned to steady-state power within a factor of two or three. The rates of change of power and temperature were more gradual after the prompt power peak was terminated.

If the coolant coefficient were equal to zero, the response shown in Fig. 4 is changed to that shown in Fig. 5. Figures 4 and 5 are virtually identical during the prompt excursion. They deviate only after sufficient time elapsed for the heat to be transferred from the fuel to the coolant gas. In this kind of transient the coolant-density coefficient is a delayed effect whereas the Doppler coefficient is a prompt effect. The difference in the final peak temperatures was approximately 180°F. We conclude that the coolant-density coefficient becomes less important as the Doppler coefficient is increased.

The maximum allowable reactivity insertion is of course significantly larger if control-rod action is taken. We assumed control-rod insertion starting 50 ms after the step reactivity insertion, producing a linear decrease in reactivity at the rate of \$173 per second for 250 ms. The assumed 50-ms delay is intended to include the times required for the scram signal to be given, for the rod actuation, and for an initial relatively slow rate of reactivity insertion. In this case the maximum allowable step reactivity insertion was \$1.35, with excessive cladding temperatures occurring rather than excessive fuel temperatures. Figure 6 shows the power and temperature responses as a function of time for a \$1.35 step reactivity insertion with control rod action. This plot illustrates the importance of the various time constants. The prompt burst was completed before control action was started. The only mechanism controlling this burst was the fuel Doppler coefficient, which was assumed to act instantaneously. Within a few milliseconds after control action began, the fuel temperature virtually stopped rising. Because of the relatively long time constants for heat transfer, the peak fuel temperature did not begin to fall until nearly a second after the control rods were inserted. As the heat flowed outward from the fuel, the cladding temperature

FIG. 5. Response of Reactor with $T dk/dT = 0.0235$ and Coolant Density Coefficient = 0 to a \$1.85 Step Reactivity Addition without Control Rod Action.

FIG. 6. Response of Reference Reactor to a \$1.35 Step Reactivity Addition with Control Rod Action at 0.05 Seconds.

FIG. 7. Response of Reference Reactor to a \$35/sec Ramp Reactivity Addition with Control Rod Action at 0.05 Seconds.

FIG. 8. Response of Reactor with $T dk/dT = 0.0235$ to a \$100/sec Continuous Ramp Reactivity Addition without Control Rod Action.

FIG. 9. Peak Excess Reactivity on Loss of Coolant as a Function of Half-Life for Coolant Pressure and Flow Loss.

FIG. 10. Time Until Failure of Fuel Pin as a Function of Rate of Loss of Pressure and Flow.

started to rise, reaching its peak several hundred milliseconds after the control action began.

With control-rod action, the maximum allowable reactivity insertion by a ramp was somewhat larger than by a step. The reference reactor could tolerate a maximum ramp reactivity insertion of \$33.00 per second until control-rod action was initiated at 50 ms. The total reactivity insertion by the ramp was thus \$1.64. Figure 7 shows the power and temperature responses as a function of time to a \$35 per second ramp reactivity insertion with control rod action. The phenomenon of multiple fast bursts is illustrated here. The first power burst occurs when the reactivity inserted by the ramp is enough to cause the reactor to become prompt critical. The resulting swift temperature rise causes the Doppler coefficient to return the reactor to below prompt critical. The power level then drops and the temperature rises at a slower rate. The rate of reactivity removal by the Doppler coefficient becomes less than the addition rate from the ramp. This causes the reactor to become prompt critical again, initiating a second burst. After con-

trol action is taken, the temperature behaves in the same manner as for step reactivity insertions. In Fig. 7 the limiting factor was a cladding temperature above 2250°F.

Figure 8 also illustrates the phenomenon of multiple bursts. Here we are looking at the response of the reactor with a Doppler coefficient of about four times actual size to a \$100 per second ramp. The power was in the middle of its fifth burst when the fuel temperature exceeded 6500°F.

The positive coolant-density coefficient implies that a sudden reduction in coolant inventory would lead to a reactivity increase as well as a reduction in capability for heat removal. Of course, the negative Doppler coefficient reduces the net reactivity increase which would otherwise occur. In Fig. 9 the maximum reactivity occurring during a coolant-loss transient is plotted as a function of the coolant loss rate (assuming no control rod action). The minimum credible half-life for coolant loss will need further study, but it appears likely that it would be in the neighborhood of 20 sec. If so, the peak reactivity during the transient would be \$0.26 instead of the \$1.47 associated with instantaneous coolant loss.

FIG. 11. Response of Reference Reactor with Control Rod Action at One Second to Partial Depressurization and Loss of Three Blowers.

FIG. 12. Response of Reference Reactor with Control Rod Action at One Second to Complete Depressurization and Loss of Three Blowers.

Figure 10 shows a more significant aspect of coolant loss, namely, the time available for corrective action after the coolant loss is started, again assuming that no control rod action is taken. If the coolant-loss half-life is no less than 20 sec, then the time available before the fuel cladding starts to melt is about 10 sec. The implication is that a relatively slow type of emergency cooling system would be effective.

We have studied two situations in which coolant pressure and flow loss were followed by control-rod action. In the first situation we assumed pressure equalization between the primary and secondary containment vessels, which reduces the coolant pressure to 10% of normal. It was assumed also that, in this accident, three of the four blowers would stop. We allowed the pressure equalization and blower coast-down to be completed in 20 sec, and the control-rod insertion to start 1.0 sec after the start of the transient. The power and temperature responses to those conditions is shown in Fig. 11. Immediately following the scram the heat production decreased faster than

the heat removal, allowing the temperatures to fall. As the coolant flow and pressure continued to decrease, the temperatures went up again until the afterheat production fell below the final heat-removal capability of the coolant. Maximum allowable temperatures were not exceeded at any time during the transient.

In Fig. 12 we have the more severe case of the coolant pressure being reduced to atmospheric with stoppage of three blowers on a 20-sec half-life. In this instance, also, the maximum allowable temperatures were not exceeded.

Further analysis of such situations is required with particular emphasis on cases involving stoppage of all four blowers. The analysis here would involve the investigation of one or more types of emergency cooling system.

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Discussion

Mr. Morelle (Belgo-Nucleaire): I would like to comment on the rod-drop time and reactivity insertions. Many times during this meeting it has been stated or mentioned or used as an assumption that the insertion of reactivity or inception of reactivity insertion could occur in something like 50 msec. This implies that between the time that something is detected and the scram system operates after reactivity is inserted into the reactor, only 50 msec is available. I think that this number is much, much too small. A realistic time would be about 200 msec for most practical cases. Quite a few numbers presented in the meeting might be quite drastically changed if you pass from 50 to 200 msec.

Mr. Carlsmith: I don't know how drastic the change would be, and in some of the cases I don't think it would be quite as drastic as might have been implied. The prompt burst is already terminated by the time the control rods have been inserted and things are acting at a much slower rate at that time, at least in the cases I showed.

Session VII-B
October 14, 1965

Other Systems and Concepts

Chairman: K. P. COHEN
General Electric

Secretary: J. T. Madell
Argonne National Laboratory

Mixed Fuel-cycle Fast Breeder Reactors

Nuclear, Safety, and Materials Considerations

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Much of the emphasis in recent 1000-MWe fast breeder reactor studies^(1, 2) was related to reactor safety. Those aspects connected with the sodium coefficient of reactivity and loss of core sodium appeared to have a profound influence upon the various reactor configurations. To obtain a small core-sodium reactivity coefficient requires some significant compromises on nuclear performance. In particular, the core conversion ratio tends to be lower, the fissile loading in the fuel higher, and the specific power may be lower than for the corresponding system with optimum nuclear performance having a considerably larger core-sodium reactivity coefficient. Furthermore, for metal-fueled systems, the choice of a modular array, with relatively higher plutonium content in the fuel alloy than for the optimum geometric configuration, directly affects the maximum possible operating temperature.

Quite apart from these considerations, it has been reported⁽³⁾ that unmoderated U-233-Th-fueled systems generally have more negative sodium reactivity coefficients than corresponding Pu-U-238-fueled systems. Conceptually it is then possible to replace part of a Pu-U-238 fuel system with U-233-Th fuel with some advantages as regards the sodium reactivity coefficient. If this replacement is carried out in a system with reasonably optimum nuclear performance and geometry, some advantages might be realized relative to the Pu-U-238 systems that have been specifically designed to counteract a possible deleterious positive sodium reactivity coefficient.

Generally, the sodium coefficient is a rapidly varying function of position, being positive near the center of the core and becoming negative near the core boundary. The crossover point from positive to negative is a sensitive function of core volume, composition, and geometry. Nevertheless, it seems reasonable to introduce the U-233-Th fuel in those regions of the core where the Pu-U-238 fuel gives the more positive contributions to the sodium reactivity coefficients.

It is assumed desirable to have the overall system operating on the Pu-U-238 cycle. Therefore, the breeding blankets are assumed to contain primarily natural

or depleted uranium. The core will be constituted of two types of fuel subassemblies. One will be the standard type which contains Pu-U-238 fuel elements with axial segments of fertile material. The other subassembly will be similar to the standard subassembly, having essentially the same fuel element or pin height. However, the central region of unspecified height is constituted of U-233-Th fuel while the axial core extremities consist of Pu-U-238 fuel.

The core that is assembled in this manner will have a central region of U-233-Th fuel which is surrounded on all sides by a larger region of Pu-U-238 fuel. The core is reflected by a blanket of natural or depleted uranium with the necessary engineering compromises. These usually imply a lower atom density of uranium in the axial blanket than is acceptable in the radial reflector.

This overall core-assembly principle can be applied to any of those major fuel types (e.g., oxide, carbide, and metal) as well as others that are not as obvious, e.g., mixtures of ceramic and metal as well as nitride and sulfides. The simplest application assumes that whatever type of fuel is used for the Pu-U-238 material is also used in the U-233-Th region. However, the rather good irradiation stability⁽⁴⁾ of U-233-Th metal does suggest that this material may be used in conjunction with a carbide or oxide fuel bearing Pu-U-238. On the other hand, a central region of U-233-Th oxide in a metallic Pu-U-238 system has safety advantages above those provided by the acceptable sodium coefficient. This particular approach would provide a significant negative Doppler coefficient of reactivity with a magnitude that is significantly larger than that associated with the more conventional design of a metallic-fueled system.

I. Conceptual Considerations

A number of multigroup diffusion-theory calculations were carried out in spherical geometry to simulate the optimum neutronic configuration for the reactor illustrated in Fig. 1. The analyses were made with 22 energy-group constants of the ANL26 group set.⁽⁵⁾

FIG. 1. Vertical Section through Conceptual Reactor.

FIG. 2. Spatial Distribution of Sodium Worth (Carbide Reactors; C-5 Reference, C-6 with Central Zone).

The constants for the light-element materials in this set were obtained in the same manner as described in Ref. 3. A few comparative calculations with more detailed generation procedures for light-element cross sections exhibited about the same small uncertainties as indicated in Ref. 3. Calculations were also carried out to ascertain the significance, if any, on the conclusions when utilizing the recently measured plutonium fission cross sections.⁽⁶⁾ These calculations indicated small changes in numerical values but no

significant changes in the principle of the reactor concept considered here.

Most of the analyses focused upon a core volume of 3000 liters. This was chosen as typical and in the range of interest. The choice is not intended to imply any particular capability for heat removal nor are the various configurations studied here assumed to have the same thermal performance. Some ancillary studies indicated, as expected, that increasing core volume required larger central $U^{233}-Th$ regions than for the reference 3000-liter core volumes to achieve the same magnitude of the core-sodium reactivity coefficient.

Figure 2 shows the spatial distribution of sodium reactivity worth in the core of a 3000-liter carbide core. Case C-5 is the distribution for a 3000-liter $Pu-U^{238}$ system and Case C-6 for the same system with a central 800-liter core of $U^{233}-Th$ fuel. Figure 3 is a similar comparison for typical oxide-fueled fast breeder reactors. Case O-I is the $Pu-U^{238}$ system. Cases O-II and O-III are similar systems with a central 800-liter core of $U-Th$. Case O-II assumes that the central core is fueled by U^{233} only. Case O-III speculatively assumes a rather high concentration ($\sim 20\%$) of U^{234} in the uranium of the $U-Th$ fuel. This content was chosen not only to simulate U^{234} , but also to provide some pessimism in the analysis regarding uncertainties in the U^{234} cross sections. Qualitatively, Fig. 2 and Fig. 3 indicate that the magnitude of the positive sodium reactivity can be

FIG. 3. Spatial Distribution of Sodium Worth (Oxide Reactors; O-I Reference, O-II with U^{233} , and O-III with U^{233} and U^{234} Central Zone).

TABLE 1. METAL-FUELED REACTORS (3000-LITER CORES)

Case	Inner Core Volume (liters)	Core Conversion Ratio			Breeding Ratio	Core-sodium Void ($\% \Delta k/k$) ^(a)		P/A ^(d)	Core Mass Ratios		Core Fuel Material ^(e) (kg)		
		Inner	Outer	Total		Inner	Outer		Inner M^U/M^{Th}	Outer M^{Pu}/M^U	Inner	Outer	Total
1	0	—	—	1.02	1.67	—	+0.79	2.03	—	0.14	—	1410	1410
2	600	0.82	0.92	0.89	1.50	+0.14	+0.11	1.62	0.110	0.157	220	1225	1445
3	800	0.80	0.90	0.86	1.47	+0.11	-0.01	1.64	0.113	0.161	299	1146	1445
4	800	0.84	0.87	0.86	1.48	+0.16	+0.021	1.50	0.108	0.167	287	1182	1469
5	800 ^(b)	0.95	0.82	0.87	1.51	+0.27	+0.08	1.31	0.125	0.176	328	1235	1563
6	1200	0.77	0.87	0.81	1.39	+0.001	-0.15	1.71	0.117	0.167	464	970	1434
7	800 ^(c)	0.58	1.01	0.81	1.38	-0.15	-0.10	1.98	0.157	0.147	247	1061	1308

(a) 40% removal of core sodium.

(b) With U²³⁴.(c) U²³³-Th oxide.

(d) Peak-to-average power density in core.

(e) Inner ~ Uranium (233 or 233 + 234); Outer ~ Plutonium.

TABLE 2. CARBIDE-FUELED REACTORS (3000-LITER CORES; 800-LITER CENTRAL CORES)

Case	Inner Core Type ^(a)	Core Conversion Ratio			Breeding Ratio	Core-sodium Void ($\% \Delta k/k$) ^(b)		P/A ^(c)	Core Mass Ratios		Core Fuel Material ^(d) (kg)		
		Inner	Outer	Total		Inner	Outer		Inner M^U/M^{Th}	Outer M^{Pu}/M^U	Inner	Outer	Total
1	Reference Pu-U	—	—	0.97	1.49	—	+0.92	2.05	—	0.151	—	1375	1375
2	U ²³³ -Th	0.70	0.94	0.83	1.33	+0.002	+0.013	1.79	0.131	0.156	275	1037	1312
3	U ²³³ -U ²³⁴ -Th	0.84	0.84	0.84	1.38	+0.21	+0.12	1.40	0.145	0.172	302	1134	1436
4	U ²³³ -Th Metal	0.76	0.95	0.86	1.32	+0.014	+0.015	2.08	0.120	0.154	314	1027	1341

(a) Carbide unless otherwise indicated.

(b) 40% removal of core sodium.

(c) Peak-to-average power density in core.

(d) Inner ~ Uranium (233 or 233 + 234); Outer ~ Plutonium.

TABLE 3. OXIDE-FUELED REACTORS (3000-LITER CORES; 800-LITER CENTRAL CORES)

Case	Inner Core Type ^(a)	Core Conversion Ratio			Breeding Ratio	Core-sodium Void ^(b) ($\% \Delta k/k$)		P/A ^(c)	Core Mass Ratios		Core Fuel Material ^(d) (kg)		
		Inner	Outer	Total		Inner	Outer		Inner M^U/M^{Th}	Outer M^{Pu}/M^U	Inner	Outer	Total
1	Reference Pu-U	—	—	0.80	1.29	—	+0.55	2.02	—	0.261	—	1555	1555
2	U ²³³ -Th	0.56	0.85	0.70	1.14	-0.27	-0.26	2.14	0.163	0.246	256	1087	1343
3	U ²³³ -U ²³⁴ -Th	0.65	0.73	0.69	1.13	-0.03	-0.13	1.88	0.189	0.291	294	1244	1538
4	U ²³³ -U ²³⁴ -Th	0.61	0.78	0.70	1.12	-0.12	-0.17	2.18	0.203	0.271	313	1176	1489
5	U ²³³ -Th Metal	0.76	0.77	0.76	1.17	-0.28	-0.13	2.19	0.119	0.273	312	1182	1494

(a) Oxide unless otherwise indicated.

(b) 40% removal of core sodium.

(c) Peak to average power density in core.

(d) Inner ~ Uranium (233 or 233 + 234); Outer ~ Plutonium.

significantly lowered by introducing a central core of U-233-Th fuel. In fact, it is apparent that there may be a significant reactivity loss upon removal of core sodium under some conditions.

Tables 1, 2, and 3 show parameters for primarily metals, carbide-, and oxide-fueled reactors, respec-

tively. Detailed comparisons of the data in the tables are difficult since fission distributions are not always continuous across the inner-outer core boundary. However, the cited peak-to-average core power densities give some indication of the extent of effective "power flattening" that has been factored into the analyses.

Furthermore, the ceramic and metallic systems were assumed to contain fission products for an average of 5% and 2.75% burnup, respectively, consistent with the 1000-MWe^(1, 2) studies. The metallic systems all utilize the "high void" fuel elements⁽²⁾ to accommodate irradiation growth. The latter assumption may be somewhat pessimistic for the U-Th metal since its irradiation growth is believed to be somewhat less than that for the corresponding Pu-U metal fuel element. The plutonium isotopic content is generally consistent with that of the corresponding 1000-MWe studies.

In general, it can be seen from the preliminary results of Tables 1, 2, and 3 that the potentially deleterious core-sodium reactivity may be significantly altered in a favorable direction by utilizing the U-233-Th cycle over a relatively small region near the center of the core. The size of the region will depend upon basic core volume, geometry, composition, and higher isotope content in both plutonium and fissile uranium. Current uncertainties in the Pu-240 and U-234 cross sections could have significant bearing upon the ultimate configurations. The effect of the 27-day Pa-233 half-life relative to the shorter Np-239 half-life has not been taken into consideration; this will present some unique control problems. A related problem arises because the core conversion ratio of inner and outer core regions are usually different. However, as one attempts to "flatten" the power distributions radially, by decreasing and increasing fissile content in inner and outer cores, respectively, this difference tends to be minimized. Cases III and IV of Table 3 are typical of such power "flattening" effects.

Case 7 of Table 1 is the metallic core configuration that would have a significant negative Doppler coefficient of reactivity due to thorium that is larger than usually associated with a metallic core.

The breeding-ratio penalty associated with the more desirable configurations appears to not exceed 0.2 except for the Case 7 cited above. The total requirements for core fuel material (fissile plus higher isotopes) is relatively insensitive to the system complexity.

Since the conversion ratio of the U-233-Th inner core is always less than unity, makeup U-233 could be obtained by placing fertile thorium in the blankets, presumably away from the core boundary so that the low Th-232 fission cross section does not significantly alter the fertile fission fraction.

There may be distinct engineering advantages to not building a three-segment fuel element. There is no reason why the central core, including the axial extremities, could not be constituted of only U-233-Th fuel. The major disadvantages of this approach are

reduced breeding ratio and increased requirements for U-233.

It is also conceivable that U-235 fuel can be used instead of U-233 in a thorium base to obtain the same relatively small-magnitude sodium reactivity worths. This substitution would lower the initial central core conversion ratio by ~ 0.2 and aggravate the previously mentioned control problems. The initial requirements for fuel material for the central core would increase, but not quite double in this case. This approach may be a basis for gradually providing U-233 fuel in the central core.

The central core might also be constituted of U-235-U-238 or U-233-U-238 fuel. This approach would provide initial conditions that may be acceptable, but would require a larger central core than that for the corresponding U-233-Th system. The major problem here is plutonium buildup, which would tend to alter the reactivity coefficients of the coolant.

The use of the three-segment fuel element may be viewed in a number of ways. Such an element may certainly be used on a long-range basis. It may also be viewed as an interim fuel element to gain operating experience with a large fast reactor until one may confidently operate a Pu-U-238 system, presumably after some current uncertainties regarding coolant reactivity are resolved. Finally, the use of a three-segment fuel element might be a basis for reducing significantly the required number of uncoupled core modules in a given application. This could be in the design of a 1000-MWe system as well as similar systems of higher power for desalination.

II. Materials Considerations

The combination of a uranium-base and thorium-base fuel in a fuel element obviously presents a number of problems which do not exist for fuel elements containing only a single fuel material. For economic reasons it is probably most desirable to produce a fuel pin of multiple composition in a single cladding. It should, however, be mentioned that it might be entirely feasible to combine individual fuel pin sections in which each section is made from materials that are best suited for its particular function. A sodium-bonded section may therefore be neighboring a helium-bonded section of different fuel composition. The complexities introduced by such an approach may be unattractive economically, but may provide a great deal of flexibility.

To produce an integrated fuel element of multiple composition its is desirable

1. that the cladding material for both the thorium- and uranium-base fuel be the same. Preferably this should be an austenitic type of stainless steel

or a similar material of higher strength. Refractory alloy cladding materials are acceptable, but because of cost are less desirable;

2. that the outside dimensions of the cladding for all parts of the fuel pins be the same so that a constant coolant flow can be maintained in an individual channel;
3. that the attainable burnup level for the thorium core and the surrounding uranium-plutonium fuel can be matched. Since the power level in the inner zone of the reactor core is higher than in the outer zone, e.g., the axial extremities, the inner zone will reach its maximum burnup at a time when the burnup of the outer zone is still relatively low. Therefore, if both fuels are capable of attaining the same high-burnup level, the fuel element will be removed from the reactor for reprocessing long before the fuel capability of the ends has been fully utilized. The resulting waste or handling complications should be avoided. It is therefore desirable to select two fuels which simultaneously will reach their maximum burnup capability.

The fuel materials that are available for a composite fuel element are the metallic fuels, the oxides, and the carbides. For the metals and oxides there exist applicable technologies. For the carbides the technology is still in the process of development.

The following summary of the pertinent fuel properties is given solely for the purpose of providing a basis for discussion of suitable fuel, cladding, and bond combinations.

The uranium-plutonium-base metallic fuels have been evaluated by Kittel, Sebilliau and Rateick,⁽⁷⁾ and more recently by Kelman *et al.*⁽⁸⁾ and by Beck *et al.*⁽⁹⁾ The most promising metallic fuels are the uranium-plutonium-zirconium and the uranium-plutonium-titanium alloys. These alloys are superior to the uranium-plutonium-molybdenum alloys and the uranium-plutonium-fizzium alloys because of their much higher solidus temperatures. The uranium-plutonium-zirconium alloys are compatible with stainless steel and with sodium. An experimental alloy with 18.5 w/o Pu and 14.1 w/o Zr has reached a burnup of 4.2% of the fissionable atoms in CP-5 at a central metal temperature of 820°C and an outside cladding temperature of 600°C.⁽¹⁰⁾ A titanium alloy with 15 w/o plutonium and 10 w/o titanium has reached a burnup of 4.6% at a central metal temperature of 760°C and a cladding temperature of 560°C.⁽¹⁰⁾ The titanium alloys, however, are not compatible with stainless steel and require a vanadium-20% titanium alloy cladding.

The thorium-uranium alloys with up to 20% uranium are probably the best metallic fuels from a standpoint

of irradiation resistance.⁽⁴⁾ Thorium-uranium alloys with up to 31% uranium were irradiated to burnups as high as 10 a/o at the maximum irradiation temperatures of 750°C; the average rate of swelling was less than 6% volume increase per a/o burnup. Unfortunately, these alloys are not compatible with stainless steel, but they are compatible with the vanadium-titanium cladding alloys.⁽¹¹⁾

Among the ceramic materials, UO₂ has received the most extensive evaluation. The fabrication technology is well-established. Very high burnups are attainable. A good summary of our present knowledge is given by Frost.⁽¹²⁾

It is generally assumed that UO₂-PuO₂ solid solutions behave similarly to UO₂. There are still a number of unsolved questions where predictions are difficult to make. They concern the volatility of the nongaseous fission products of high vapor pressure, like tellurium, iodine, cesium, rubidium, and the alkaline earth metals, and their redistribution, compound formation, and effect on swelling. There are also the changes in stoichiometry caused by the presence of the fission products, their chemical reactions with the UO₂-PuO₂ matrix, particularly at high central fuel temperatures when melting occurs. Our knowledge of the compatibility of the oxides with sodium as a bonding material, effects such as the penetration of the sodium into the cracks of irradiated fuel pellets,⁽¹³⁾ and the effect of a high internal vapor pressure of sodium on the cladding still require evaluation. In designing an oxide fuel element it is usually assumed that all the fission gases are released and that an equivalent void volume should be provided for. Also provision must be made for the prevention of gross movement of the fuel which might cause a reactivity increase. At the present stage of development it is probably advisable not to bond with sodium but to design with a helium bond, accepting its inherently poorer heat-transfer properties.

Rabin and Ullmann⁽¹⁴⁾ have irradiated ThO₂-UO₂ pellets to burnups of 95,000 MWD/ton (2.43×10^{21} fissions/cm³). The centerline temperature was 1370°C (2500°F) and the surface cladding temperature was 94°C (200°F). The results indicate so far that ThO₂-UO₂ fuel should not be inferior to the pure UO₂ fuel.

An extensive evaluation of the U-Pu carbides was recently made by Strasser, Wheelock, and Neimark.⁽¹⁵⁾ An important variable is the carbon content of the fuel. Perfectly stoichiometric single-phase carbides are compatible with sodium. So are hypostoichiometric carbides containing the carbide phase and free metal which, however, cause problems of fuel-cladding compatibility. Sodium in contact with hyperstoichiometric material transfers carbon to the cladding and causes embrittlement. Hyperstoichiometric carbides require a

TABLE 4. POTENTIALLY USEFUL FUEL COMBINATIONS FOR MIXED FUEL-CYCLE FAST BREEDER REACTOR

Fuel Zone		Cladding	Bond
Center	Outer		
A			
Th-U	U-Pu-Zr or U-Pu-Ti	V-20 w/o Ti	Na
ThO ₂ -UO ₂	UO ₂ -PuO ₂	Stainless Steel	He
ThC-UC ^(a)	UC-PuC	Stainless Steel	He
ThC-UC ^(b)	UC-PuC	Stainless Steel	Na
B			
ThO ₂ -UO ₂	U-Pu-Zr	Stainless Steel	He
ThO ₂ -UO ₂	U-Pu-Ti	V-20 w/o Ti	He
ThC-UC ^(a)	U-Pu-Zr	Stainless Steel	He
ThC-UC ^(a)	U-Pu-Ti	V-20 w/o Ti	He
ThC-UC ^(b)	U-Pu-Zr	Stainless Steel	Na

^(a) Hyperstoichiometric.^(b) Single phase.

helium bond. The important open question, therefore, pertains to the change in stoichiometry and the effect on the stability of the desirable single-phase structure that may occur with fission product formation. The fission products may easily shift the phase relationships in the U-Pu-C system in an unfavorable sense. Recent work by Boucher and Barthelemy⁽¹⁶⁾ indicates that it might be possible to widen the stability range of the UC-PuC solid solution by TiC additions, which then would permit a variation in carbon and oxygen content (oxygen as an impurity). It would enable some control to be exercised over the effect of the fission products.

There is very little information available on the ThC-UC fuels. The ternary Th-U-C phase diagram was investigated by Benesovsky and Rudy.⁽¹⁷⁾ ThC and UC form a continuous series of solid solutions. The authors note, however, that the fcc phase rich in ThC is considerably less stable in air at room temperature than is the UC-rich solid solution. It can only be guessed that the properties of ThC-UC fuels are comparable to those of the UC-PuC fuels.

This very short resume is sufficient to permit an initial evaluation of the potentially useful fuel combinations.

As a first approach, it appears to be reasonable to adhere either to a metallic, oxide, or carbide fuel throughout each fuel pin. In such a case a Th-U metallic center section would be placed between a U-Pu-Zr or U-Pu-Ti alloy. Such a combination would permit sodium bonding and sodium cooling, but would eliminate stainless steel as a uniform cladding material for the entire fuel pin. A V-20 w/o Ti alloy cladding

would be suitable. At this time it cannot be predicted that the Th-U alloys will permit a higher burnup than the U-Pu-Zr or U-Pu-Ti alloys, since neither material has been subjected to a high-enough burnup to permit evaluation of its full potential.

If the center zone contains ThO₂-UO₂ and the outer zone UO₂-PuO₂, stainless steel can be used as a cladding material for both zones. Both would require helium bonding. At this time it must be assumed that the burnup capabilities for both materials are the same and that, therefore, the material in the outer zone will not be fully utilized when the fuel pins are removed for reprocessing.

Similar conditions apply to the carbides if hyperstoichiometry should prevail throughout the irradiation. Stainless steel cladding and a helium bond are preferred. Again, at the time of maximum burnup of the center zone the material in the outer zone would not be fully utilized. Similar considerations apply to single-phase and stoichiometric carbides, for which a sodium bond might be feasible. These cases are summarized in Table 4A.

To satisfy the condition of a burnup capability that is higher in the center than in the outer zone, the combinations listed in Table 4B are potentially useful. In these cases the outer zone has a U-Pu-Zr or a U-Pu-Ti metal fuel, the center zone either an oxide or a carbide fuel. Stainless steel can be used as a cladding material for the U-Pu-Zr alloy, but a helium bond would have to be used throughout unless complications are permitted by use of a different bonding material for each zone. With a U-Pu-Ti alloy in the outer zone the V-20 w/o Ti alloy would be the cladding. If, on the other hand, the stoichiometry of the ThC-UC fuel can be controlled well enough to permit sodium bonding, the last of the combinations listed in Table 4B would become very attractive.

Other possible combinations would include a thorium-uranium metallic center zone together with an oxide or carbide outer zone. This is probably the least desirable combination as can readily be seen from the bonding and cladding and burnup requirements.

One other potential combination which cannot be ruled out entirely is an oxide center zone with a carbide outer zone, or vice versa, a carbide center zone with an oxide outer zone, since well-matching burnup capabilities might be obtainable in such a way. A much more detailed knowledge of the respective burnup capabilities is required than is presently available even from our most advanced studies, to evaluate such a combination.

For all of these reasons the combinations listed in Table 4 are those that, with the present technology, meet most closely the requirements of a fuel element

for a power breeder reactor in which a thorium-uranium center zone is surrounded by a uranium-plutonium outer zone.

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Discussion

Mr. Sesonke (Purdue): Do you have any fear of

of possible complications in fuel management and fuel cycle because of these different isotopic arrangements?

Mr. Loewenstein: I think it's quite obvious that there will have to be two separate streams of fabrication or reprocessing. Whether or not one can put these two types of fuel elements in the same clad or whether one has to provide three separate axial fuel elements is not clear at this time. I don't mean to be facetious, but I think this is an engineering detail at this point.

Mr. Sesonke: I was thinking more of possible economic penalties.

Mr. Loewenstein: I am sure that there are obvious economic penalties, but I think there are also some economic advantages depending on whether you compare big apples with little apples. For example, here is a way to build a system with a trivial sodium coefficient of reactivity and in some cases with an enhanced Doppler coefficient of reactivity with a slightly higher core conversion ratio and, generally speaking, a significantly higher total breeding ratio than some of the systems that have been specifically designed to overcome the positive sodium coefficient.

Mr. Heck (WAPD): I am curious if you considered the possibility of simply using a radially zoned loaded core with the thorium-U-233 in the central region to avoid the possibility of having the multiple rods axially. In a cylindrical core you might be able to achieve somewhat the same effect by loading the whole central fuel, all the central fuel assemblies with thorium-U-233, and then put plutonium normal fuel assemblies around it instead of having to split assemblies.

Mr. Loewenstein: Yes, we have looked at this, but we believed that there was a premium paid to try to utilize plutonium-U-238 as much as possible, because this seems to have been the ground rule for the studies of large systems. The U-233-thorium effect was used only as a tool here. Certainly if one did what you suggest the core conversion ratio would decrease, the total breeding ratio would decrease, and the sodium coefficient, generally speaking, would be slightly more negative, but not much more.

Mr. Weiner (USAEC, Washington): Did I understand you to say that you got the same sodium voiding effect with U-235 at the beginning of life?

Mr. Loewenstein: I did not do the calculations specifically for this system, but we have a number of calculations of U-233-thorium systems compared with U-235-thorium systems. Generally speaking, they exhibit about the same sodium reactivity coefficients.

Mr. Weiner: Then wouldn't it be worthwhile to examine the possibility of using U-235 in the central zone? I would expect that you would get some worse sodium voiding effects towards the end of life, but I am wondering how much worse.

Mr. Loewenstein: Again the question comes up, what are you comparing: clean U-235 or U-235 that has U-236 in it? Generally speaking, I think you are probably right that U-235 would aggravate the situation slightly, but would not begin to approach the problem with plutonium.

Mr. Weiner: Then wouldn't this be a more economical way of getting the effect you are looking for rather than by the use of U-233.

Mr. Loewenstein: It might very well be, except that in these studies we were primarily concerned with having something that would be self-regenerative. In other words, you put enough thorium in the blankets to make up the U-233 that you needed in the core, whereas it's kind of hard to breed U-235. Your comments are quite valid if one has a lot of U-235.

Mr. Cohen (GE): You showed the core breeding

ratios themselves to be somewhat less than one. Have you investigated the breeding ratio of the thorium *per se*?

Mr. Loewenstein: In the oxide system, offhand, the regeneration of the thorium region by itself is around 0.55. In the carbide and in metal it would be somewhat higher. I would guess about 0.7 in the thorium system, whereas the carbide conversion ratio of just the plutonium-U-238 is around 0.9. When you average over the whole core, you obtain something around 0.75.

Mr. Cohen: That number would suggest, of course, that you would have a reactivity swing to deal with.

Mr. Loewenstein: Yes. I think the fact that you have a three-day half-life of neptunium and a thirty-day half-life in protactinium will present a real problem to the control people.

Nuclear Design of MPBE*

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Introduction

A conceptual design of a large molten plutonium fast breeder reactor has previously been described.⁽¹⁾ Although the general performance of such a system can be expected to be quite favorable, there are a number of areas in which there are inadequate bases for defining performance limits. One such major area is the burnup limit for molten plutonium fuel. In the present paper, we will describe the design of a Molten Plutonium Burnup Experiment (MPBE) which will be used to determine the properties of a molten plutonium fuel during and after high burnup.

We first give a very brief review of the large reactor design concept. Then, some of the relevant parameters of the Fast Reactor Core Test Facility (FRCTF),⁽²⁾ in which this experiment will be done, are given. In the context set by the concept and by the facility, we then consider the criteria used in the design of the MPBE. Some of the expected performance features of this core are presented.

In a related paper,⁽³⁾ the materials program and the results of experimental work relative to the molten plutonium program have been considered. In another paper,⁽⁴⁾ one of the several unique safety aspects of a molten fuel has been discussed.

I. Design Concept

The reactor concept⁽¹⁾ is illustrated schematically in Fig. 1. The general configuration is an array of reactor modules, containing a sufficient number of modules to produce 500–1500 MW(e) (typically 10–30 modules).

It is assumed that the power level of the core is limited by the available heat transfer area. For a capsule core, this assumption—without reference to what the limiting value is—is adequate to determine a ratio of plutonium density in the fuel to container area which is optimum with respect to doubling time.⁽⁵⁾ As an example, with tantalum capsules of the order of a 1-cm diameter and a 0.05-cm wall, the optimum plutonium density is in the range of 3–6 g Pu/cc fuel.

A specific and realistic case based on the current best

estimates for limiting factors yields the parameters given in Table 1.

II. Facility

The MPBE will be operated in the FRCTF, which will provide an environment in which a sequence of fast cores may be tested. As with any facility, however, there are certain limits which are relevant to the design of experiments. There are three factors which are potentially limiting, as shown in Table 2.

This facility is presently under construction. The building is nearing completion, and the present schedule for installation of equipment is directed toward initial operation of MPBE in 1968.

III. Design Criteria

A prime consideration in the design of MPBE, as for any experiment, is safety. Within this constraint, the intent is to investigate areas which strongly affect the economics of a large system and for which substantial uncertainty exists.

The unique and impressive safety features resulting from the use of a molten metal fuel (such as large expansion coefficient, negative sodium void effect, and potential for low fission product inventory) have been discussed elsewhere.⁽⁶⁾

Two fuel-cycle costs may be singled out, which are common to all reactors and which are frequently the dominant factors: fuel charges (burnup or buy-back, plus use charge), and fabrication charges. The fuel charges are related to the system doubling time. Fabrication charges are related to total burnup capability of the fuel elements (per unit fabrication cost).

Lifetime (burnup) capability for most fast reactor fuel elements is determined by structural changes of the fuel. With a molten fuel, this is not likely to be an early limit, for no structural damage to the fuel itself can occur. Consideration of reactivity lifetime can (conceptually) be avoided. The burnup limit is thus likely to be determined by fuel-containment life, by gross chemical fuel changes due to fission products, or by economics.

One other point relative to the lifetime of a fuel element should be mentioned. No matter what the con-

* Work performed under the auspices of the U. S. Atomic Energy Commission.

FIG. 1. Conceptual Layout of Large Modular Core.

TABLE 1. ILLUSTRATIVE FACTORS FOR A HEAT-TRANSFER-LIMITED LARGE CORE

Doubling Time (Core Inventory)	7 yr
Breeding Ratio (at Beginning of Life)	1.5
Specific Power (Core Inventory)	0.9 MW/kg Pu
Power Density	4.0 MW/liter fuel
Loading/Module	120.0 kg Pu
Power/Module	110.0 MW(th)

TABLE 2. SOME FRCTF OPERATING LIMITS

Heat Dump and IHX Capacity	20 MW(th)
Max Flow of Sodium	132 kg/sec
Max Mixed Mean Outlet Temperature of Sodium	650°C

cept, it is necessary to hold a fluid fuel in some type of container, which may or may not be a heat transfer medium. In principle, the use of a fluid fuel permits the changing of fuel without the replacement of the container. However, the container will also have a finite lifetime. The design choice as to whether to refill

or to replace containers must consider the lifetime of the container as compared with that of the fuel. For a molten plutonium system of the type we are considering, it is not clear whether the lifetime capability of the fuel or of the container (if either) will be limiting. Thus, an ultimate decision as to whether to pursue a plumbed or a fixed fluid concept would be premature at this stage. It could be that the ability to handle fertile blanket materials in a fluid form will be a more significant advance.

The present design uses sealed capsules, but is also compatible with the use of vented capsules. The selection of this type of fuel containment for the present experiment has several distinct practical advantages: the technology developed for solid fuel in thermal and mechanical designs is available and useful; the performance limits are more readily defined; a failure of containment has limited and tolerable consequences; and the design is well suited to collection of data on properties of materials as a function of burnup. In addition, actual successful operational experience on such a core exists (LAMPRE I).⁽⁷⁾

The principal purpose of the Molten Plutonium Burnup Experiment is to extend the available data on the properties of the fuel and, to some degree, on the properties of tantalum containers, during and after high burnup. Upon completion of this experiment, it may be feasible to define realistic burnup limits.

IV. Selection of Core Design Parameters

The performance potential of a molten plutonium system will clearly be affected by burnup in many ways. For example,

- (a) The change in plutonium content by burnup and by accumulation of soluble fission products will change the physical character of the fuel (melting point, etc.).
- (b) The presence of gross amounts of fission products in the fuel may affect the integrity of the containment.
- (c) The volatile fission products will cause a pressure buildup in sealed capsules.
- (d) The rate of gas generation may affect the equilibrium dilution (bubble content) of the fuel (the detailed character of this dilution at high burnup is not fully known).
- (e) The presence of insoluble fission products may result in mechanical changes to fuel performance (e.g., interference with natural circulation of the fuel).
- (f) The burnup of plutonium and dilution of the fuel by fission products will determine the required reactivity compensation.

With regard to these considerations, we may inquire as to the parameters which are to be optimized in the design of the experiment. It may be noted that power is fission or burnup rate, specific power is fractional burnup rate, and power density is burnup rate per unit volume. Fractional burnup, or integrated specific power, is a reasonable measure of flux time (nvt).

The change in neutronic properties is likely to be described in terms of fractional burnup or integrated specific power. Likewise, if neutron irradiation damage to the container is limiting, integrated specific power is the factor of interest. Gross fission product content, however, represents integrated power density. Thus, fuel changes are more likely to be characterized by this parameter. Further, container damage from fission product recoil is measured in terms of integrated fission or power density. To the extent that there are reasons to differentiate between power density and specific power, the emphasis in the design is on obtaining a high power density. This separation relies on the assumption that plutonium density in the fuel and the effects of burn up are not strongly related; an assumption which will be verified by selected special tests.

V. Core Design Model and Calculational Results

In the scoping studies, preliminary design studies, and nuclear design calculations described here, the two-dimensional S_n code DDK⁽⁸⁾ was used. The use of two-dimensional calculations at this stage is feasible only if the calculational model is held to modest size, both in detail and in extent. The availability of the DPC code,⁽⁹⁾ however, has permitted the accounting for a considerable amount of detail in determining the material content of various regions in the design. The core cross section is shown in Fig. 2. Surrounding the region shown will be a reflector-blanket region. The initial operation (MPBE) will utilize a nickel reflector, with perhaps some special experiments in the reflector region.

In the calculations, the compositions of control segments are taken to include all of the material between the two structural members shown. Two control-segment compositions are considered: that with the control segment being nickel and that with tantalum. In both cases, an extra 10% of the total region volume is assumed to be sodium for internal cooling of the control segments.

The core sleeve and core-sleeve liner are solid 304 stainless steel.

All core regions are constructed of stainless steel module cans in sodium, each containing seven tantalum capsules. Two distinct types of core compositions are used: *fueled regions* and *gas-space regions*, representing the fueled and the void portions of the capsules. Figure 3 shows the cross section and dimensions of core modules.

In the core area are both fueled and reflector modules. The reflector compositions may further be separated into *axial reflectors* (physically part of the fueled-core modules), *radial reflector modules*, and *fueled-control followers* (upper sections of fueled-control clusters). All modules have the same configuration as shown in Fig. 3, with the following exceptions. Rather than capsules, each contains seven solid rods. For axial reflectors, these rods are of nickel, with the same diameter as the core capsules. For fueled-control followers, the rods are of solid tantalum, with the same diameter as the core capsules. The radial reflector rods are solid nickel of 0.500-in. diameter.

The core design is compatible with fuel of either 6.2 or 8.0 g Pu/cc. In addition, some margin in size is required for several purposes. Experimental objectives at the time of operations could dictate an increase or a decrease in the density of the plutonium in the fuel. Beyond this, since this is a new fuel, a substantial margin for analytical uncertainty is advisable. Further, compensation for depletion by increasing size may be desired. Thus, a wide range of loadings may be of

FIG. 2. Cross Section of MPBE Core.

interest. Table 3 shows the estimated size and loading for a few cores.

Cases 92 and 95 of Table 3 are considered to be the reference cases. Case 98 represents a hot, critical case with the 6.2 g Pu/cc fuel. Case 10 is included as a measure of the minimum fuel density which might be tolerable in this core geometry without severely compromising the available gas space.

Control Worths

Table 4 shows the calculated reactivity and reactivity swing for various control configurations.

For the 6.2 g Pu/cc, 530°C cases, the following observations may be made: The total control swing (11.3% Δk) and the fueled control swing (7.3% Δk) both appear to be ample. The worth of the inert control segments is slightly less than might be desired (4.6%)

FIG. 3. Core Module of MPBE.

TABLE 3. CRITICAL SIZE AND LOADING

Case	Fuel (g Pu/cc)	Temp (°C)	Fraction of Control Up		Loading		
			Fuel	Inert	No. of Mod- ules	Height (in.)	kg Pu
92	6.2	530	2/3	1/2	91	13.3	95.8
98	6.2	530	3/3	2/2	91	11.7	84.7
09	6.2	160	3/3	2/2	91	11.3	81.4
95	8.0	530	2/3	1/2	91	8.4	79.3
10	5.0	530	2/3	1/2	121	16.1	124.2

but should be fully adequate. Some small interaction among various control motions may be inferred from the fact that the worths of fueled and of inert control do not add up to the combined worth. Part of this difference may be associated with the relatively loose convergence criteria used for these calculations.

The control worths at 160°C are seen to be roughly comparable to those at 530°C.

For the 8 g Pu/cc fuel core, some loss in control worth may be noted. The fueled-control worth is not greatly reduced, in spite of the shortened stroke relative to that of the 6.2 g Pu/cc fuel case. The inert control is of somewhat more concern. This reduced worth is such as to suggest the possibility that different modes of operation may be required for the 8 g Pu/cc fuel case than for the 6.2 g Pu/cc fuel case, with somewhat greater reliance being placed on the fueled control.

Reactivity Coefficients

A few of the basic reactivity changes associated with normal changes in composition are given in Table 5. A considerable extension of this table, both from displacement and from perturbation calculations, is underway. It will be noted that none of these coefficients indicates serious difficulties.

TABLE 4. CONTROL-WORTH CALCULATIONS

Case	Fuel (g Pu/cc)	Temp (°C)	Fraction Control Up		k	Δk (%)
			Fuel	Inert		
92	6.2	530	2/3	1/2	0.995	—
99A			0/3	0/2	0.924	-7.1
98			3/3	2/2	1.037	+4.2
99			0/3	1/2	0.946	-4.9
98			3/3	1/2	1.019	+2.4
94	8.0	530	2/3	0/2	0.971	-2.5
93			2/3	2/2	1.016	+2.1
02			2/3	1/2	1.008	—
04			0/3	0/2	0.936	-7.2
09			3/3	2/2	1.043	+3.5
95			2/3	1/2	0.991	—
11			0/3	0/2	0.933	-5.8
12			3/3	2/2	1.026	+3.5
97			2/3	0/2	0.973	-1.8
96			2/3	2/2	1.006	+1.5

TABLE 5. REACTIVITY WORTHS OF CERTAIN CHANGES

Cases	Fuel, g Pu/cc	Change	Inferred Coefficient
92-00	6.2	Isothermal ΔT	$-65 \times 10^{-6} \Delta k/^{\circ}\text{C}$
92-13		Fuel-expansion	$-3.6 \times 10^{-3} \Delta k/\%$
			$= -34 \times 10^{-6} \Delta k/^{\circ}\text{C}$
92-05		Sodium Removal	$-4.7 \times 10^{-4} \Delta k/\%$
			(uniform)
92-02		All except fuel ex- panded	$-34 \times 10^{-6} \Delta k/^{\circ}\text{C}$
92, 04, 09, 98	8.0	Change in fuel height (con- stant density)	$+0.33 \Delta k/(\Delta H/H)$
02-92		Temperature de- fect	$-1.3\% \Delta k$
92-13		Power defect (100°C)	$-0.4\% \Delta k$
95-01 95		Isothermal ΔT	$-66 \times 10^{-6} \Delta k/^{\circ}\text{C}$
		Change in fuel height (con- stant density)	$+0.30 \Delta k/(\Delta H/H)$

Summary

The general prospect for the use of molten plutonium as a fast breeder fuel is favorable, but several major extrapolations are involved in such assessments. A particular case, for example, has power density, ~ 4 MW/liter fuel; specific power, ~ 1 MW/kg Pu; with burnup capability assumed to be $\lesssim 10\%$. None of these have been suitably demonstrated. The MPBE will investigate this range of burnup, with high power densities (1.3–2 MW/liter fuel). The design presented

TABLE 6. SUMMARY OF SOME PROPERTIES OF THE REFERENCE CORES

	g Pu/cc Fuel	
	6.2	8.0
Operational Loading (kg Pu)	95.8	81.5
Specific Power (kW/kg)	210.0	245.0
Power Density (MW/liter fuel)	1.3	2.0
Minimum Critical Loading (kg)	84.7	—
Available Total Control Swing ($\% \Delta k$)	11.3	9.3
Available Fueled-control Swing ($\% \Delta k$)	4.6	3.3
Power (Peak to Average)	1.46	1.43
Medium Energy of Neutrons Causing Fission (MeV)	0.65	0.69
Medium Flux Energy (MeV)	0.55	0.61
Average Flux ($n/\text{cm}^2\text{-sec}$)	1.7×10^{15}	2.0×10^{15}

appears to have adequate control, very favorable reactivity coefficients, and conservative thermal design.

A summary of some of the more important properties of this core is given in Table 6.

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Discussion

Mr. Leitz (GE): In earlier reports on the molten plutonium system when the circulation of the molten fuel through the core was considered, it was observed that the generation of fission gases could be a limit to fuel lifetime or, perhaps better, core-residence time; the concern was less one of just simple dilution of the fuel than the fact that the gases could interfere with

heat transfer from the fuel to the coolant. I was wondering if you developed a solution to this problem or if there is some reason, which isn't immediately obvious to me, why the sealed-pin molten plutonium concept would not be subject to this difficulty of fission gases, particularly those which might be held up on the interior of the cladding interfering with the heat transfer.

Mr. Hannum: It is expected that the bulk of the fission gas bubbles which have not yet been released from the fuel will, in fact, imply only that the fuel temperature will be higher than it normally is. In this concept the limiting temperature is not the centerline of the fuel; we would be much happier to run it much hotter. The limiting criterion is that the corrosion is worse at high temperatures. We worry about the interface temperature between the plutonium and the container, and that should not be affected by bubbles.

Mr. Bartels (USAEC, Washington): If you take the core of the Fast Core Test Facility as being reactivity limited, what would your fuel lifetime be?

M. Hannum: The major factor which changes the reactivity is simply the retention of fission gas bubbles within the fuel diluting the fuel, and thereby decreasing the reactivity. We expect to attain several percent depletion between partial refuelings even if a major fraction of the bubbles are retained. There is some preliminary indication from thermal reactor radiations that the dilution of fuel by bubbles saturates very quickly at about the amount it did in LAMPRE-I, namely, about 5 v/o of the fuel, in which case we would take an initial reactivity loss and after that the reactivity loss would be very slow.

Mr. Chernick (BNL): Can you tell us what your experience, if any, has been with single elements on reactivity swings you might get with gas foaming, and the changes in density during operation?

Mr. Hannum: Nothing. No short-term reactivity changes have been detectable that can be associated with release of fission gases. If there is anything, it is a matter of a fraction of a cent. The bubbles come off continuously or in small quanta.

Faret Core-I Reference Design

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The Fast Reactor Test Facility (FARET) being designed at Argonne is described in the literature.⁽¹⁻³⁾ Briefly, however, the FARET facility has a heat-removal capacity of 50 MWt and a mean outlet coolant temperature of up to 650°C. It can accommodate a variety of core configurations, coolant flow rates, and core sizes ranging from less than 40 liters to about 500 liters. The smaller cores would be used for high-power-density experiments in the order of 1.5 MW/liter. The facility design includes provisions for the instrumentation of in-core components, and it can accommodate re-entrant loops for irradiation studies such as vented fuel elements, corrosion studies, or new fuel concepts.

Introduction

The purpose of this paper is to present the fuel-irradiation program and the reference core planned for the first experiment in the FARET facility. The objective of the experiment is to test fast reactor fuel elements over a range of operating conditions and design specifications associated with large fast power breeders. It is planned to study simultaneously the performance of several different designs of ceramic- and metal-fuel assemblies in a small (70-liter) core. With the small core, the design and geometry of a prototype reactor can be simulated, but the fuel enrichment will be different. The plutonium content in the mixed fuel is made similar to that of the large reactor, with U^{235} replacing U^{238} to achieve criticality. By selection of core position and/or enrichment, the fuel assemblies can then be tested over the desired range of operating conditions.

The basis for utilizing this enrichment technique is that the substitution of U^{235} for U^{238} would not alter the metallurgical or physical properties of the fuels, at least to a first-order approximation. The U^{235} -enriched uranium-plutonium system is chemically identical to a nonenriched uranium-plutonium system.

This technique is to be compared with the method in which a nonenriched uranium-plutonium system is irradiated in a higher neutron-flux environment to achieve the same fission rate or heavy atom burnup.

Phenomena such as diffusion or segregation of atoms which depend on temperature gradients are expected to differ, if at all, only to second order between the two fuel systems.⁽⁴⁾ Fission product yields for U^{235} and Pu^{239} reported for thermal neutrons⁽⁵⁾ and estimates for fast neutrons⁽⁶⁾ indicate that the total yield of the more prevalent gaseous fission products would be similar to within 10% for the enriched and nonenriched fuels. Differences in the fission-gas release rate and fission product migration are not expected in solid-solution fuels.

The advantages of the enrichment technique as used in FARET are:

1. Many different fuel element designs may be tested simultaneously in a flexible and accessible small-core loading.
2. The hard neutron spectrum and the magnitude of the flux in FARET would subject the clad material to neutron dose rates comparable to those that would be obtained in the typical and somewhat softer spectrum of a large power reactor at the same fuel ratings.
3. The fuel inventory is determined only by the number of test assemblies required in the experiment.
4. The flexibility in core size allows generating experimental data at accelerated rates of heavy atom burnup.
5. The frequency of adding or replacing fuel elements is determined only by the experimental requirements of these same test elements.
6. Finally, and most important, along with these low fuel loadings is the advantage of needing only a low-heat-removal system.

Basic Core Plan and Test Conditions

The FARET Core I reference design assumes that as the subassemblies are removed for inspection and destructive testing, they will be replaced with fresh subassemblies of the same design. This need not be the case. Improved fuel-element designs resulting from the continuing fast reactor fuel-development program can be inserted into the core whenever available, replacing types showing a reduced performance

TABLE 1. FUEL-ELEMENT DATA

Fuel Material	UO ₂ -15 w/o PuO ₂	UC-15 w/o	PuC Metal ^(b)
Elements per Subassembly	37	37	61
Fuel Height, in.	18	18	18
Clad Outside Dia, in.	0.290	0.290	0.220
Clad Thickness, in.	0.020	0.020 ^(a)	0.016
Spacer Wire Dia, in.	0.057	0.057	0.052
Axial Blanket Height (each end), in.	6	6	6
Gas Reservoir, in.	25	25	25
Element Height, in.	55	55	55
Effective Fuel Density, %	80	80	70
Bond Material	Helium	Helium	Sodium
Fuel U/Pu Atom Ratio	6/1	6/1	5/1
Fuel Details	Coprecipitated; O/M 1.97-2.00	Single Phase; Solid Solution	—

^(a) 0.015 in. in C4 subassemblies.

^(b) U-15 w/o Pu-6.5 w/o Ti; U-15 w/o Pu-10 w/o Zr.

capability. Further, should some originally fabricated designs prove to be of less interest in the early stages of the FARET experiment, they would be replaced by additional subassemblies of other designs having greater potential. Extra subassemblies of several types will be provided in the original fuel inventory. The physics of the core permits such flexibility.

The reference core will consist of carbide, metal, and oxide fuels and will test simultaneously ten different fuel element designs with good statistics. The fuel-element design data are presented in Table 1. The elements have a 6-in. axial blanket at each end, with a 25-in. fission-gas reservoir to allow for 15 a/o burnup, if design and fuel performance permits. The ceramic fuels are helium gas-bonded. A conceptual instrumented fuel subassembly is illustrated in Fig. 1.

The fuel-element types and designs and the tentative test conditions are presented in Table 2. The elements are typed according to fuel material, fuel fabrication method, clad material, and thermal and power conditions. The reference core contains six mixed-oxide types, four mixed-carbide types, two metal alloy types, and three subassemblies for special experiments, if desired, making an overall total of 51 subassemblies. Two of the oxide designs will be run at two different linear heat-power ratings, and hence different maximum fuel and cladding temperatures. Each subassembly type is to be tested up to about 15 heavy a/o burnup, if design permits. The elements are to be destructively tested at predetermined intervals of burnup. The suggested periods of approximately 5, 9, 12, and 14 or 15 a/o reflect the current confidence of the metallurgists that most of these elements will be able to achieve at least the first two burnup periods, say up to 9 a/o. Also reflected in the frequency of inspection is the attempt to maximize

full-power operating time. As will be subsequently discussed, other important constraints on the frequency of testing and irradiation intervals are the maintaining of criticality and the worth of the control system.

The core composition and configuration designed to approach the thermal conditions outlined are presented in Fig. 2 and Table 3. The overall plan view of the core loading (see Fig. 2) includes the placement of the control rods in the stainless steel reflector, and the core edge and reflector loops. The preliminary placement of some of the instrumented assemblies and the manner in which the cables may be laid out are also shown. The nomenclature, 056I, for a subassembly is to be read as 05 type oxide, removal at the end of the sixth irradiation period, instrumented.

Several conditions were considered in the positioning of the subassemblies. Symmetry was maintained to subject the elements of each type to similar power conditions throughout the experiment. The positioning was also influenced by the sequence of removal and replacement of the subassemblies. The instrumented assemblies are located so that the loading and unloading of other assemblies are not made cumbersome by having instrument leads located over subassemblies to be removed. For example, the instrument lead from the C4SI assembly is passed over subassemblies which are scheduled to be removed after the C4SI.

It is planned to remove the instrumented subassemblies in the latter stages of burnup for each type of element. This makes possible the continual recording of fuel and coolant temperature, and possibly of coolant flow rates, neutron flux, and fission gas release throughout most of the core life of each fuel element design.

In addition to measuring and recording engineering

FIG 1 Instrumented Core Subassembly

TABLE 2. SUBASSEMBLY DATA

Fuel Material	Sub-assembly Designation	Fuel Type	Clad Material	Number of Sub-assemblies	Instrumented Sub-assemblies	Max Linear Heat Flux (Design), kW/ft	Max Clad Temp, °F(°C)	Max Fuel Temp, °F(°C)	Burnup ^(a) at Removal a/o	Max Clad Thermal Stress, psi	Coolant Velocity, ft/sec
Oxide	O1	Pellets	304	4	1	15	1250 (675)	3220 (1770)	5, 9, 12, 14	13,300	16
	O2	Pellets	304	4	1	22	1290 (700)	4220 (2330)	5, 8, 10, 12	19,400	23
	O3	Vipak	304	4	1	15	1250 (675)	3220 (1770)	5, 9, 12, 14	13,300	16
	O4	Vipak	304	4	1	22	1290 (700)	4220 (2330)	5, 8, 10, 12	19,400	23
	O5	Pellets	HSA	4	1	22	1340 (725)	4250 (2340)	5, 9, 12, 15	—	20
	O6	Vipak	HSA	4	1	22	1340 (725)	4250 (2340)	5, 9, 12, 15	—	20
Carbide	C1	Pellets	304	4	1	15	1250 (675)	2030 (1110)	5, 9, 12, 14	13,300	16
	C2	Vipak	304	4	1	22	1290 (700)	2520 (1380)	5, 9, 12, 14	19,400	23
	C3	Pellets	HSA	4	1	29	1340 (725)	3050 (1680)	5, 9, 12, 15	—	30
	C4	Vipak	HSA	4	1	36	1340 (725)	3390 (1870)	5, 9, 12, 15	—	39
Metal	M1	U-Pu-Zr	HSA	4	1	14	1250 (675)	1580 (860)	5, 9, 12, 14	—	20
	M2	U-Pi-Ti	V-Ti	4	1	14	1250 (675)	1580 (860)	5, 9, 12, 14	—	20
Advanced	C4S	—	—	3	3						
				51	15						

Notes: 1. Vipak means "vibratory compacted;" HSA means "high strength alloy," not necessarily the same for oxide as for carbide; etc.

2. Coolant inlet temp is 810°F. Average outlet temp from each subassembly is 1050°F. (Exception: outlet temp from 05 and 06 subassemblies is 1085°F.) Axial peaking factor = 1.2.

3. Max clad temp include both a 1.33 factor for flow and power maldistribution effects on coolant temp rise, and a >1.10 uncertainty factor. Max fuel temp include the 1.33 factor but no uncertainty factor.

^(a) Approximate burnup intervals.

FIG. 2. FARET Core I.

TABLE 3. PERFORMANCE PARAMETERS OF FARET CORE I. REACTOR POWER: 50 MW(1)

Core Composition (%): Ceramic Fuel 37/Sodium 40/Structure 22;
 U/Pu Atom Ratio 6/1
 Metallic Fuel 30/Sodium 48/Structure 22;
 U/Pu Atom Ratio 5/1

Core Volume: 70 Liters
 Height: 45.7 cm (18 inches)

Row and No. of Subassemblies	Fuel Type	Approx U-235 Enrichment, %	No. of Elements per Subassembly (Dia in in.)	Average Power per Sub-assembly, MW	Power Density per Sub-assembly, MW/l	Avg (Max) Linear Power, kW/ft	% Heavy Atom Burnup (180 Days at Full Power)
(1) 1	C4	0.67	37 (0.29)	1.5	1.1	27 (31)	5
(2) 6	C4	0.67	37	1.45	1.06	26 (31)	4.9
(3) 4	O4	0.60	37	0.88	0.64	15 (19)	3.9
4	O6	0.60	37	0.88	0.64	15 (20)	3.9
4	C3	0.60	37	1.18	0.88	21 (27)	3.9
(4) 4	O2	0.65	37	0.84	0.61	15 (20)	3.6
4	O5	0.65	37	0.84	0.61	15 (20)	3.6
4	C2	0.67	37	1.01	0.73	18 (23)	3.6
4	M1	0.57	61 (0.22)	0.97	0.71	10 (13)	2.7
2	M2	0.57	61	1.0	0.73	10 (13)	3
(5) 4	O1	0.65	37 (0.29)	0.72	0.53	13 (16)	3.3
4	O3	0.60	37	0.69	0.50	12 (15)	3.2
4	C1	0.60	37	0.85	0.62	15 (19)	2.7
2	M2	0.57	61 (0.22)	0.84	0.61	9 (12)	2.4

data, the instrumentation has now added a dimension of safety to the fuel-irradiation experiment. Fuel-element failure can probably be anticipated by the continuous surveillance of these instrument readings.

In Table 3 are listed the enrichments and linear power rating for the loading shown in Fig. 2. A condition imposed on the obvious flexibility of enrichment was that the uranium enrichment be specified and held fixed for each method of fuel fabrication. Thus, the C1 and C3 pellets have the same enrichment, although the clad materials and thermal conditions are different.

The uranium enrichments also include the effect of control rods on the power-density distribution. The power density in the carbide C1 assembly may still be higher than that specified in the test conditions. If this is the case, the C1 elements may be moved into the "corner" positions of Row 5. Computations indicate that about a 5% variation in the power densities can be expected between these two positions.

Considerations in Planning the Experimental Program

In planning the experimental program an important factor in establishing the sequence of fuel removal and replacement was the total worth of the control

system. The optimum experiment with respect to time would be one in which the excess reactivity needed for the maximum burnup is accommodated by the initial fuel enrichment. The approach to the optimum experiment then is to design the control system with the maximum worth.

In Table 4 are listed the calculated nuclear and control parameters for the reference core. The total neutron flux ranges from about 2 to 4×10^{15} n/cm²-sec over the core. The percentage of total neutrons having energies above 1.35 MeV at the center assembly and in the outer row are 22% and 13%, respectively. As will be discussed later, the quality of the spectrum may be an important factor in the study of radiation damage to cladding material.

Also listed are the expansional reactivity coefficients. The signs are all negative, and the magnitudes are of the same order as those computed for similar sized but uniformly loaded cores. The total worth of the 12 boron carbide control rods are estimated to range between 7.5% and up to almost 10%.

Included in Table 4 are the reactivity effects due to the expansion of coolant, structure, and fuel systems for the anticipated operational temperature change. If a range of excess reactivity of 3.5% to 6% for burnup, along with a 2% shutdown margin and the

TABLE 4. CALCULATED NUCLEAR AND CONTROL PARAMETERS FOR REFERENCE DESIGN OF FARET CORE I

Nuclear Data: Neutron Flux, $n/\text{cm}^2\text{-sec}$			
Energy	Radial Position		
	Core Center	Core Edge Loop	Reflector Loop
$>1.35 \text{ MeV}$	0.8×10^{15}	0.3×10^{15}	0.3×10^{14}
$100 \text{ keV} < 1.35 \text{ MeV}$	2.4×10^{15}	1.5×10^{15}	5.7×10^{14}
$1 \text{ keV} < 100 \text{ keV}$	0.4×10^{15}	0.5×10^{15}	3.9×10^{14}
$<1 \text{ keV}$	6.5×10^{11}	1.8×10^{13}	1.2×10^{14}
Total	3.6×10^{15}	2.3×10^{15}	1.1×10^{15}
Prompt-neutron Lifetime, μsec			0.21
Effective Delayed-neutron Fraction β			0.0058
Reactor Control: Average Reactivity Coefficients, $\Delta k/\Delta T(^{\circ}\text{C})$			
Overall Doppler at 750°K Uranium			-0.45×10^{-6}
Plutonium			$< +0.1 \times 10^{-6}$
Radial Core Expansion			-10×10^{-6}
Overall Axial Fuel Expansion			-4×10^{-6}
Sodium Expansion Coefficient			
Overall Core Region (avg)			-10×10^{-6}
Core and Reflector			-14×10^{-6}
Estimated Range of Total Worth of Control Rods:			
Composition (v/o): B_4C 55/Coolant 25/Structure 20			
No. of Rods			12
70% B^{10} Enrichment			0.075-0.085
95% B^{10} Enrichment			0.085-0.10
Excess and Shutdown Reactivity:			
Heavy-atom burnup			0.035-0.060 ^(a)
Operational Temperature			
Sodium $\Delta T_{\text{avg}} = 500^{\circ}\text{C}$ core and reflector		0.007	
Radial core expansion $\Delta T_{\text{avg}} = 500^{\circ}\text{C}$		0.005	
Axial fuel expansion $\Delta T_{\text{avg}} = 1500^{\circ}\text{C}$ oxide		0.004	
$\Delta T_{\text{avg}} = 1100^{\circ}\text{C}$ carbide			
$\Delta T_{\text{avg}} = 500^{\circ}\text{C}$ metal			
Total operational temperature ΔT			0.016
Excess Reactivity			0.051-0.076
Shutdown Reactivity			0.020
Design Worth of Control System			0.071-0.096

^(a) Depending on the manner of compensating for burnup.

1.6% margin for the operational temperature change, is taken, the required range of control of 7.1% to 9.6% is within the range of the computed worth of the control system.

FARET Critical Experiments

A series of FARET critical experiments in ZPR-3 was completed to firm up the physics analysis of multifueled, nonuniform core loadings. In Fig. 3 is presented the final core loading of the series which con-

sisted of an inner region (first two rows) of oxide-fueled assemblies followed by a row of carbide and a row of oxide assemblies, with the fifth row comprised of alternating alloy and oxide assemblies. The 93-liter core had a height of 51 cm. The U^{235} enrichment in the fuels ranged from 11% in the alloy to about 40% in the carbides and about 30% in the oxides.

Of primary interest was the establishment experimentally of the predicted control-rod reactivity worths of FARET core systems. Of equal importance was

FIG. 3. ZPR-3 FARET Critical Loading.

TABLE 5. WORTH OF CONTROL RODS IN THE FARET CRITICAL EXPERIMENTS

Oxide-Carbide-Metal Loading
 B^{10} Enrichment $\sim 70\%$

Control-rod Position	Reactivity			
	Experimental		Calculated	
	Inhours	Δk	Inhours	Δk
P-22	-425	0.0076	-520	0.0093
P-22, J-17	-690	0.012	-850	0.015
All Rods (12)	-3705	0.067	-4125	0.074

the substantiation of the neutronic behavior of a mixed core when subassemblies of different type fuels are interchanged or replaced. Other completed measurements included the distributed worths of fuel and structural materials, reaction-rate traverses, and power periods.

The preliminary results of the experiments have been published.⁽⁷⁾ Some analytical results for the control-rod experiments in the nonuniform loading are summarized in Table 5. Banked-rods as well as single-rod experiments were performed to study the interaction effects. The preliminary values of the computations and the experiments compare to within 11% for the banked rods and to about 20% for the single

and paired rods. The reasonable agreement establishes at least the range of control to be expected in the FARET cores.

In Table 6, the experimental data on the substitution of fueled assemblies in each of the rows are summarized and compared with the computed values. Except for the interchange between the alloy and oxide assemblies in Rows 4 and 5, the computations agree to well within 10% of the experimental values.

TABLE 6. SUBASSEMBLY REPLACEMENTS IN THE FARET CRITICAL EXPERIMENT

Mixed Core (93 liters)		559 Inhours = 1% k	
Replacement (1.5-liter assembly)	Row	Reactivity Change, inhours	
		Experiment	Calculations
Carbide for Oxide	1	+396	+375
Alloy for Oxide	1	+214	+195
Carbide for Oxide	2	+382	+370
Alloy for Carbide	3	-150	-154
Carbide for Oxide	4	+264	+237
Alloy for Oxide	4	+140	+114
Oxide for Alloy	5	-84	-66
Alloy for Oxide	5	+82	+63
Oxide for Reflector	6	+150	+148
Carbide for Reflector	6	+272	+248
Reflector for $\frac{1}{2}$ Oxide	5	-214	-234

FIG. 4. Variation of Reactivity versus Fuel-irradiation Periods (Days) at Full Power for FARET Core-I Experiment.

The critical experiments are still in the process of being analyzed; however, the significance that may be attached to the preliminary results thus far is that the behavior of a mixed core loading can be understood as well as that of a uniform core loading.

The maximum allowable excess reactivity for burnup studies in the FARET core will be determined from the critical experiments on the mockup of the final design.

The Experimental Program

The uranium enrichments quoted in this paper include the 6% excess reactivity for burnup.

In Table 7 are presented the atom percent burnup, the irradiation periods, and the number of subassemblies removed and replaced. The carbide C4 fuel assembly is expected to reach a 5 a/o burnup in 180 full-power operating days. At this time, two of the higher-performance oxide assemblies would also be removed for investigation. These two types of assemblies would then be allowed to go directly to about 9 a/o burnup, if the investigation shows satisfactory performance. An intermediate step, in which the other type of oxides have burned to about 5 a/o, would require some 60 additional days.

After the third irradiation period (305 operating days), a reactivity loss of almost 5% is expected. If, at this time, two additional fuel assemblies are added in the fifth row, the reactivity loss could be reduced to 3.2%. The excess reactivity would now allow an

additional irradiation period of 125 days, at the end of which most of the fuel types would have incurred between 8 and 12 a/o burnup.

In Fig. 4 is plotted the reactivity as a function of operating days. The dashed curve is an alternative plan to illustrate the case where the control system allows less than 6% excess reactivity for burnup. A 5% excess reactivity would require a control system having a total worth of 8.6%. The only difference between the two cases is the addition of two assemblies in the initial replacement step. The burnup interval becomes somewhat longer, so that the 8 to 12 a/o burnup would occur at about 460 days.

At the end of the 430-day operating period at full power, the performance of close to 1000 fuel elements would have been investigated at advanced thermal and burnup conditions. During this irradiation period, the fuel-pin cladding material would have been subjected to a total neutron exposure of 1.4×10^{23} nvt. The exposure from neutrons above 1.35 MeV, that is, in the range of the (n,p) and (n, α) threshold reactions, would have been about 3×10^{22} nvt. These threshold reactions may prove to be a factor in limiting the life of the fuel cladding. In this case, the harder neutron spectrum in FARET would subject the clad material to a dose rate comparable to that attained in the softer spectrum of a large reactor having a total flux of 10^{16} n/cm²-sec. In some power systems studied the percent neutrons above 1.35 MeV ranges from 8 to

TABLE 7. REMOVAL AND REPLACEMENT SEQUENCE IN THE EXPERIMENTAL PROGRAM FOR FARET CORE I (HEAVY-ATOM BURNUP VERSUS DAYS AT FULL POWER)

Row and Number of Subassemblies	Fuel Type	Irradiation Period (Days)							
		1	2	3	4	5	6	7	8
		a/o Burnup							
		180	240	305	430	550	690	780	890
(1) 1	C4								
(2) 6	C4	5		8.5	12	15.2			
(3) 4	O4		5.2		9.5	12	15		
4	O6	3.9			9.5	12	15		
4	C3		5.2		9.5	12	15		
(4) 4	O2	3.6			9	11	14.3		
4	O5		4.8		9	11	14.3		
4	C2		4.4		8		12.5	14.3	
4	M1			4.6		8.3		12	15
2	M2			5		9			
(5) 4	O1		4.4		8		12.5	14.3	
4	O3		4.4		8		12.5	14.3	
4	C1			4.6		8.3		12	15
2	M2							10.5	14
No. of Subassemblies Removed		3	6	4	9	9	8	6	3
No. of Subassemblies Replaced		3	6	4	15 ^(a)	9	8	6 ^(b)	
No. of Subassemblies Added in Row 5				2					

^(a) Replace 6 C4-Subassemblies or others as required to maintain desired reactivity for burnup beyond 10 a/o.

^(b) Irradiated Subassemblies M1, M2, and C1 remaining may be moved into Row 3.

10% for ceramic- and metal-fueled reactors. This is to be compared with 22% in the FARET core.

In conclusion, the performance limits of a fuel-element design, including the effect of radiation on the clad, as determined in the FARET experiment, could then be assigned directly to a fuel-element design of a large power reactor with a high degree of confidence.

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Discussion

Mr. Zebroski (GE): Will the core and the system be designed to permit accommodating vented fuel? I would like to comment, if it doesn't detract from the very fine overall design picture, that the linear powers and central temperatures given for the oxide fuel seem inconsistent to me. I think, for example, the vibratory compacted fuel showed similar linear powers and central temperatures, and there should be something like a 15% penalty in conductivity on the vibratory compacted powder if it behaves as it does in water-cooled reactors. Also, the central temperatures seem generally low. Also, can you take vented fuel?

Mr. Persiani: Yes, we have tentatively set up some loop-type experiments. We also have considered allowing certain assemblies to vent directly from the element. We can do pin-element experiments and vent these directly out of the reactor system. If we decide to vent into the coolant, we might use a loop in the first phase of the experiment.

Mr. Kittel (ANL): I'm not sure what Mr. Zebroski is getting at about the penalty for vibratory compacted fuel. In the FARET Core-I design, both the pellet fuel and the vibratory compacted fuel have the same smear density, so the amount of oxide material per inch of fuel rod length is the same in either case. The penalty that is paid for loss of thermal conductivity in the compacted powder lasts, we think, for only a rather short time until sintering begins to occur. This has been borne out at other sites working with UO_2 .

Mr. Bump (ANL): In the written paper it is pointed out that fuel temperatures don't include any uncertainty factors. The reason is that today I'm not quite sure what I want to use; but our final design will have some sort of uncertainty factor. Some changes from the Table presented will result. I could say that I am doing the same as General Electric. I am assuming that the oxide forms a central void, and I'm assuming the void eventually will represent the total amount of void originally included. In other words, if the fuel goes out against the clad and is es-

essentially 100% dense, all the original void occurs in the central hole. As a result you get a savings on central temperature from the geometrical effect.

Mr. Zebroski: I think you should. There are some data which indicate that even after the sintering process has occurred, there is still a systematic penalty you will pay for the powdered fuel, even at the same smear density.

Mr. Chernick (BNL): Could somebody from Argonne explain the philosophy on fuel failures? How many do you think you're going to have during the 900-day operating period? Do you expect to monitor flux? What are you going to do to replace the failed element? To what extent are you going to depend on calculations for this and to what extent on instrumentation?

Mr. Persiani: First I want to say that there is a fuel-irradiation program. Information will be fed back into the fuel system considered for FARET. These fuel pins in FARET will not be untested systems. They will probably have been irradiated in single elements in the EBR-II program. The goals listed were tentative conditions, incidentally. We expect to integrate into this program new ideas on fuels as well as modifications if we find, between now and two years from now when we have to firm up the core, that some of these elements are not performing properly. If not out they go. The frequency of failures to be expected is difficult to predict. We will monitor fuel behavior destructively. A lot of the elements will be instrumented to measure temperatures and perhaps other parameters. We would have fuel-element detection systems like those at EBR-II, or better.

Mr. Chernick: I assume that you're going to pursue these to the extent that you would expect some failure, not catastrophic failure, but some. When you

get your first failure and have to remove your first bundle, what is your design philosophy at this point? Do you stop and do the criticals over again? Do you check your calculations? Do you do a flux scan? To what extent do you depend on your calculations that you've done to date? Will you move fuel elements around?

Mr. Persiani: No, these will be replaced. First of all, we hope to minimize any failed fuel elements by removing them before they fail. We have a constant inspection scheme where we feed back this information into the experiment. If we are not satisfied with the performance of a particular type of fuel element, out it goes and in go the new fuel elements that have performed well up until that day. We will have a fuel inventory, other than the core loading shown. The flux distribution may vary a little, but this is a reactor with large neutron mean free paths. And little trouble is expected in predicting and maintaining flux distributions.

Mr. Leitz (GE): I'd like to shift attention to the positive side. I think a very good point has been made relative to the utility of small fast fueled test reactors. Their deficiency in flux can be compensated for in the case of the technique of enriching fuel and in the case of the clad. The test reactors tend to have a harder spectrum than the fast power reactors, at least those that will be fueled by ceramic fuels. I'd like also to observe that both of those principles could be used now in available fast reactors, namely, EBR-II, Fermi, and of course they are in use at Dounreay.

Mr. Persiani: Yes, except that FARET has the feature of instrumenting the fuel elements. (This is engineering data that has not been heretofore available.) We hope to be able to test more than 1000 fuel pins, and that should provide good statistics.

Transient Response of Coupled Fast Reactor Cores

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SYMBOL TABLE

C	Precursor concentration
k_{ii}	Internal reactivity
k_{ij}	Cross coupling reactivity
l_{ii}	Internal lifetime
l_{ij}	Cross coupling lifetime
s	Laplace Transform dummy variable
S	Neutron source
t	Time
T	Temperature
β	Delayed-neutron fraction
γ	Delayed-neutron worth
τ	Delayed-neutron time constant

Introduction

The reactor core presented by Westinghouse at the Argonne National Laboratory panel discussion on fast reactor safety⁽¹⁾ and in the Westinghouse-AEC 1000-MWe FBR Design Study⁽²⁾ is comprised of seven identical hexagonal modules (see Fig. 1). Each module is composed of a central region of fuel with axial blankets and an annular radial blanket which in turn is surrounded by a barrier of graphite. A detailed description of the core is given in reference (2).

The arrangement of the core results in a small amount of neutronic coupling between the modules. In the steady state, the leakage of neutrons into each module from the adjacent modules just balances the fraction by which the module would be subcritical if it were out of the array. The dynamic behavior of each module is also affected via the cross coupling with its neighboring modules. Because of this effect, a reactivity insertion into one module is divided among the other modules by some function of the neutronic coupling. The effect of the neutronic cross coupling on the dynamic behavior of a perturbed module was evident in the experiments conducted at Los Alamos^(3, 4) with two loosely coupled KIWI-4B reactors. In these pulsed-power experiments, the driver core was closely followed at a lower power level by the passive core. No neutronic instabilities were encountered in the system.

The theory for neutronically coupled systems has received considerable attention during the past several

years for clustered nuclear rockets. The intent of this paper is not to conclude which of the mathematical models, that is, the one employing neutron lifetimes,⁽⁵⁾ time delays,⁽⁶⁾ or neutron waves,⁽⁷⁾ is correct, but to present analytical results and trends obtained from one model with regards to the ability of neutronic coupling apparently to enhance the safety characteristics of the modular reactor presented in references (1) and (2). The safety characteristics of the modular core based on an isolated module using the Westinghouse FORE computer code are given in reference (1). A simplified study of the neutronic coupling effect on the safety characteristics of the modular core is given in reference (2).

Two computer codes are being developed at Westinghouse in order to study the modular system. Both codes use point kinetics with six groups of delayed neutrons. The first code uses the mathematical model of Avery⁽⁵⁾ and is capable of handling nineteen coupled cores. The second code handles two cores and is a simplification of Avery's model. In this model, the cross-coupling lifetimes are made equal to the internal lifetimes. Both codes consider Doppler feedback and are capable of handling sinusoidal and ramp reactivity insertions. In addition, the first code has provisions for scramming the reactor as well as for considering other feedback mechanisms (due to clad, coolant, and fuel).

The Kinetics Model

The kinetics model of Avery⁽⁵⁾ was used in developing the computer codes at Westinghouse. This basic model has several virtues. It is conceptually simple. The equations are similar in form to the single-core kinetics equations. The physical quantities required can be determined without recourse to transient methods.

As a brief introduction to the Avery model, consider a two-core problem and neglect the delayed neutrons. Divide the total rate at which neutrons are generated in each reactor into two parts. Let one part represent the neutrons resulting from neutrons produced in the same reactor (one generation earlier) and the other represent the neutrons resulting from neutrons produced in the other reactor (one generation earlier).

Let

$S_1(t)$ = total neutrons/sec produced in reactor 1;

$S_2(t)$ = total neutrons/sec produced in reactor 2.

Then

$$S_1(t) = S_{11}(t) + S_{12}(t);$$

$$S_2(t) = S_{21}(t) + S_{22}(t),$$

where $S_{12}(t)$ is that portion of the total neutrons/sec being produced in reactor 1 by neutrons born in reactor 2 in the previous generation. The other S_{ij} 's have a similar definition.

Now, if $S_{11}(t)$ neutrons second are produced at time t from neutrons originally produced in reactor 1, what is the magnitude of $S_{11}(t + t_g)$, where t_g is one prompt-neutron generation time? Neglecting second-order terms, we may write $M_1^{11} S_{11}(t)$, where M_1^{11} is the single-generation multiplication factor for a neutron source having the distribution of S_{11} producing new fission neutrons in reactor 1. But this is not sufficient. There is also a neutron source in reactor 1 at time t , identified as $S_{12}(t)$, which has a different spacial distribution than S_{11} , since it is the first-generation source resulting from the sources in reactor 2. The quantity which we require is the source, with the same spacial distribution as S_{11} , which results from the previous generation of S_{12} , since we wish to treat them henceforth as S_{11} neutrons.

The S_{12} source does not produce a new source in one generation which has the spacial distribution of S_{11} . Hence, the simple splitting of the total source into S_{11} and S_{12} components is valid only in the steady state. In the transient case there are a series of sources in reactor 1, representing successive generations of neutrons from previous S_{12} -type sources. Each of these has a slightly different multiplication factor and a slightly different generation time. Thus the assumption that the source of neutrons can be divided into two, and only two, components is not valid.

This has been verified by attempting to treat two parts of a single large thermal core as a pair of coupled reactors with very high coupling. For the purpose of studying coupled modular cores where the degree of coupling is small, the inaccuracy arising from this source may be tolerated.

Of course, if this problem were unique to this one approach to coupled-core kinetics, then this approach would be abandoned. Unfortunately, this is a fundamental problem associated with any attempt to portray coupled cores in which separability of time and space is assumed. Time and space are not separable in the coupled-core problem. There are problems with convergence of very loosely coupled regions in static calculations; hence, one may expect considerable difficulty in achieving successful kinetic calculations, even with adequate funds and computing equipment. Therefore,

it does not seem practical at this time to attempt to perform spacial kinetic calculations.

The multiple coupled-core kinetic equations solved in the computer program are in the form proposed by Avery:

$$l_{jk} \frac{\partial S_{jk}}{\partial t} = k_{jk} (1 - \beta^*) \sum_{m=1}^N S_{km} - S_{jk} + k_{jk} \sum_{i=1}^M \tau_i C_i \gamma_i;$$

$$\frac{\partial C_{ki}}{\partial t} = \beta_i \sum_{m=1}^N S_{km} = \tau_i C_{ki};$$

$$\beta^* = \sum_{i=1}^M \beta_i \gamma_i; \quad S_j = \sum_{m=1}^N S_{jm}.$$

The symbols and equations are those used by Avery with the exception of the introduction of the delayed-neutron worth values, γ_i .

The shift from the S_{12} mode to the S_{11} mode can be handled by several approaches. One is to ignore the fact that an S_{12} source distribution does not become an S_{11} distribution within one prompt generation time and simply treat all fissions resulting from S_{12} as being in the S_{11} distribution without any importance weighting. This overestimates the change in S_1 .

A second approach is to importance weight S_{12} and assume that all fissions resulting from the importance-weighted S_{12} are in the S_{11} distribution. The source arising from the importance-weighted S_{12} distribution is the correct asymptotic S_{11} distribution; therefore this is nearly correct. Since the approximate time to reach the asymptotic value is actually several prompt-neutron generations, this slightly underestimates the change in S_1 . A third possibility is to permit the S_{12} -type source to reproduce itself partially as an S_{12} -type source and partially as an S_{11} -type source.

These possibilities represent progressively more complex systems of equations, and longer running times for computer solutions. Our initial program to investigate coupled-core kinetics uses the first (simplest) type of representation. The results will eventually permit an estimate of the magnitude of the errors and a decision as to the advisability of introducing further complexity for multiple coupled-core analyses.

The Computer Program

In addition to the neutron kinetics equations, simplified heat transfer equations are included. The differential equations are handled in the program as simple forward difference equations.

The program will accept up to 19 distinct regions or modules. Reactivity insertions may be made independently in any or all regions, and may have the form of continuous ramp changes, terminated ramps, or sine waves. Temperature-dependent feedbacks in linear form are provided for coolant, clad, and fuel tempera-

tures. Temperature feedback is provided for the Doppler effect based on the average fuel temperature, with the Doppler effect having a $1/T$ variation.

Provision is made for the insertion of control rods (seram) in several modes. Control-rod seram may be initiated by total power or by period from any region. Seram may then be initiated either in all regions or only in the region from which the seram signal originated. Once a seram is initiated, a time delay is provided before actual insertion occurs. The insertion is in the form of a terminated negative ramp.

Initial total power in thermal megawatts is a separate input for each region. In order to insure correct initial conditions, the program assumes that the k_{ii} values are absolute, and that the cross-coupling k 's are correct relative to each other, and are adjusted if required for steady state. Provision is provided for up to seven delayed-neutron groups.

A time-step size is input. The program will print out results when ever the power has changed by 5% or after 10 time steps have gone by without a printout. The program treats the input time step as a trial value and continuously recomputes a time-step size to satisfy input accuracy criteria for the neutron kinetic equations.

Neutronic Cross-coupling Analysis

In checking the solution given by the computer code, insight into the behavior of two coupled cores as described by the Avery model was obtained by linearizing and simplifying the equations for analytic solutions. The details of this analysis are given in Appendix A. The assumptions made to obtain solutions are as follows:

1. Reactivity is held constant after zero time.
2. Temperature-dependent reactivity feedbacks are set to zero.
3. A two-core system is used.
4. One group of delayed neutrons is employed.
5. All neutron lifetimes, as well as internal and cross coupling, are equal.
6. In the cross-coupling effect, the delayed neutrons are neglected.
7. $k\beta \simeq \beta$.

The basic equations are 1 through 6 in Appendix A. Applying the above assumptions, one obtains equations 9 through 13, which provide solutions of the form

$$S_i(t) = \sum_{m=1}^4 A_{mi} e^{B_m t}.$$

In a real two-core system, it would be expected that the core with a reactivity insertion would pull the second (passive) core along with it and simultaneously

tend to be restrained somewhat by the passive core. This results from a change in the ratio of neutron leakage to the total neutron production in each of the two cores. This neutron cross-coupling effect can be interpreted as a reactivity sharing effect. This effect can be illustrated by rewriting equations (9) and (10):

$$\frac{dS_1}{dt} = \left[\frac{k_{11} - 1 - \beta + \left(k_{12} \frac{S_2}{S_1} \right)}{l} \right] S_1 + \frac{k_{11}}{l} \tau C_1;$$

$$\frac{dS_2}{dt} = \left[\frac{k_{22} - 1 - \beta + \left(k_{21} \frac{S_1}{S_2} \right)}{l} \right] S_2 + \frac{k_{22}}{l} \tau C_2,$$

that is, the cross-coupling term can be interpreted as a reactivity effect which is directly proportional to the ratio of the powers in the two regions. For example, if k_{11} is increased such that at some point in time $S_1 = 1.1 S_2$, then the effective reactivity in core one is reduced by $k_{12} [1 - (1/1.1)]$, and the reactivity in core two is increased by $k_{21}(1.1 - 1)$.

One result of the reactivity sharing effect is that there is no clear definition of the meaning of reactivity worth for an insertion, a control rod, or a fuel assembly in a modular system. The effective worth to a module of an insertion in one module out of two, for example, lies somewhere between the worth calculated as if that module were isolated and one-half of that value, depending upon the neutronic cross coupling and the relative power in the second (passive) module. This reactivity sharing phenomena will be illustrated in the following analyses. The neutronic cross-coupling effect on the dynamic behavior of one perturbed module out of two was evident in the pulsed-power experiments conducted at Los Alamos on the KIWI-4B reactor. The driver core was closely followed by the passive core even with 0.8 cents of neutronic cross coupling.

Analyses Performed

The importance of the neutronic coupling to the reactivity sharing effect between coupled cores was indicated in the analytical solutions (see Appendix A) derived to obtain an insight into the behavior of the kinetic equations. In addition, it was also considered that the coupling neutron lifetime may be an important parameter in determining the power split in a coupled-core system. To investigate these two parameters, a reactor system was selected which consisted basically of two modules of the modular core depicted in Fig. 1. The cross-coupling reactivity and cross-coupling lifetime were varied by an order of magnitude for a large and a small finite ramp and sine wave reactivity insertions into one core. A Doppler effect, $T(dK/dt)$, of

-0.00383 was the only reactivity feedback included in the parametric study. Table 1 gives the selected combinations of coupling coefficients, neutron lifetimes, and reactivity insertions.

The 0.4 % coupling reactivity, k_{12} and k_{21} , represents beginning-of-life modules (see ref. 1), with depleted fuel in the radial blanket. The 4 % coupling reactivity is approximately twice the coupling which would exist

FIG. 1. Modular Core Array.

TABLE 1. PARAMETRIC STUDY: COMBINATION OF COEFFICIENTS REPRESENTING TWO LOOSELY COUPLED CORES
Doppler $T(dK/dT) = -0.00383$

Case Number	Reactivity		Neutron Lifetime		Reactivity Insertion into Driver Core			
	Internal k_{12}, k_{22}	Coupling k_{12}, k_{21}	Internal $l_{11}, l_{22},$ 10^6 sec	Coupling $l_{12}, l_{21},$ 10^6 sec	Ramp Insertion Rate, \$/sec	Insertion Time, sec	Amplitude of Sine Wave, \$	Sine Wave Frequency, radians/sec
A. Parametric Analysis								
	Ramp Insertion							
1	0.996	0.004	0.35	3.5	2.0	0.5		
2	0.960	0.040	0.35	3.5	2.0	0.5		
3	0.996	0.004	0.35	3.5	0.10	0.5		
4	0.960	0.040	0.35	3.5	0.10	0.5		
5	0.996	0.004	0.35	0.35	2.0	0.5		
6	0.96	0.04	0.35	0.35	2.0	0.5		
7	1.0	0.0	0.35		2.0	0.5		
8	1.0	0.0	0.35		0.10	0.5		
B. Parametric Analysis								
	Sine Wave Insertion							
9	0.996	0.004	0.35	3.5		0.5	1.0	3.14
10	0.960	0.040	0.35	3.5		0.5	1.0	3.14
11	0.996	0.004	0.35	3.5		0.5	0.05	3.14
12	0.960	0.040	0.35	3.5		0.5	0.05	3.14
13	0.996	0.004	0.35	3.5		1.0	1.0	3.14
14	0.960	0.04	0.35	3.5		1.0	1.0	3.14
15	1.0	0.0	0.35			0.5	1.0	3.14
16	1.0	0.0	0.35			0.5	0.05	3.14

TABLE 2. COMBINATION OF COEFFICIENTS FOR TWO LOOSELY COUPLED CARBIDE-FUELED CORES WITH 3% PLUTONIUM IN THE RADIAL BLANKET

$$\begin{aligned}k_{11} &= k_{12} = 0.9783 \\k_{12} &= k_{21} = 0.0217 \\l_{11} &= l_{22} = 0.3325 \times 10^{-6} \text{ sec} \\l_{12} &= l_{21} = 3.524 \times 10^{-6} \text{ sec}\end{aligned}$$

	Reactivity Feedbacks		Ramp Reactivity Insertion			
	Doppler	Axial Expansion	Driver Core		Passive Core	
	$T \frac{dK}{dT}$	$\frac{dK}{dT} \times 10^6 \text{ } ^\circ\text{F}$	Rate, \$/sec	Insertion Time, sec	Rate, \$/sec	Insertion Time, sec
1. Inherent Stability						
a. One core perturbed	-0.00338	0.0	0.10	1.0	0.0	0.0
b. Both cores perturbed	-0.00338	0.0	0.10	1.0	0.10	1.0
c. Both cores perturbed	-0.00338	-3.896	0.10	1.0	0.10	1.0
2. Expulsion at 100% power from one module a control rod worth 80 cents	-0.00338	0.0	2.29	0.35	0.0	0.0

in the selected system with 3 % plutonium in the radial blankets.

The behavior of an isolated module was obtained with each reactivity insertion to provide a basis for assessing the reactivity sharing between the two coupled modules. In the isolated module, the internal reactivity of course is 1.0. In addition, analyses were performed for the two-module system using the coupling reactivity corresponding to the end-of-life radial blanket plutonium content, 3 %, given in ref. 1. The cases studied are typical of those selected for accident analyses and are as follows:

1. inherent stability for a small reactivity insertion in one module;
2. inherent stability for a small reactivity insertion into both modules;
3. expulsion at 100 % power from one module of a control rod worth 80 cents.

These cases are listed in Table 2 along with the coefficients calculated for the coupled core design of ref. 1.

Discussion of Results

Parametric Study

The analyses for the parametric study were performed with the use of two forms of reactivity insertion, finite ramps and sine waves; two amounts of reactivity were inserted, 5 cents in 0.5 sec and one dollar in 0.5 sec. For clarity in the following discussion, the module being perturbed, core 1, is designated as the driver module and the unperturbed module, core 2, as the passive module.

The power rises for both the finite ramp insertion

and the sinusoidal insertion of 5 cents in 0.5 sec are shown in Figures 2 and 3, respectively. The power rise for an isolated module is also shown to provide a basis for assessing the neutronic coupling effect. The coupling lifetime is an order of magnitude greater than the internal lifetime (see Table 1, Cases 3, 4, and 8 for the ramp, and Cases 11, 12, and 16 for the sine wave).

The power rise in the driver module decreases with increasing neutronic coupling. With 0.4 % coupling, the power rise for the driver core is 63 % of that of the isolated module; with 4 % coupling it is 52 %. The passive module power follows the driver module power but at a lower level. The spread between them is strongly dependent upon the magnitude of the coupling reactivity. For example, with 0.4 % coupling the passive module power rise is 50 % of the driver module power rise. Increasing the coupling reactivity to 4 % causes the passive module power rise to be 90 % of that of the driver core.

The results for the one-dollar reactivity insertion in 0.5 sec for both the finite ramp and the sine wave insertions are shown in Figures 4 and 5, respectively. The combinations of coefficients are given in Table 1, Cases 1, 2, and 7 for the ramp, and Cases 9, 10, and 15 for the sine wave. The neutronic coupling greatly reduces the peak power in the driver module over that obtained in the isolated module for this amount of reactivity insertion. With 0.4 % coupling reactivity, the peak power rise for the driver module with the finite ramp was 33 % and for the sinusoidal insertion 38 % of the power rise for the isolated module peak. This is a greater reduction than was obtained with the 5-cent insertions shown in Figures 2 and 3. At 1.2 sec for both one-dollar insertions, the power rise for

Fig. 2. Parameter Study: Response of Two Coupled Cores to a Ramp Reactivity Insertion of 5 Cents in 0.5 Sec.

Fig. 3. Parameter Study: Response of Two Coupled Cores to a Sinusoidal Insertion of 5 Cents at 0.5 Cycles per Second.

the driver module for 0.4% coupling was 50% and for 4.0% coupling 33% of that of the isolated module.

The effect of neutronic coupling in limiting the power rise in a modular system relative to that in an isolated

core appears to be more predominant with a large than with a small reactivity insertion. Undoubtedly, with larger reactivity insertions and hence larger temperature increases, the Doppler coefficient of the passive core

FIG. 4. Parameter Study: Response of Two Coupled Cores to a Ramp Reactivity Insertion of One Dollar in 0.5 Sec.

FIG. 5. Parameter Study: Response of Two Cores to a Sinusoidal Reactivity Insertion of One Dollar at 0.5 Cycles per Second for 0.5 Sec.

becomes correspondingly more important in assisting the Doppler coefficient of the driver core in reducing the total power rise of the system. Moreover, with the one-dollar insertions, when the coupling reactivity is increased from 0.4% to 4.0%, the power rise of the driver module is reduced much more than the corresponding increase in power in the passive module.

However, with the 5-cent insertions, the passive module power rose about the same amount as the driver module power decreased when the coupling reactivity was varied from 0.4% to 4.0%.

The sinusoidal reactivity insertions discussed above were for only a quarter-cycle duration. The response of the two modules to a half-cycle of the one-dollar

sinusoidal reactivity insertion is shown in Fig. 6. These results are presented to show that the passive core follows the driver core in the same fashion regardless of whether the reactivity insertion into the driver core has a positive or negative slope.

The neutron coupling lifetime was found to have a negligible effect on the power split between the driver core and the passive core. The combinations of coefficients for this part of the parametric study are given in Table 1, Cases 5 and 6. The results are given in Table 3.

In summary, the neutronic coupling reactivity in a modular reactor is effective in spreading among all the modules any reactivity insertion into one of the modules. The neutron coupling lifetime apparently has a negligible effect on the power split. The power rise of the driver module, relative to an isolated module power rise, is shown in Fig. 7 as a function of the coupling reactivity and the magnitude of the reactivity insertion. From Fig. 7 it can be seen that even a small amount of coupling reactivity, such as 0.1 %, will have a pronounced effect in reducing the power rise in a

FIG. 6. Response of Two Coupled Cores to a Sinusoidal Reactivity Insertion of One Dollar at 0.5 Cycles per Second.

TABLE 3. EFFECT OF CROSS-COUPLING LIFETIME

Rate of reactivity insertion = 2.0 \$/sec.

Insertion time is 0.5 sec.

$l_{11} = l_{22} = 0.35 \times 10^{-6}$ sec.

$P_0 = 100\%$ power

Time, sec	Coupling Lifetime $l_{12} = l_{21} = 0.35 \times 10^{-6}$ sec		Coupling Lifetime $l_{12} = l_{21} = 0.35 \times 10^{-5}$ sec	
	Core Power Ratio		Core Power Ratio	
	Driver	Passive	Driver	Passive
A. $k_{11} = k_{22} = 0.996$; $k_{12} = k_{21} = 0.004$				
0.0	1.0	1.0	1.0	1.0
0.08	1.1182	1.0622	1.1177	1.0616
0.16	1.2681	1.1412	1.2668	1.1399
0.22	1.4084	1.2149	1.4063	1.2130
B. $k_{11} = k_{22} = 0.960$; $k_{12} = k_{21} = 0.040$				
0.0	1.0	1.0	1.0	1.0
0.056	1.0623	1.0571	1.0621	1.0569
0.22	1.2984	1.2737	1.2989	1.2740
0.377	1.6401	1.5864	1.6371	1.5834

modular system. Moreover, the effectiveness of the coupling reactivity in limiting the excursion increases with increasing reactivity insertion due to the negative feedback of the Doppler coefficient of the passive core.

Analyses of a Specific Modular Reactor

The analyses listed in Table 2 were performed for the Westinghouse modular reactor described in ref. 1. The coefficients used in the analyses were calculated for this system with end-of-life conditions, that is, with 3 % plutonium in the radial blanket. The coupling reactivity for this case is 2.17 %. The analyses are typical of those required for an accident study.

The inherent stability of two modules to a ramp insertion of 10 cents (10 cents/sec) into each module is given in Fig. 8. The analyses were performed with Doppler and with Doppler-plus-axial-expansion feedback. As expected, when both modules are simultaneously and equally perturbed, the system responds in the same manner as would an isolated module.

In the case that only one of the modules is perturbed by this reactivity insertion, the inherent stability with only Doppler feedback would be as shown in Fig. 9. The power rise in the driver core after 1.2 sec is 53 % of the power rise when both modules are perturbed equally (see Fig. 8). It can also be seen from these curves that the 2.17 % coupling reactivity in a modular system is worth slightly more than an axial expansion coefficient of $-3.896 \times 10^{-6}/^{\circ}\text{F}$ would be in reducing the power rise in an isolated core.

FIG. 7. Parameter Study: Ratio of Power Rise in Two Coupled Modules to an Isolated Module.

FIG. 8. Response of Two Coupled Cores with a Ramp Reactivity Insertion of 10 Cents in Each Core.

The response with Doppler feedback of a two-module system to a ramp reactivity insertion of 80 cents in 0.35 sec is given in Fig. 10. This much reactivity (but with a parabolic distribution and thus less total reactivity inserted over the first 0.35 sec) was used for the shim control-rod expulsion accident analysis in ref. 1. The resultant power rise in the driver module is some-

what lower than the power rise in the analysis of ref. 1 in which both Doppler and axial expansion feedback were considered.

Thus, due to the division of the reactivity insertion among all the modules, the severity of an accident initiated in one module will be much less than the same accident in an isolated core.

FIG. 9. Response of Two Coupled Cores for a Ramp Insertion Worth 10 Cents into One Core.

cores inherently possesses a promptly acting safety mechanism through the neutronic coupling effect. It was shown that in a two-core system with only a few tenths per cent coupling coefficient a reactivity insertion into the driver core will result in a significant split of the resultant rise in power between the driver and passive cores. The power rise in the driver core is considerably less than the level which would be attained in an isolated core subjected to the same amount of reactivity insertion.

The Westinghouse reactor system of ref. 1 consists of seven modules. Reactivity insertions into one module out of seven will result in lower power rises in the driver core than those exhibited in the two-core analyses of this paper. Thus, it may very well be that the seven-module array is an overly conservative design. Future investigations may reveal that a smaller number of larger modules might still be a very conservative design but economically even more attractive. This would be a result of the higher specific powers, and internal

FIG. 10. Response of Two Coupled Cores to a Ramp Reactivity Insertion of 80 Cents in 0.35 Sec.

Conclusions

From the results presented herein, it can be seen that a reactor system consisting of a multi-modular array of

breeding ratios and lower inventory requirements doubling times and fuel-cycle costs which could conceivably be obtained in fewer modules.

Appendix

Some insight into the behavior of coupled cores as described by the Avery model can be obtained by

linearizing and simplifying the equations in order to obtain analytic solutions. For this purpose we make the

following assumptions:

- 1) constant reactivity values after time zero;
- 2) no temperature feedback to reactivity;
- 3) only two core regions;
- 4) only one group of delayed neutrons;
- 5) all neutron lifetimes are equal;
- 6) in cross coupling, delayed neutrons will be neglected;
- 7) $k\beta \simeq \beta$.

The basic equations are:

$$l_{11} \frac{dS_{11}}{dt} = k_{11}(1 - \beta)S_1 - S_{11} + k_{11}\tau C_1; \quad (1)$$

$$l_{12} \frac{dS_{12}}{dt} = k_{12}(1 - \beta)S_2 - S_{12} + k_{12}\tau C_2; \quad (2)$$

$$l_{21} \frac{dS_{21}}{dt} = k_{21}(1 - \beta)S_1 - S_{21} + k_{21}\tau C_1; \quad (3)$$

$$l_{22} \frac{dS_{22}}{dt} = k_{22}(1 - \beta)S_2 - S_{22} + k_{22}\tau C_2; \quad (4)$$

$$\frac{dC_1}{dt} = \beta S_1 - \tau C_1; \quad (5)$$

$$\frac{dC_2}{dt} = \beta S_2 - \tau C_2. \quad (6)$$

By assumption 5), $l_{11} = l_{12} = l_{21} = l_{22} = l$. Addition of equation (1) to (2), and of (3) to (4) yields

$$l \frac{dS_1}{dt} = (k_{11} - 1 - \beta)S_1 + k_{11}\tau C_1 + k_{12} \cdot \left[\frac{(1 - \beta)}{k_{12}} S_2 + \tau C_2 \right]; \quad (7)$$

$$l \frac{dS_2}{dt} = (k_{22} - 1 - \beta)S_2 + k_{22}\tau C_2 + k_{21} \cdot \left[\frac{(1 - \beta)}{k_{21}} S_1 + \tau C_1 \right]. \quad (8)$$

Then by application of assumption (6), which assumes that $(\tau C - (\beta/k)S)$ in the cross term can be neglected, we have

$$\frac{dS_1}{dt} = \frac{(k_{11} - 1 - \beta)}{l} S_1 + \frac{k_{11}}{l} \tau C_1 + \frac{k_{12}}{l} S_2; \quad (9)$$

$$\frac{dS_2}{dt} = \frac{(k_{22} - 1 - \beta)}{l} S_2 + \frac{k_{22}}{l} \tau C_2 + \frac{k_{21}}{l} S_1; \quad (10)$$

$$A_{m1} = \frac{\left[\frac{\beta S_1^0}{l(B_m + \tau)} + S_1^0 \right] \left[B_m - \frac{k_{22} - 1 - \beta}{l} - \frac{\tau\beta}{l(B_m + \tau)} \right] + \frac{k_{12}\beta S_2^0}{l_2(B_m + \tau)} + \frac{k_{12}S_2^0}{l}}{\left[B_m - \frac{k_{22} - 1 - \beta}{l} - \frac{\tau\beta}{l(B_m + \tau)} \right] \left[1 + \frac{\tau\beta}{l(B_m + \tau)^2} \right] + \left[B_m - \frac{k_{11} - 1 - \beta}{l} - \frac{\tau\beta}{l(B_m + \tau)} \right] \left[1 + \frac{\tau\beta}{l(B_m + \tau)^2} \right]}; \quad (17)$$

$$\frac{dC_1}{dt} = \beta S_1 - \tau C_1; \quad (5)$$

$$\frac{dC_2}{dt} = \beta S_2 - \tau C_2. \quad (6)$$

Taking the Laplace transform of equations (9), (10), (5), and (6), combining and applying boundary conditions consisting of

$$S_1(0) = S_1^0; \quad (11)$$

$$S_2(0) = S_2^0; \quad (12)$$

$$\frac{dC_1(0)}{dt} = \frac{dC_2(0)}{dt} = 0, \quad (13)$$

we obtain the following transform solutions for the sources:

$$S_1^*(s) = \frac{\frac{S_1^0\beta}{l(s + \tau)} + \frac{k_{12}S_2^*(s)}{l} + S_1^0}{s - \frac{k_{11} - 1 - \beta}{l} - \frac{\tau\beta}{l(s + \tau)}}; \quad (14)$$

$$S_2^*(s) = \frac{\frac{S_2^0\beta}{l(s + \tau)} + \frac{k_{21}S_1^*(s)}{l} + S_2^0}{s - \frac{k_{22} - 1 - \beta}{l} - \frac{\tau\beta}{l(s + \tau)}}. \quad (15)$$

This provides solutions of the form

$$S_i(t) = \sum_m A_{mi} e^{B_m t},$$

where the B_m are the roots of

$$\left[s - \left(\frac{k_{22} - 1 - \beta}{l} \right) - \frac{\tau\beta}{l(s + \tau)} \right] \cdot \left[s - \left(\frac{k_{11} - 1 - \beta}{l} \right) - \frac{\tau\beta}{l(s + \tau)} \right] - \frac{k_{12}k_{21}}{l} = 0. \quad (16)$$

This is a quartic equation in s , yielding, in general, four roots, all four of which appear in the solutions for both sources.

The coefficients A_{mi} are the residues at the poles of the transforms of the two sources. The coefficients are, in general, eight different values. These have been evaluated for a few representative cases of small insertions in one region. The coefficients for the two cores are

$$A_{m2} = \frac{\left[\frac{\beta S_2^0}{l(B_m + \tau)} + S_2^0 \right] \left[B_m - \frac{k_{11} - 1 - \beta}{l} - \frac{\tau\beta}{l(B_m + \tau)} \right] + \frac{k_{21}\beta S_1^0}{l^2(B_m + \tau)} + \frac{k_{21}S_1^0}{l}}{\left[B_m - \frac{k_{22} - 1 - \beta}{l} - \frac{\tau\beta}{l(B_m + \tau)} \right] \left[1 + \frac{\tau\beta}{l(B_m + \tau)^2} \right]} \quad (18)$$

$$+ \left[B_m - \frac{k_{11} - 1 - \beta}{l} - \frac{\tau\beta}{l(B_m + \tau)} \right] \left[1 + \frac{\tau\beta}{l(B_m + \tau)^2} \right].$$

The results are typically one small positive exponent and three large negative exponents, with a slightly larger coefficient for the positive exponential for the core in which reactivity is inserted. Thus the passive region tend to follow the region with the insertion, but at a lower level.

It is of some interest to check these solutions for limiting cases. If the cores are uncoupled, then k_{21} and k_{12} go to zero. Then the equation for the roots, (16), reduces to

$$\left[s - \frac{k_{22} - 1 - \beta}{l} - \frac{\tau\beta}{l(s + \tau)} \right] \cdot \left[s - \frac{k_{11} - 1 - \beta}{l} - \frac{\tau\beta}{l(s + \tau)} \right] = 0. \quad (19)$$

Then there are two pairs of roots, one for each core. These pairs are independent, since either bracket being zero will satisfy the equation. Hence the solutions limit correctly as the coupling vanishes.

At maximum coupling, we would expect $k_{11} = k_{12} = k_{22} = k_{21}$. Then the equation for the roots becomes

$$\left[s + \frac{1 + \beta}{l} - \frac{\tau\beta}{l(s + \tau)} \right]^2 - 2 \left[s + \frac{1 + \beta}{l} - \frac{\tau\beta}{l(s + \tau)} \right] \frac{k_{11}}{l} = 0, \quad (20)$$

and either

$$s + \frac{1 + \beta}{l} - \frac{\tau\beta}{l(s + \tau)} = 0 \quad (21)$$

or

$$s + \frac{1 + \beta}{l} - \frac{\tau\beta}{l(s + \tau)} - 2 \frac{k_{11}}{l} = 0. \quad (22)$$

Equation (21) may be discarded since it would give roots independent of reactivity. Therefore, the solution effectively reduces to a single quadratic equation, (22), having two roots, just as one finds for a single core. Therefore, the equations at least approximately limit to the behavior of a single core at maximum coupling.

Physically, one expects that the core with a reactivity insertion would tend to pull the other core along with it, and simultaneously tend to be somewhat restrained by the other core. This results from a change in the ratio of in-leakage to total neutron production in the

two cores. This can be interpreted as a reactivity sharing effect. Equations (9) and (10) can be rewritten to illustrate this:

$$\frac{dS_1}{dt} = \left[\frac{k_{11} - 1 - \beta + (k_{21}S_2/S_1)}{l} \right] S_1 + \frac{k_{11}}{l} \tau C_1;$$

$$\frac{dS_2}{dt} = \left[\frac{k_{22} - 1 - \beta + (k_{21}S_1/S_2)}{l} \right] S_2 + \frac{k_{22}}{l} \tau C_2,$$

that is, the cross-coupling term can be interpreted as a reactivity effect which is directly proportional to the ratio of the powers in the two regions. For example, if k_{11} is increased such that at some point in time $S_1 = 1.1 S_2$, then the effective reactivity in core one is reduced by $k_{12} [1 - (1/1.1)]$, and the reactivity in core two is increased by $k_{21}(1.1 - 1)$.

One result of this reactivity sharing is that there is no clear definition of the meaning of reactivity worth for a modular core. The effective worth of a control-rod insertion in one module of seven, for example, lies somewhere between the worth calculated for the module isolated and one-seventh of that value, depending upon the coupling and the relative power in the other modules.

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Discussion

Mr. Kohler (Texas A&M): Have you tried to derive your kinetics equations from the Boltzmann equations

directly? At Karlsruhe, in investigating the safety of coupled fast thermal reactors, it was found that when very strong reactivity changes occur in one of the cores, which makes the space-time nonseparability worse, your kind of model is not satisfactory any more to describe the kinetic behavior or a fast-thermal system. I would think this would apply to fast reactors, too.

Mr. Heck (WAPD): There is some discussion of this in the paper, in fact, particularly where you get very large excursions in one core relative to the other. It is clear that the simple assumption that the driven neutron source goes immediately in one generation into the first mode, which is what is assumed in those equations, is not going to correctly describe the situation. One of the possibilities we've been looking into is to split that particular equation, so that the driven source can go both into the first mode and back into the driven source as a first approximation to adjust for the correct spatial distribution.

Mr. Gibson (AI): We, too, have looked at the modular type of core, and we seem to differ a little bit on the value of strong coupling coefficients. Some of our studies demonstrate that for a module which is \$30 subcritical when you pull it out of the complete array, you can get significant power shifts from local reactivity perturbations. How extensively have you investigated this? Secondly, Mr. Wright the other day mentioned that there was a possible Achilles heel of the modular concept in terms of the secondary criticality considerations when one module melts down. I would be interested in your comments.

Mr. Gunson: What happens when one module melts down has not really been studied. From our analysis

one might draw the conclusion that the severity when a single module melts is reduced; but as the next modules go off, you might get a whole series of "bangs."

Mr. Lellouche (BNL): Do you have any expectation that such a model with a constant coupling would be valid for very fast excursions initiated in one module? In fact, to get any decent results, do you have to go to a space-dependent examination?

Mr. Gunson: At Westinghouse we have discussed this space-dependency aspect, and the change in the flux shape, but this is a "hairy" problem. We took the simple approach, just to get an engineering feel.

Mr. Spinrad (ANL): I would like to point out what's different in the calculations presented from essentially pseudo-static calculations for a single core which might, for example, be divided into two parts quite arbitrarily; the motion of a rod locally would have a bigger reactivity effect than it would if you tried to make a global reactivity. It seems to me that this is just a calculation of that effect and that to that extent it is, simply, a so-called modal expansion set of experiments. Can you comment on the difference between those two?

Mr. Heck: I'm not quite sure I know how to answer the question as it was posed. Yes, this is a sort of a modal approach to a system. The reason that one expects at least reasonable results from this is that in the kind of system we were looking at the coupling was extremely low, so that the spatial variations were reasonably small. We have looked at grossly unbalanced cores, but this work is still in progress.

Mr. Madell (ANL): Have you compared your method with Westinghouse's WIGLE code?

Mr. Gunson: No.

Enhanced Safety Characteristics of a Sodium-cooled Coupled Reactor*

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The importance of a strong negative Doppler reactivity effect and a weakly positive sodium void reactivity effect on the safety of fast reactor systems has been recognized by numerous investigators. As an outgrowth of a study of coupled reactors, a reference design was completed by Atomics International under AEC sponsorship of a fast reactor which was optimized to obtain a very strong negative Doppler effect and a negative sodium void reactivity effect. The design consists of modular fast regions surrounded by low-enriched flux-trap blankets. For a 1000-MWe plant, the core includes a hexagonal array of ten identical regions (see Fig. 1). The blanket regions are formed by two rings of low-enriched fuel elements (3½% average plutonium enrichment on equilibrium cycle), separated by a ring of graphite moderator elements. These regions and the associated control rods can be seen in Fig. 1.

By the utilization of blanket regions with an equilibrium enrichment of 3½%, the power extracted from these regions is large, specifically, 40% of the total reactor power. Similarly, the Doppler contribution from the blanket regions is large due to the large nuclear importance of the enriched blankets and the softened spectrum in the blanket region. Table 1 shows the negative isothermal Doppler constant by region and the "effective" isothermal Doppler constant when weighted by the appropriate temperature rise in each region.

Figure 2 shows the reactivity change due to sodium voiding as a function of the radial fraction of the core voided for a flux-trap blanket arrangement compared with a single-blanket arrangement. As noted from the figure, voiding of the single-blanket arrangement results in a slightly positive initial reactivity addition followed by a strongly negative effect as the blanket regions are voided. In the reference design arrangement, neutrons born in the fast region which penetrate to the outer blanket region of the module have a low probability for re-entering the fast region. This is due to their low energy state and the large

mass of moderating material between the blanket and the fast core. This neutron-trapping effect and the leaky nature of the modular arrangement are the principal reasons for the negative sodium void effect observed.

Table 2 presents some of the principal nuclear characteristics of the reference design compared with a conventional fast breeder reactor in which no concerted effort is made to improve upon the Doppler or sodium reactivity coefficients. The Doppler effect shown in Table 2 represents the total reactivity change resulting from an increase from normal operating temperature to the point at which fuel damage occurs. The sodium void reactivity effect represents the maximum possible reactivity addition obtained during voiding of the core. As noted from the table, some compromises in economic characteristics result from the enhancement of the safety characteristics. The breeding ratio is reduced and the fissile mass is increased from that of the conventional fast breeder reactor.

The kinetic behavior of the reference design reactor was studied in a series of hypothetical accident situations. Conditions in which the reactor protective system was both operative and disabled were studied. A criterion for fuel damage was established at 6500°F center fuel temperature. Reactivity insertion rates were programmed at up to \$100/sec. It was found that the strong Doppler effect of this design (0.008) is sufficient to control a reactivity insertion of \$1 without rapid scram. With normal action of the protective system, a reactivity insertion of \$1.65 can be tolerated with no fuel damage. This is sufficient to control such hypothetical accidents as control-rod ejection, dropped fuel element, and flow-channel blockage.

To determine the potential for damage in a hypothetical situation in which coolant flow is arbitrarily and instantaneously reduced to zero with no immediate compensating action by the protective system, the reference design was compared with a different fast reactor having a similar Doppler reactivity effect but a slightly positive void reactivity effect. The results of this analysis are shown in Fig. 3. For the reactor

* Work performed under Contract AT(11-1)GEN-8 for the U. S. Atomic Energy Commission.

FIG 1 Modular Flux-trap Concept.

TABLE 1. ISOTHERMAL DOPPLER CONSTANT^(a) $T(dk/dT)$:

Fast Region	-0.002298
Inner Blanket Region	-0.009024
Outer Blanket Region	-0.006782

Total	-0.0181
"Effective" Total Weighted by Regional Temperature Rise	-0.008

^(a) U^{238} effect only.

with the positive void reactivity, it was assumed that the maximum void effect during loss of coolant from the core would be approximately \$3.50 positive and that complete voiding of the core would result in a slightly negative effect. As can be seen from Fig. 3, the net reactivity to the core becomes rapidly negative in the reference design arrangement due to both Doppler and void reactivity effects. In the case of the other fast reactor, although the Doppler effect reduces the total reactivity addition, the positive effect is more pronounced. The resulting temperature rise in the centerline of the hot fuel is also shown on Fig. 3.

Another study was performed to determine the compromise made in economics by emphasizing the sodium void reactivity effect. This study utilized an arrangement in which an additional row of fuel elements was added to the fast region. This increased the diameter of the module. In a 1000-MWe plant, an array of 7 such modules with an active height of

5 ft was compared with the 10-module array for the small module design. The core reactivity effects and economics for these designs are shown in Table 3. It can be seen from the table that the Doppler effects are similar for the two designs, but the large module has a significantly larger positive sodium void effect. The fuel-cycle costs of the large-module design are roughly $\frac{1}{4}$ mill/kWhr less than those of the small-module design.

Potential long-term improvements in both safety and economics are obtained by using a modified version of the reference design with mixed-carbide fuel. Due to its lower operating temperature, the sodium-bonded, high-power-density, carbide-fueled core can tolerate reactivity insertions in excess of \$2.00 with protective system action and in excess of \$1.50 without rapid scram. Coolant boiling occurs more rapidly with the carbide fuel than with the oxide fuel because of the improved heat transfer characteristics. Therefore, it is necessary to suppress boiling by reducing coolant temperature drop or by some other means in order to take advantage of the improved carbide characteristics.

To determine the magnitude of additional costs associated with conservative safety criteria and conservative design practice for a first-generation plant, a cost estimate of two 1000-MWe fast reactor plants was made. The results of this cost estimate are shown in Table 4. As shown in the table, the total costs of energy generation range from a low of 3.2 mills/kWhr to a high of 4.4 mills/kWhr. This corresponds to a capitalized cost difference of \$58,000,000. The high

FIG. 2. Void Reactivity Effects.

value shown represents the probable costs for a first-generation plant with rather stringent safety requirements. For example, this plant would probably include a steel structure for containment of the entire primary system. Additionally, inert-gas-filled vaults would be utilized to house equipment components in the primary system. Both primary and secondary sodium systems would be used.

In addition to high figures for capital cost, the fuel-cycle and operating and maintenance cost figures of a first-generation plant are higher. The fuel costs shown represent those associated with the small-module design previously discussed. The fuel-cycle costs also include a conservative estimate for fabrication costs. The inventory interest rates are computed at 10% per annum, and plutonium is valued at \$10/gm.

The low costs shown are believed to be representative of values which could be obtained after demonstration of certain safety concepts. This demonstration could be accomplished by a successful research and development program and by the construction of large prototype and/or operating fast reactor plants. The low costs include the kinds of reduction in capital costs which would occur from elimination of the secondary sodium system, from the possible use of a low-leakage tilt-up concrete building structure, from the elimination of inert-gas-filled equipment cells, and from a better definition of contingencies. The fuel-cycle costs shown in Table 4 (0.45 mill/kWhr) represent those obtained for the large-module design

TABLE 2. PHYSICS RESULTS

Doppler Compensates in Event of Sodium Loss		
	Modular Flux Trap	Conventional FBR (Typical)
Doppler Effect (\$)	-1.50	-0.50 to -0.75
Sodium Loss (\$) (max)	+0.53	+8.0 to +12.0
Doppler Coefficient Insensitive to Sodium Loss		
Enriched Blankets Stabilize:		
Power Distribution		
Sodium Void		
Doppler Coefficients		
Single Module Criticality Requires \$30 Perturbation		
Safe Design Requires Modest Economic Compromises		
	Modular Flux Trap	Conventional FBR (Typical)
Breeding Ratio	1.30	1.4 to 1.5
Fissile Mass (kg)	3050	2000 to 2500
Doubling Time (yr)	15	8 to 12

and include optimistic assumptions about fabrication cost for mixed oxide fuel.

In summary, certain economic and safety trade-offs are possible in fast reactor design. These trade-offs are capable of increasing or decreasing the plant cost by many millions of dollars. The arrangement of the reference design provides a method for enhancing the Doppler reactivity effect and reducing the positivity of the sodium void effect while still maintaining an

FIG. 3. Voiding Accident Behavior.

TABLE 3. CORE REACTIVITY COEFFICIENTS VS ECONOMICS

	Small Module	Large Module
Active Height (ft)	4.2	5.0
Diameter across Flats (ft)	3.5	4.0
Doppler Reactivity Change to Fuel Damage (\$)	-1.56	-1.55
Maximum Sodium Voiding Reactivity Change (\$)	0.25	2.00
Fuel-cycle Cost (mill/kWhr)	0.76	0.55

TABLE 4. ENERGY COSTS OF 1000-MWe FAST REACTOR, INVESTOR-OWNED PUBLIC UTILITY

Cost Item	Capital Cost (\$10 ⁶)		Rate (%)	Annual Cost (\$10 ⁶)		Unit Cost (mill/kWhr)	
	High	Low		High	Low	High	Low
Fixed Charges							
Capital	150.6	120.6	14.5	21.8	17.5	3.12	2.50
Nuclear Liability Insurance	—	—	—	—	—	—	—
Operating Costs							
Fuel-cycle Cost	—	—	—	6.02	3.15	0.86	0.45
Operating and Maintenance	—	—	—	2.3	1.58	0.33	0.22
Total Cost of Energy Generation				30.45	22.56	4.36	3.22

economic system with a fuel-cycle cost of $\frac{3}{4}$ mill/kWhr (at \$10/gm plutonium and 80% load factor). These particular safety-oriented parameters (Doppler and voids) can be strengthened or weakened in the reference design by suitable rearrangement of the geometric array of the reactor. In general, increased safety results in increased cost.

Discussion

Mr. Peterson (Battelle-Northwest): A couple of years ago we investigated a similar concept and we found that indeed it did help our safety problems tremendously, but we traded this off for a poor power distribution across the core. We also found quite a power shift during the core lifetime. Would you care to comment on these characteristics?

Mr. Eggen: I'm sorry I forgot to give acknowledgment to you. We are aware of some of your printed reports. The indications of power distribution across the core are satisfactory as far as I can remember. We find that there is, of course, loading of depleted fuel in the blanket region and taking out of fuel which has derived about 7% plutonium by end of life, which amounts to something to the order of about 50,000 MWd/T equivalent irradiation. In the blanket region there will be, of course, local power shifts. Investigation has shown that the temperature swings were relatively small; the problems associated with the structure, etc., could be accommodated, and hydraulic factors were introduced to accommodate this in the reactor design. Our complete report will soon be published as NAA-SR-11378.

Mr. Chernick (BNL): To what extent do you pay for the fact that your coefficients don't apply to isothermal conditions, that the blanket coefficient Doppler effect is indeed delayed relative to the core, and that you again have problems of flux shape during possible accidents?

Mr. Eggen: We carried these through the various kinetic calculations with appropriate time constants using the Avery type of coupling analysis. We have also looked at it from the standpoint of using an analog system in which we put certain components. Because of the smallness of the system and the fact

that most of the reactors operate with very fast neutrons, the time delays are not of concern.

Mr. Chernick: In really big accidents the short time constants of the fast reactor above prompt critical would be of concern.

Mr. Sevy (AI): In evaluating the Doppler feedback the specific power in the blanket which is lower than that in the core was taken into account. I believe what Mr. Chernick is asking about is the effect rising from the difference of prompt-neutron lifetimes in the thermalized blanket as compared to fast sections.

Mr. Eggen: In this case the Doppler is down by a factor of 3 or 4.

Mr. Wirtz (Karlsruhe): Did you say how your power is distributed between the fast and thermal parts of your reactor?

Mr. Eggen: We do not have a thermal part, but the blanket area has about 40% of the total power of the system. About 25% of the power is in the inner blanket and about 13 or 14% in the outer blanket region; 60% is in the fast core sections.

Mr. Wirtz: Did you say that in this case you have a breeding ratio of about 1.3?

Mr. Eggen: Overall, yes.

Mr. Wirtz: From my memory of the Karlsruhe calculation this seems to be fairly high.

Mr. Eggen: Some people are inclined to say that the results of our calculations are a little higher than the average. Yet we had access to four fast reactor reports. We cross calculated such systems and derived approximately the same characteristics and the same breeding ratio. We thus assume we are on a comparable basis.

Mr. Häfele (Karlsruhe): I think that in such a coupled concept as yours there is a very flat power distribution in the core and I think an accidental excursion gives a relatively large energy release. The energy release might be higher by a factor of 60 or so in the case of a very flat power distribution. Although you do get advantages with respect to Doppler coefficients and sodium void coefficients, in the case of a maximum hypothetical accident your energy releases might be higher by a factor of 50. How do you balance that against each other?

Mr. Eggen: One factor, of course, is that we have isolated the different modules. We believe that you can mechanically shut down the rest of the reactor system. For our excursion, a Bethe-Tait type of analysis indicated energy releases only something of the order of 70 lb of TNT.

Mr. Greebler (GE): One aspect of your calculation that is a little disturbing is that the mode of the decoupling has appeared to make each module look like a very small reactor for sodium voiding purposes. In

other words, neutrons along the edge have a low importance; yet for the Doppler effect it appears that the neutrons along the edge must have high importance. There is a bit of a contradiction here, but let's be specific. How did you treat the question of coupling, and how did the coupling between the modules change when sodium is removed? If you did this by studying a single module with adjustable boundary conditions, one can get very large errors from what one assumes about the boundary conditions.

Mr. Eggen: The power affects the Doppler coefficient and the temperature rise in the system, etc. Most of the Doppler effect comes from the inner part of the inner blanket, the outer blanket contributing primarily to the sodium void coefficient. The sodium void coefficient is significantly reduced by trapping neutrons from adjacent module units and also those that might have been reflected back into the system. Thus it is the outer blanket which effectively holds down the sodium void coefficient and it is the inner module that contributes most strongly towards the Doppler coefficient where it has more importance.

Mr. Sevy: In regard to Mr. Greebler's second question, we evaluated coupling in the normal operating case and determined that the self-criticality in the case of k_{11} for any one of the modules was approximately 0.90. This was done by having the critical-source modes in one module but no fissioning in the others.

Mr. Eggen: I think the answer is that we did not study that particular problem.

Mr. Avery (ANL): My comment relates to the previous paper mostly, but it was touched upon here. I would like to make a few qualitative statements about when we can expect the equations of the previous paper to be good and when they may fail. Let's talk about two modules, take the case where neutrons from one module will give rise to a distribution in the other module which is essentially of the same shape regardless of where the neutrons originated in the first one. Then I believe we can expect the equations as they are stated to be quite good, and I think that in two relatively isolated modules this will be the case. Quite clearly if we take a large system and arbitrarily cut it in two, this condition would not be met, and one would be involved in the situation in which one must treat the space-dependent equations. Thus by relatively intuitive thinking I think one can tell when the conditions are satisfied. This can be tested by straightforward static test if one does a single iteration starting with different source neutrons at different positions and looks at the shape at which they arrive.

Some Safety Considerations Based on Design Experiences in the BNL Settled-bed Fast Reactor Concept*

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VERTICAL SECTION

FIG. 1 Axial Flow Settled Bed Reactor.

The mechanical design of a fast reactor can greatly affect its safety under a loss-of-coolant accident. In the short time available, we shall discuss one of the

* Work performed under the auspices of the U. S. Atomic Energy Commission

dominant safety features of the Settled Bed Fast Reactor (SBFR) concept, a concept we introduced at the conference two years ago. This safety feature can be of importance not only for reactors having fuel elements consisting of packed beds of fuel particles in random

FIG. 2. Sodium Temperature Coefficient vs Position in Axial Flow Reactor.

FIG. 3. Power Transient due to a Permanent Drop of 100°C in Reactor Inlet Sodium Temperature.

or ordered arrangement, but also for other reactor concepts.

In the SBFR there is a sodium region above the core and each of the axial blankets to provide head room for fluidization. In Fig. 1 is shown an elevation of one conceptual design. Immediately above and below the core are the sodium regions which are considered here. For both of these regions there is a strong negative reactivity effect when the sodium expands.

In Fig. 2 the local sodium expansion coefficient at power is shown as a function of position in this re-

actor. As in most fast reactors, there is a local positive effect at the center of the core, and this becomes negative at the core edge. Here, however, there is a still stronger negative effect as we move into the sodium region. The overall temperature coefficient in this sodium region is many times larger than the core sodium and Doppler coefficients.

The temperature coefficient in the sodium region is a delayed one, due to the sodium transit time through the core, of the order of magnitude of 0.1 sec. In the present case this is a powerfully stabilizing influence.

FIG. 4. Variation of Local Core Sodium Temperatures after 100°C Drop in Reactor Inlet Sodium Temperature.

Figure 3 shows the time behavior of the reactor power when the inlet sodium temperature is reduced 100°C and maintained at that lower temperature; this corresponds to a reactivity increase of ~ 0.4 . Even for this drastic temperature change, the peak power exceeds the original power level by less than 50%. The various turning points of this curve can be related to the passage of the cold front through various regions of the reactor, but our time limit here prevents discussing this.

Figure 4 shows the core sodium temperature at several positions as a function of time for the same perturbation. The peak rise of exit temperature exhibited is only 25% above the initial value. The time behavior exhibited was obtained from a multiregion space-and-time solution of the reactor equations, which were programmed at Brookhaven National Laboratory.

The sodium regions were introduced into the SBFR because of bed engineering requirements. However, a similar high-leakage region might well be of benefit in other reactor designs as well. The location, the size, and the composition of such a region can all be ad-

justed to obtain desirable stability properties. This might be considered for use in many fast reactor designs. For fuel elements containing ordered arrays of fuel particles, such as in the Brookhaven National Laboratory Countercurrent Reactor concept presently under study, the values of the parameters of location, size, and composition are set by the fashion in which the individual fuel particles are stacked in the fuel element. Since the ordered bed fuel element can be designed so to be disassembled and reassembled periodically, these parameters can be altered when necessary.

Discussion

Mr. Kohler (Texas A&M): I noticed that on your curve for the active power when inserting cold sodium into the reactor, there was initially a very small dip in the reactor power. Could you explain that?

Mr. Levine: Yes. This very very small dip is a real effect. It is a very slightly positive reactivity coefficient from the blanket of the reactor.

Comparison of He, CO₂, and Steam as Coolants of a 1000-MWe Fast Reactor*

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1. Introduction

The safety problems arising because of the sodium void coefficient in big fast reactor cores make the possibility of using a much less dense coolant, like a gas, very interesting. The recent discovery of the explosive character of sodium boiling^(1, 2) also makes these problems even more serious.

In the present paper the performances of three fluids: helium, carbon dioxide, and steam, as coolant of a big fast reactor core are compared.

2. Equations Governing the Heat Transfer in the Core‡

Assuming a chopped-cosine, axial power distribution and a core geometry given by a cluster of parallel smooth fuel rods, the hydraulic diameter of the cooling channel is given by

$$d_h = K_0 \left(\frac{P'}{P} \right)^{0.8} \frac{HP'^{0.2}}{Q^{0.2}}, \quad (1)$$

where

$$K_0 = 0.0193(T_2 - T_1)^{0.2} \frac{1}{\nu} \left(\frac{\rho_w}{\rho} \right)^{0.8} \cdot \left(\frac{c_{pm}^{0.2} c_{pw}^{0.8}}{c_{pm}} \right) \frac{k_w^{0.2}}{P_w^{0.4}} \left(\frac{H'}{H} \right);$$

$$\nu = \frac{1}{\sqrt{\left(2\varphi \frac{c_{pm}}{c_{pw}} - 1 \right)^2 \sin^2 \frac{\pi H}{2H'} - 1}};$$

$$\varphi = \frac{T_{wm} - T_1}{T_2 - T_1}.$$

The factor K_0 includes all the parameters depending on the physical properties of the coolant, but it depends also on the ratio H'/H , on the coolant temperatures at the inlet and outlet of the core, and on the maximum

surface temperature of the fuel element. However, once these parameters are chosen, K_0 is fixed and characteristic of the coolant chosen. It is interesting to notice at this point, that K_0 is pressure-independent for a perfect gas.

The total pressure drop in the core is the sum of various different contributions:

Δp_1 , due to friction of the coolant against the wall of the coolant channel;

Δp_2 , due to turbulent dissipation caused by the grids supporting the fuel rods;

Δp_3 , due to the momentum loss necessary to accelerate the coolant in the cooling channel (acceleration due to increase of temperature and decrease of pressure), and acceleration losses and recoveries at the inlet and outlet of the channel.

If we assume a core geometry given by smooth fuel rods supported by transversal grids, the analytical expressions of these pressure drops are

$$\Delta p_1 = \frac{1.115}{(T_2 - T_1)^{1.8}} \frac{\mu_m^{0.2}}{\rho_m c_{pm}^{1.8}} \frac{(P'/P)^{1.8} H}{d_h^3 P'^{1.8}} Q^{1.8}; \quad (2)$$

$$\Delta p_2 = \frac{16\xi}{(T_2 - T_1)^2} \frac{1}{\rho_m c_{pm}^2} \frac{(P'/P)^2 Q^2}{d_h^2 P'^2}; \quad (3)$$

$$\Delta p_3 = \frac{16}{(T_2 - T_1)^2} \left(\frac{T_2 - T_1}{T_m} + \ln \frac{p_1}{p_2} + \frac{1.1T_1 - 0.55T_2}{2T_m} \right) \frac{1}{\rho_m c_{pm}^2} \frac{(P'/P)^2 Q^2}{d_h^2 P'^2}. \quad (4)$$

To these pressure drops correspond three contributions to the total pumping power required to circulate the coolant in the core. The pumping power, as usual, is referred to the heat quantity to be abducted:

$$\frac{N_1}{Q_{th}} = K_1 (P'/P)^{0.6} \frac{Q^{2.4}}{H^2 P'^{2.4}}; \quad (5)$$

$$\frac{N_2}{Q_{th}} = K_2 (P'/P)^{0.4} \frac{Q^{2.4}}{H^2 P'^{2.4}}; \quad (6)$$

$$\frac{N_3}{Q_{th}} = K_3 (P'/P)^{0.4} \frac{Q^{2.4}}{H^2 P'^{2.4}}, \quad (7)$$

* This paper has been prepared within the framework of the association EURATOM-Gesellschaft für Kernforschung mbH., in the field of fast breeder development.

† Delegated from EURATOM to the "Fast Breeder Project," Kernforschungszentrum Karlsruhe.

‡ The formulae shown in this paragraph are obtained in Appendix I. A table of nomenclature follows the appendices.

where

$$K_1 = \frac{1.553 \cdot 10^5}{\eta} \frac{\nu^3}{(T_2 - T_1)^{3.4}} \frac{(\rho/\rho_w)^{2.4}}{\rho_1 \rho_m} \cdot \frac{c_{pM}^3}{c_{pm}^3 c_{pw}^{2.4}} \frac{\mu_m^{0.2} P_r^{1.2}}{k_w^{0.6}} \left(\frac{H}{H'} \right)^3;$$

$$K_2 = \frac{4.29 \cdot 10^4 \xi}{\eta} \frac{\nu^2}{(T_2 - T_1)^{3.4}} \frac{(\rho/\rho_w)^{1.6}}{\rho_1 \rho_m} \cdot \frac{c_{pM}^2}{c_{pm}^3 c_{pw}^{1.6}} \frac{P_r^{0.8}}{k_w^{0.4}} \left(\frac{H}{H'} \right)^2;$$

$$K_3 = \frac{4.29 \cdot 10^4}{\eta} \left(\frac{T_2 - T_1}{T_m} + \ln \frac{p_1}{p_2} + \frac{1.1T_1 - 0.55T_2}{2T_m} \right) \cdot \frac{\nu^2}{(T_2 - T_1)^{3.4}} \frac{(\rho/\rho_w)^{1.6}}{\rho_1 \rho_m} \frac{c_{pM}^2}{c_{pm}^3 c_{pw}^{1.6}} \frac{P_r^{0.8}}{k_w^{0.4}} \left(\frac{H}{H'} \right)^2.$$

Therefore the total pumping power required by the core is

$$\frac{N_t}{Q_{th}} = [K_1(P'/P)^{0.6} + K_2(P'/P)^{0.4} + K_3(P'/P)^{0.4}] \frac{Q^{2.4}}{H^2 P^{2.4}} = K_4 \frac{Q^{2.4}}{H^2 P^{2.4}}. \quad (8)$$

3. General Comparison Based on Heat Transfer Properties

For a certain fuel-element rod (constant P'/P , P' , H) and a certain amount of heat Q to be carried away from any coolant channel of the core, d_h is proportional to K_0 .

We assume that the core is formed by parallel fuel rods without extended heat transfer surfaces (fins, etc.). The coolant volume fraction in the fuel boxes is given by

$$\alpha = \frac{1}{1 + \frac{d}{d_h}}. \quad (9)$$

Equation (19) of Ref. 3 shows that the optimum diameter of the fuel rod from the point of view of fuel-cycle considerations is proportional to $\chi^{0.25}/H^{0.25}$ for constant density, enrichment, and average burn-up of the fuel. The quantity χH is the heat produced by each fuel-element rod, and it is equal to Q , the heat abducted in any coolant channel, in the case of square array of rods, or equal to $1/2 Q$ in the case of a triangular array. Thus we can write

$$d = c_1 \frac{Q^{0.25}}{H^{0.5}}. \quad (10)$$

From (1), (9), and (10) we obtain

$$\alpha = \frac{1}{1 + \frac{d^{2.8}}{K_0 c_1^2 c_2^2 Q^{0.3}}}, \quad (11)$$

where $P' = c_2 d$. Equation (11) gives the physical meaning of K_0 : for constant fuel-rod diameters, an increase in K_0 means an increase in coolant volume fraction; for constant coolant volume fractions, an increase in K_0 requires an increase in fuel-rod diameter.

In the previous reasoning χH , or Q , is held constant: this is significant because, as shown in Ref. 3, χH is one of the main parameters upon which the fuel cycle depends.

The physical meaning of K_4 is easier to understand: for a certain fuel-element rod and certain thermal output Q for a coolant channel, K_4 is proportional to the total pumping power required to circulate the coolant in the core, referred to the total thermal output.

In order to compare the values of K_0 and K_4 for He, CO_2 , and steam as coolant of a fast reactor core, we have to make a series of estimates in choosing the numerical values of some of the parameters which appear in the expressions.

Coolant Pressure at the Core Inlet

The physical properties of the coolant depend on the coolant pressure. The pressure variation along the cooling channels is generally quite small in comparison with the absolute value (of the order of a few percent for acceptable reactors). Accordingly we can take the pressure at only one section of the core as a characteristic of the pressure field. The most significant pressure is the maximum, because it influences the design of the pressure vessel, and it is generally at the inlet of the core. We have performed calculations for three values of p_1 , namely, 70, 100, and 150 atm. Such high pressures are necessary with gas cooling and are technically feasible with the use of prestressed concrete pressure vessels.

Maximum Cladding Surface Temperature

This temperature depends on the material used for cladding and on the can concept used. In terms of stainless steel, a maximum surface temperature of 650°C would be probably appropriate with free-standing clad; and 700°C with collapsed clad. (It is interesting to notice that these values are nominal maximum and that they would correspond to maximum hot spot temperatures of 750–800°C and 800–850°C, respectively.) These figures, however, are only indicative; the cladding problem requires a lot of experimental investigation yet. Calculations were performed for both temperature values.

FIG. 1. Schematic Flow Diagram of Steam-cooled Power Plant.

Coolant Temperature at the Core Outlet

In the case of steam cooling no secondary circuit is used (see Fig. 1). Thus the coolant temperature at the core outlet is practically equal to the maximum superheated steam temperature required by the turbines. Modern big turbines require temperatures in the range from 538^(4, 5) to 565°C or more.⁽⁶⁾ At the lower temperature ferritic steels can still be used; with the higher austenitic steels are necessary. In our calculations we used an average value of 550°C. In the case of He or

CO₂, heat exchangers are required. A difference between outlet gas temperature and maximum steam temperature in the heat exchangers often used is 30°C.⁽⁷⁾ In recent studies of gas-cooled fast reactors such a difference has been assumed equal to 60°C⁽⁴⁾ or to 83°C.⁽⁵⁾ As a reasonable compromise we chose 50°C. The resulting He and CO₂ outlet temperature is 600°C. In reality this temperature should be obtained by an optimisation process based on the cost of the kWhe, because the greater is T_2 (for a constant maximum steam tempera-

ture) the smaller are the sizes of the heat exchangers and (for a constant value of T_{wM}) the bigger the pumping power.

Coolant Temperature at the Core Inlet

The gas inlet temperature (for He and CO₂) should also be obtained by an optimisation process. Indeed, the higher it is, the higher is the efficiency of the steam cycle, but the coolant mass flow must be increased to carry away the same quantity of heat from the core and the pumping power increases considerably. In the studies indicated above,^(4, 5) T_1 has been chosen equal to 260°C, which seems a reasonable value in terms of the considerations just mentioned. The same value was chosen in the present study. However, it is believed that the range of variation of T_1 is much larger than that of T_2 , and it depends on the coolant pressure (more precisely, higher values of p_1 allow higher T_1 's). In a very recent study,⁽⁸⁾ released when the majority of our calculations were already performed, T_1 was assumed equal to 300°C for $p_1 = 70$ atm and equal to 340°C for $p_1 = 140$ atm.

For steam the choice of T_1 is not free. Indeed, Fig. 1 clearly shows that T_1 depends on the pressure p_1 and the pumping power N_t/Q_{th} , which fixes the degree of superheating of the steam at the core inlet. In Appendix II the relationship between T_1 , p_1 , and N_t/Q_{th} is studied. The values of T_1 shown in Table 1 were ob-

tained from the values of N_t/Q_{th} in the numerical example shown in the next paragraph. The indicated values of T_1 are then less general than those for the other two gases. However, the variation of T_1 with N_t/Q_{th} in the range of practical values is quite small.

Other Parameters Appearing in the Expressions of K_0 and K_4

We assume:

$P'/P = 1$, because we refer to fuel element without heat transfer extended surfaces.

$H/H' = 0.821$, which refers to the numerical example of the next paragraph.

$\xi = 0.21$, which corresponds to a fuel-element grid support studied at Karlsruhe.⁽⁹⁾

$\eta = 0.726$.⁽¹⁰⁾

The coolant pressure at the core outlet, p_2 , appears also in the expression for K_4 , but the influence on K_4 is very weak. Thus, as in the case of T_1 for steam, the values of p_2 have been obtained from the numerical example of the next paragraph. The physical properties of helium were taken from References 11, 12, and 13. The transport properties of carbon dioxide are from Ref. 14 and the thermodynamic properties from Ref. 15. The properties of steam were taken from References 16–20.

Table 1 below shows the values of K_0 and K_4 calculated on the basis of the above assumptions. These

TABLE 1

Coolant	Maximum Surface Fuel-element Temperature, T_M (°C)	Core Inlet Pressure p_1 (atm)	Core Inlet Coolant Temperature T_1 (°C)	Core Outlet Coolant Temperature T_2 (°C)	Collant Channel Size Factor $K_0 \cdot 10^2$ (cal ^{0.2} cm ^{-0.2} sec ^{-0.2})	Pumping Power to Thermal Output Ratio Factor $K_4 \cdot 10^6$ (cal ^{-2.4} cm ^{4.4} sec ^{2.4})
He	650	70	260	600	1.468	4.133
		100	260	600	1.468	1.816
		150	260	600	1.468	0.7706
	700	70	260	600	2.132	1.603
		100	260	600	2.132	0.7599
		150	260	600	2.132	0.3310
CO ₂	650	100	260	600	1.040	4.575
		150	260	600	1.039	1.331
	700	70	260	600	1.521	2.755
		100	260	600	1.543	1.111
		150	260	600	1.518	0.4242
Steam	650	70	310.4	550	1.666	3.066
		100	319.1	550	1.644	0.9439
		150	344.5	550	1.584	0.3028
	700	70	295.6	550	1.972	1.228
		100	315.2	550	1.981	0.5080
		150	342.6	550	1.856	0.1791

values are fairly general because they depend mainly on the gas chosen, and on the pressure and temperatures indicated in the table.

One can see that:

K_0 is practically not affected by the pressure.

Higher values of T_{wM} mean higher coolant volume fractions for constant fuel-rod diameters or higher fuel-rod diameters for constant coolant volume fractions.

Carbon dioxide requires smaller coolant volume fractions or smaller fuel-rod diameters than those for helium or steam.

In any case, steam requires the minimum amount of pumping power.

Helium is the second best.

4. Numerical Example

The above considerations, although useful and rather general, are not really conclusive. We know, for instance, that steam requires less pumping power than the other two gases, but this doesn't really mean much if we don't know something about the core dimensions, plant efficiency, fuel rating, etc.

We therefore made calculations for particular reactors using the above values of K_0 and K_4 , and the following parameters:

plant net electrical output: 1000 MWe

pin diameter in the core: 0.635 cm

core height: 183 cm (chosen in a way to have reactors with $H/D \cong 0.7$)

cladding material: stainless steel

cladding thickness: 0.38 mm

average power per pin length: 211 W/cm

maximum power per pin length: 384 W/cm (that is, a maximum oxide fuel temperature equal to 2200°C)

pin length due to axial blanket and gaseous fission products store chamber: the same as the core height

pin diameter in gaseous fission products store chamber: 80 % of that in the core

pumping power required by the primary circuit other than reactor: 2 % of the total thermal output for He and CO₂; 1 % for steam at 70 and 100 atms, 0.5 % for steam at 150 atms

core blockage factor: 14 % in volume, of which 13 % made up of structural material and 1 % of coolant

The steam cycle thermodynamic efficiencies (which depend mainly on T_1 and T_2) have been calculated with the optimum number of regenerative preheaters.⁽²¹⁾ Auxiliary power other than that required by the primary coolant circulators and by the feedwater

TABLE 2

Coolant	Maximum Surface Fuel Element Temperature T_{wM} (°C)	Core Inlet Pressure p_1 (atm)	Coolant Channel Hydraulic Diameter d_h (cm)	Pressure Drop across the Core, Δp (atm)	Pumping Power to Thermal Output Ratio N_t/Q_{th} (%)	Power Plant Net Electrical Efficiency (η_t (%))	Reactor Thermal Output Q_{th} (MW)	Pumping Power N_t (MW)	Core Volume V (liters)	Coore Coolant Volume Fraction (%)	Core Steel Volume Fraction (%)	Core Fuel Volume Fraction (%)
He	650	70	0.468	15.6	18.0	27.2	3670	661	11120	37.4	24.2	38.4
		100	0.468	9.8	9.0	33.0	3030	274	9170	37.4	24.2	38.4
		150	0.468	6.2	5.0	35.7	2800	140	8490	37.4	24.2	38.4
	700	70	0.680	4.9	8.4	33.5	2990	250	10790	45.4	22.4	32.2
		100	0.680	3.3	5.0	35.6	2805	141	10130	45.4	22.4	32.2
		150	0.680	2.2	3.3	36.7	2720	90	9830	45.4	22.4	32.2
CO ₂	650	100	0.332	51.4	17.1	26.3	3800	737	10080	30.5	25.7	43.8
		150	0.332	23.4	7.1	34.3	2910	206	7725	30.5	25.7	43.8
	700	70	0.485	21.4	12.6	30.7	3256	412	10020	38.2	24.0	37.8
		100	0.492	11.9	6.3	34.8	2872	181	8910	38.5	23.9	37.6
		150	0.484	7.5	3.6	36.5	2740	100	8430	38.2	24.0	37.8
	Steam	650	70	0.532	15.6	12.9	28.6	3490	451	11200	40.2	23.6
100			0.525	8.4	4.7	37.0	2700	126	8600	39.9	23.6	36.5
150			0.506	5.0	1.7	40.4	2470	42	7750	39.1	23.8	37.1
700		70	0.629	7.3	5.8	35.5	2815	164	9790	43.7	22.8	33.5
		100	0.632	4.7	3.0	38.6	2588	78	8980	43.9	22.7	33.4
		150	0.592	3.1	1.2	40.9	2450	30	8250	42.5	23.0	34.5

FIG. 2. Pressure Drop across the Core for a Constant Net Electrical Output of 1000 MWe.

pumps in the turbine circuit has been neglected. The primary-coolant circulators were assumed to be driven by high-pressure steam turbines.

These parameters and assumptions can be considered typical of big gas-cooled reactors. Some of them are from previous studies on the subject;^(4, 5, 8) some come from estimates of the best or most likely values of these parameters.

The necessity for choosing values for such parameters reduces considerably the generality of the comparison performed in this paper. Indeed, the three coolants should be compared, not for the same parameters, but in the conditions in which their different properties can be used best. In practice this is very difficult, especially because of the great uncertainties in estimating the variations of plant capital cost when the

above parameters, plus temperatures and pressure, vary. Nevertheless, it is believed that this type of comparison is still significant, because the physical properties of the three coolants compared are not extremely different. A comparison with sodium in these conditions would naturally be less significant.

The results of the calculations are reported in Table 2 and Figures 2 to 6. As expected, carbon dioxide requires the minimum amount of coolant volume fraction, but produces the highest pressure drops in the core. As far as pumping power and plant efficiency are concerned, steam is always the best, at parity of p_1 and T_{WM} , followed by helium and then by carbon dioxide.

The comparison performed was for constant core height and therefore constant fuel-rod diameter [see equation (10)]. The results of a comparison in which H and d vary, but always remain in the relationship (10), especially for carbon dioxide (where the values of K_0 are so much different from those of the other two coolants) are shown in Table 3 for CO₂; the coolant-

FIG. 3. Net Electrical Efficiency for a Constant Net Electrical Output of 1000 MWe.

channel hydraulic diameter was held equal to that obtained for helium (0.68 cm). One can see that the performance of CO_2 improves with respect to the case with $H = \text{constant}$, although it is still not as good as that of steam, but this improvement is paid for by a considerable reduction in fuel-rod diameter. This would probably produce an increase in the number of supporting grids (increase in ξ) which was not taken into account in the calculation and which would probably completely cancel the improvement in N_t/Q_{th} and η_t . Besides, H/D becomes about 1.1, and the core volume increases more than 20%. Thus we thought that the improvement was not real and we did not pursue this way further.

FIG. 4. Pumping Power Required by the Coolant Primary Circuit for a Constant Net Electrical Output of 1000 MWe.

FIG. 5. Core Volume for a Constant Net Electrical Output of 1000 MWe.

5. Neutronic Computations

The thermal calculations allowed us to fix the dimensions, coolant, structural material, and fuel volume fractions in the core. With these parameters known, it is possible with neutronic calculations to obtain the fuel enrichment, fuel rating, core internal and total breeding ratio, coolant void, and Doppler coefficients.

These calculations are described in more detail in Appendix III. The results are shown in Table 4 and in Figures 7 to 11. The rating does not vary very much because of the assumptions that Q , d , and H are constant. Thus for constant total plant efficiency the fuel-cycle cost is approximately the same for all reactors. The Doppler coefficient constant A_{Dop} is proportional

FIG. 6. Core Coolant Fraction for a Constant Net Electrical Output of 1000 MWe.

to the Doppler coefficient because the fuel element temperature is approximately the same for all the cases considered.

The difference between the Doppler coefficient, calculated between the maximum fuel temperature of the average element and the fuel melting temperature, and the coolant void coefficient is shown in Fig. 11. We consider this difference as an indicating coefficient of safety. In this one makes reference to a hypothetical accident due to instantaneous outflow of the coolant from the core and takes as a danger signal the melting of all the fuel with temperature above the peak temperature of the average fuel element. This coefficient of safety is only indicative, because such an accident is in practice impossible; the action of the negative Doppler coefficient is always much faster than that of the positive void coefficient in any possible foreseen accident, especially if one contemplates the use of a prestressed pressure vessel. A real safety evaluation could be derived only by a detailed analysis of any possible credible accident.

6. Conclusions

The results of this study have not a definitive character, since too many assumptions have been made. Some are somewhat arbitrary, for instance, the fixing of the height of the core, of the coolant temperature at the core inlet for helium and carbon dioxide, and of the pumping power required by the primary circuit other than reactor.

Furthermore, the assumed fuel-element geometry can be improved. For instance, partial roughening of the fuel-element rods or use of heat-transfer-extended surfaces for mechanical support would probably allow the use of rods with bigger diameters. It is clear also that partial roughening will improve the performance more with carbon dioxide than with helium or steam because of the tendency of that gas to require very small rod diameters with smooth rods. On the other hand, use of extended surfaces will improve the performance

TABLE 3

CO ₂ : T ₁ = 260°C; T ₂ = 600°C; T _{wM} = 700°C; d _h = 0.680 cm									
Core Inlet Pressure p ₁ (atm)	Core Height H (cm)	Fuel Rod Diameter d (cm)	Pressure Drop across the core p (atm)	Pumping Power to Thermal Output Ratio N _t /Q _{th} (%)	Power Plant Net Electrical Efficiency η _e (%)	Reactor Thermal Output Q _{th} (MW)	Pumping Power N _t (MW)	Core Volume V (liters)	Core Coolant Volume Fraction (%)
70	266	0.526	15.9	9.6	32.7	3061	294	12 250	49.5
100	262	0.540	8.9	4.7	35.8	2790	132	11 360	48.9

TABLE 4

Coolant	Maximum Surface Fuel Element Temperature T_{wM} (°C)	Core Inlet Pressure p (atm)	Enrichment (Number of Atoms of Fissile Plutonium Over Total Fuel)	Rating (MWth/kg fiss)	Core Internal Breeding Ratio	Total Breeding Ratio	Void Coefficient (%)	Doppler Coefficient Constant A_{Dop}	Indicative Safety Coefficient (%)
He	650	70	0.0958	1.03	1.108	1.382	0.413	-0.00892	-1.040
		100	0.0976	1.01	1.083	1.382	0.608	-0.00870	-0.810
		150	0.0986	1.00	1.071	1.384	0.922	-0.00859	-0.478
	700	70	0.1037	0.952	1.015	1.336	0.555	-0.00810	-0.764
		100	0.1050	0.939	1.001	1.332	0.789	-0.00795	-0.506
		150	0.1050	0.939	1.000	1.330	1.183	-0.00850	-0.202
CO ₂	650	100	0.0925	1.07	1.152	1.407	0.703	-0.00944	-0.835
		150	0.0949	1.04	1.121	1.400	1.185	-0.00937	-0.342
	700	70	0.0978	1.01	1.083	1.376	0.658	-0.00887	-0.788
		100	0.0992	0.994	1.068	1.387	1.053	-0.00890	-0.396
		150	0.0998	0.988	1.062	1.369	1.579	-0.00905	+0.105
	700	70	0.1068	0.924	0.899	1.100	9.06	-0.0155	+6.54
Steam	650	70	0.1014	0.972	1.004	1.257	5.40	-0.0133	+3.24
		100	0.1050	0.939	0.947	1.209	6.89	-0.0141	+4.59
		150	0.1068	0.924	0.899	1.148	8.71	-0.0152	+5.23
	700	70	0.1068	0.924	0.944	1.214	5.92	-0.0131	+3.78
		100	0.1092	0.902	0.899	1.162	7.47	-0.0141	+5.17
		150	0.1099	0.899	0.859	1.100	9.06	-0.0155	+6.54

more with steam and helium, because there is more space to accommodate them in the core than with carbon dioxide.

These are the limitations of the present study. However, we believe that the similarity of the physical properties of the coolants considered allows a fairly accurate comparison although the absolute values may be not the best for the three coolants.

The conclusions can be summarized as follows:

1. When the maximum coolant pressure and the maximum fuel-element clad surface temperature are held constant:
 - a) Steam is by far the best coolant from a thermal point of view, i.e., the most economical, followed by helium. Carbon dioxide is the worst coolant.
 - b) Carbon dioxide-cooled reactors are the best breeders. Steam-cooled reactors have the smallest breeding ratios.
 - c) If one takes the available Doppler minus the total coolant void reactivity as a criterion indicative for safety, then helium is the safest coolant, steam the most dangerous, with the difference between helium and carbon dioxide being very small.

2. The passage from maximum fuel cladding surface temperature of 700°C (indicative for collapsed fuel clad) to a temperature of 650°C (free standing clad) produces a considerable decrease in the total plant efficiency at low pressures, the effect being almost negligible at high pressures. The effect on breeding and safety coefficient is very small; the 650°C temperature is in any case better.
3. In Figures 12 and 13 the total breeding ratios and the safety coefficients (Doppler minus void) are plotted versus the total plant efficiency η_t , thus giving an indication of breeding versus economy, and safety versus economy. From these figures it appears that for a constant plant efficiency η_t the carbon dioxide- and helium-cooled reactors are better breeders and safer than the steam-cooled reactors. However, since steam is a better coolant from a thermal point of view, the comparison at constant η_t is much closer than at constant coolant pressure. Furthermore, it should be stressed that these diagrams are biased rather strongly against steam because for a constant η_t and approximately the same fuel-cycle cost, implicit in the assumptions made in this compari-

FIG. 7. Core Internal Breeding Ratio.

son, steam is more economical than carbon dioxide or helium. Indeed, the capital costs are considerably lower with steam because the required coolant pressure is smaller, and no heat exchangers are necessary. On the other hand, the turbine costs would probably be less with carbon dioxide or helium, especially if a low coolant pressure is chosen.

Until these capital costs are known with a sufficient precision, it is not possible to draw a definitive conclusion regarding the choice of a gas coolant for a large fast reactor.

Appendix I

Equations Governing the Heat Transfer in the Core

All the following considerations are valid for a central core subassembly where the maximum amount of heat will be produced. For the other subassemblies a certain amount of orificing is required.

Temperature Distribution in Cooling Channels

We assume that heat flux distribution in the core is given axially by a cosine law:

$$q = q_0 \cos \frac{\pi z}{H'} \quad (12)$$

Then, the coolant enthalpy along the cooling channel is given by

$$G[I(z) - I_1] = \int_{-H/2}^z q_0 \cos \frac{\pi z}{H'} dz$$

$$= \frac{q_0 H'}{\pi} \left(\sin \frac{\pi z}{H'} + \sin \frac{\pi H}{2H'} \right) \quad (13)$$

The variation of pressure in a cooling channel is generally small in comparison with the absolute pressure value (of the order of a few percent), so it is possible to write with good approximation in place of equation (13),

FIG. 8. Total Breeding Ratio.

$$Gc_p[T(z) - T_1] = \frac{q_0 H'}{\pi} \left(\sin \frac{\pi z}{H'} + \sin \frac{\pi H}{2H'} \right), \quad (14)$$

where c_p is the specific heat averaged in the interval $T(z) - T_1$. This approximation is, of course, better for helium and CO₂ than for steam.

For $T(z) = T_2$ eq. (14) becomes

$$Q = Gc_{pm}(T_2 - T_1) = \frac{2q_0 H'}{\pi} \sin \frac{\pi H}{2H'}.$$

Therefore,

$$q_0 = \frac{\pi Gc_{pm}(T_2 - T_1)}{2H' \sin \frac{\pi H}{2H'}}. \quad (15)$$

By definition one has

$$P'h[T_w(z) - T(z)] = q = q_0 \cos \frac{\pi z}{H'}. \quad (16)$$

Therefore,

$$T_w(z) = T_1 + \frac{q_0 H'}{\pi Gc_p} \left(\sin \frac{\pi z}{H'} + \sin \frac{\pi H}{2H'} \right) + \frac{q_0}{P'h} \cos \frac{\pi z}{H'}. \quad (17)$$

Now, c_p and h are not independent of z because they depend on the temperature, which is not constant along the channel. However, except in cases of very high temperature gradients, this dependence is rather weak, so, for simplicity and to obtain general laws independent of temperature distributions, we assume in the following differentiation that c_p and h are independent of z . Differentiating (17) with respect to z and setting the derivative equal to zero, we obtain the section z_M where T_w has a maximum:

$$\left(\frac{\partial T_w}{\partial z} \right)_{z=z_M} = \frac{q_0}{Gc_{pM}} \cos \frac{\pi z_M}{H'} - \frac{q_0 \pi}{P'hH'} \sin \frac{\pi z_M}{H'} = 0.$$

Therefore

$$\tan \frac{\pi z_M}{H'} = \frac{P'hH'}{Gc_{pM}}. \quad (18)$$

We have from eq. (17),

$$T_{wM} = T_1 + \frac{q_0 H'}{\pi Gc_{pM}} \left(\sin \frac{\pi z_M}{H'} + \sin \frac{\pi H}{2H'} \right) + \frac{q_0}{P'h} \cos \frac{\pi z_M}{H'}. \quad (19)$$

Replacing equations (15) and (18) in (19), we obtain

$$\varphi = \frac{T_{wM} - T_1}{T_2 - T_1} = \frac{1}{2} \frac{c_{pm}}{c_p} \left(1 + \frac{1}{\sin \frac{\pi z_M}{H'} \sin \frac{\pi H}{2H'}} \right). \quad (20)$$

FIG. 9. Void Coefficient (S).

Therefore

$$\tan \frac{\pi z_M}{H'} = \frac{1}{\sqrt{\left(2\varphi \frac{c_{pM}}{c_{pm}} - 1 \right)^2 \sin^2 \frac{\pi H}{2H'} - 1}} \quad (21)$$

and

$$\frac{hP'H'}{\pi Gc_{pM}} = \frac{1}{\sqrt{\left(2\varphi \frac{c_{pM}}{c_{pm}} - 1 \right)^2 \sin^2 \frac{\pi H}{2H'} - 1}} = \nu. \quad (22)$$

Replacing equations (15) and (18) in (14) for $z = z_M$, we obtain

$$T(z_M) = T_1 + \frac{c_{pm}}{c_{pM}} \frac{T_2 - T_1}{2} \cdot \left[\frac{1}{\sin^2 \frac{\pi H}{2H'} \left(2\varphi \frac{c_{pM}}{c_{pm}} - 1 \right)} + 1 \right]. \quad (23)$$

FIG. 10. Doppler Coefficient Constant A_{Dop} .

Heat Transfer Condition in the Section of Maximum Cladding Temperature

The heat transfer coefficient under forced convection of gases in tubes at high temperature has been extensively studied in the last 15 years. The conclusions of these studies and of experimental investigations is that the heat transfer coefficient is given⁽²²⁾ by the equation

$$Nu_w = 0.020 Re_w^{0.8} Pr_w^{0.4}. \quad (24)$$

Equation (24) is valid for tubes and for ducts which do not present very acute corners. So it is also valid in the case of a parallel-rod array when the pitch-to-rod diameter ratio p/d is not too near to one. Fortunately, for big reactor cores the optimum p/d is considerably higher than one and the cooling channel doesn't pre-

sent acute corners. Consequently eq. (24) is still valid for this geometry provided that the inner diameter of the tube is replaced by the hydraulic diameter d_h .

By definition, eq. (24) can be written as

$$\frac{hd_h}{k_w} = 0.020 \left(\frac{\rho_w v d_h}{\mu_w} \right)^{0.8} Pr_w^{0.4} = 0.020 \left(\frac{\rho_w}{\rho} \frac{4A\rho v}{P\mu_w} \right)^{0.8} Pr_w^{0.4}.$$

Therefore,

$$d_h = \frac{0.020}{h} k_w Pr_w^{0.4} \left(\frac{\rho_w}{\rho} \right)^{0.8} \left(\frac{4G}{P\mu_w} \right)^{0.8}. \quad (25)$$

But from eq. (22) we have

$$h = \frac{\pi G c_{pM}}{P' H'} \quad (26)$$

and

FIG. 11. Indicated Safety Coefficient.

FIG. 12. Breeding versus Economy.

FIG. 13. Safety vs Economy.

$$G = \frac{Q}{c_{pm}(T_2 - T_1)}. \quad (27)$$

By replacing equations (26) and (27) in eq. (25) and remembering that

$$Pr_w = \frac{\mu_w c_{pw}}{k_w},$$

one obtains eq. (1)

Pressure Drop in the Cooling Channels

The equations giving the pressure drop are as follows:

$$\Delta p_1 = f_m(2H/d_h)\rho_m v_m^2; \quad (28)$$

$$\Delta p_2 = \xi \rho_m v_m^2; \quad (29)$$

$$\Delta p_3 = F(T_1, T_2, p_1, p_2)\rho_m v_m^2, \quad (30)$$

where

$$f_m = \frac{0.046}{\left(\frac{\rho_m v_m d_h}{\mu_m}\right)^{0.2}} \quad (\text{Blasius's equation}); \quad (31)$$

$$F(T_1, T_2, p_1, p_2) = \frac{T_2 - T_1}{T_m} + \ln \frac{p_1}{p_2} + \frac{1.1T_1 - 0.55T_2}{2T_m} \quad (32)$$

(see Ref. 23, p. 40) and ξ is a dimensionless constant depending on the shape, size, and number of supporting grids.

Replacing equations (27), (31), and (32) in equations (28), (29), and (30), and taking into account that $d_h = 4A/P$ and $G = A\rho_m v_m$, one obtains equations (2), (3), and (4). When the units of the physical parameters on the right side of these equations are those of the cgs system, the resulting pressure differences are in dyne/cm². To have them in atmospheres it is necessary to multiply by $0.9869 \cdot 10^{-6}$.

Pumping Power

The total pumping power required to circulate the coolant in the primary coolant circuit is established by the characteristics of the whole primary circuit. The part of the total pumping power relative to the core is given by

$$\begin{aligned} N_t &= \frac{1}{\eta} \frac{G_t}{\rho_1} (\Delta p_1 + \Delta p_2 + \Delta p_3) \\ &= \frac{1}{\eta} \frac{Q_{th}}{c_{pm}(T_2 - T_1)\rho_1} (\Delta p_1 + \Delta p_2 + \Delta p_3). \end{aligned} \quad (33)$$

We define N_1 , N_2 , and N_3 as follows:

$$N_1 = \frac{1}{\eta} \frac{Q_{th}}{c_{pm}(T_2 - T_1)\rho_1} \Delta p_1;$$

$$N_2 = \frac{1}{\eta} \frac{Q_{th}}{c_{pm}(T_2 - T_1)\rho_1} \Delta p_2;$$

$$N_3 = \frac{1}{\eta} \frac{Q_{th}}{c_{pm}(T_2 - T_1)\rho_1} \Delta p_3,$$

so that $N_t = N_1 + N_2 + N_3$.

Using equations (2), (3), and (4), one obtains

$$\frac{N_1}{Q_{th}} = \frac{1.115}{\eta} \frac{(P'/P)^{1.8}}{(T_2 - T_1)^{2.8}} \frac{\mu_m^{0.2}}{\rho_1 \rho_m c_{pm}^{2.8}} \frac{HQ^{1.8}}{P'^{1.8} d_h^3}; \quad (34)$$

$$\frac{N_2}{Q_{th}} = \frac{16\xi}{\eta} \frac{(P'/P)^2}{(T_2 - T_1)^3} \frac{1}{\rho_1 \rho_m c_{pm}^3} \frac{Q^2}{P'^2 d_h^2}; \quad (35)$$

$$\begin{aligned} \frac{N_3}{Q_{th}} &= \frac{16}{\eta} \left(\frac{T_2 - T_1}{T_m} + \ln \frac{p_1}{p_2} + \frac{1.1T_1 - 0.55T_2}{2T_m} \right) \frac{(P'/P)^2}{(T_2 - T_1)^3} \\ &\quad \cdot \frac{1}{\rho_1 \rho_m c_{pm}^3} \frac{Q^2}{P'^2 d_h^2}. \end{aligned} \quad (36)$$

Replacing equation (1) in (34), (35), and (36), one obtains equations (5), (6), and (7). When the units of the physical parameters on the right side of these equations are those of the cgs system and the heat is in calories, the resulting N/Q 's are in erg/cal. To obtain the N/Q 's in dimensionless form it is necessary to divide by $4.187 \cdot 10^7$.

Appendix II

Relationship between T_1 , p_1 , and N_t/Q_{th} for Steam

With reference to Fig. 1 one can write that the power of the steam circulator is given by

$$N_t = \frac{(I_1 - I_0)G_t}{\eta_M}, \quad (37)$$

where η_M is the ratio between the power given to the steam in the circulator and the power at the shaft of the driving turbine. The ratio η_M takes into account of the mechanical losses in the power transmission from turbine to circulator and of the mechanical losses in the circulator itself. We assume $\eta_M = 0.91$. On the other hand, the core thermal output is given by

$$Q_{th} = G_t(I_2 - I_1). \quad (38)$$

Combining equations (37) and (38), one obtains

$$I_1 = \frac{I_0 + \eta_M \frac{N_t}{Q_{th}} I_2}{1 + \eta_M \frac{N_t}{Q_{th}}}. \quad (39)$$

Once T_1 , T_2 , and p_1 are known, N_t/Q_{th} , p_2 , and p_0 must be estimated. From p_0 and the condition $x = 1$ it is possible to obtain I_0 , and from T_2 and p_2 one has

I_2 . It is possible then to calculate I_1 from eq. (39), and from p_1 and I_1 one obtains T_1 . One can then with this value of T_1 make a calculation of the pressure drop Δp and pumping power necessary to circulate the steam in the primary circuit (see Appendix I) and check if the assumed values of N_t/Q_{th} and p_2 are correct. If not, the calculation must be repeated until they are.

The difference $p_2 - p_0$ takes into account the pressure drop in the primary circuit other than in the reactor, and it can be obtained by the relationship:

$$\frac{p_2 - p_0}{p_1 - p_2} = \frac{\text{pumping power required by reactor}}{\text{pumping power required by the rest of primary circuit}}.$$

Appendix III

Neutronic Computations

The objectives of the neutronic computations were to obtain the enrichment, breeding ratio, and basic safety parameters for each of the 17 cases. Since this involves a considerable amount of computation, simplifying approximations were applied which primarily effect the coolant-loss reactivity effect, but which, it is believed, give sufficient accuracy for this type of comparative study. These simplifying approximations are the following:

1. Calculations Were Performed in Spherical Geometry

This approximation is important to the computation of the coolant-loss reactivity effects, where core leakage makes an important contribution. The cylindrical cores considered (with $H/D \cong 0.7$) can be well simulated in spherical geometry.

2. No Changes Were Made in the Microscopic Group Constants Going from Coolant-in to Coolant-out Conditions.

With sodium coolant the elastic transfer cross sections of all materials in the range of the large sodium resonance ($\cong 3$ keV) change considerably when the sodium is removed. For the gas coolants considered here such strong resonances do not exist, so it is expected that this contribution to the coolant loss effect will be small.

3. The Total Coolant Loss Effects Will be in the Same Proportions as the Maximum Positive Coolant Loss Effects

The greatest positive reactivity effect due to coolant loss most likely occurs when only the center portion of the core is void of coolant. However, for the purpose of comparing coolants, the effect of total coolant removal from the core is sufficiently representative of the maximum positive value.

The plutonium isotopic composition used in the computations is that representative of fast reactor, namely, recycled plutonium which has reached equilibrium levels.⁽²⁴⁾ The fractions of Pu-239, Pu-240, and Pu-241 in the plutonium are 0.823, 0.159, and 0.018, respectively. A fission product pseudo-element representing long-lived fission products was included with an atom density corresponding to 50 000 MWD/t burnup in the fuel. The cross-section set used is the 26-group set generated in the Soviet Union and adapted for use at Karlsruhe.⁽²⁵⁾ Plutonium "enrichments" were set to give a k_{eff} value of 1.01, and the values given in Table 4 are based on total fuel plus fission product nuclear densities. In the computations of the Doppler coefficient, both a uniform temperature distribution and change in the fuel (i.e., an isotopic coefficient) were assumed.

Conversion from the actual cylindrical dimensions of the reactor cores to equivalent spherical dimensions was done by conserving core leakage in the fundamental-mode approximation, that is, the axial and radial bucklings, B_{ax}^2 and B_{rad}^2 , were estimated from

$$B_{ax}^2 = \left(\frac{\pi}{H + 40} \right)^2 \quad (40)$$

and

$$B_{rad}^2 = \left(\frac{2.404}{\frac{D}{2} + 20} \right)^2, \quad (41)$$

where H and D are the core height and diameter, respectively, and the 20-cm extrapolation lengths are approximate for fast reactors. The spherical core radii R_{sp} are then given by

$$\left(\frac{\pi}{R_{sp} + 20} \right)^2 = B_{ax}^2 + B_{rad}^2. \quad (42)$$

A spherical blanket 40 cm thick was used. The composition of this blanket was taken to be a leakage-weighted average of the axial and radial blanket compositions. Letting $N_{sp,m}$, $N_{ax,m}$, and $N_{rad,m}$ be the nuclear densities for material m in the spherical, axial, and radial blankets, respectively, the value of $N_{sp,m}$ was obtained from

$$N_{sp,m} = \frac{N_{ax,m}B_{ax}^2 + N_{rad,m}B_{rad}^2}{B_{ax}^2 + B_{rad}^2}. \quad (43)$$

The quantities $N_{ax,m}$ and $N_{rad,m}$ were determined by considering the axial blanket to have the same volume fractions as the core, and the radial blanket to have one-half the core coolant volume fraction and the same volume ratio of steel to fuel as in the core. Blanket fuel was taken to be pure U-238 oxide.

The coolant-loss reactivity effects were computed by running one-dimensional diffusion-theory problems with and without the coolant present in the core. Doppler computations were accomplished with the 900°C and 2100°C values of the Pu-239 and U-238 cross sections available in the 26-group set. The small Doppler contribution due to Pu-240 was not included. It was then assumed that the Doppler coefficient follows the expression

$$\frac{dk}{dT_f} = \frac{A_{\text{Dop}}}{T_f}, \quad (44)$$

where A_{Dop} is a constant and T_f is the average absolute temperature of the fuel. The values of A_{Dop} have been reported for the 17 cases considered in this study and form a basis for comparison of the Doppler coefficients.

Nomenclature

Geometrical Parameters:

A	= coolant-channel cross section (cm^2)
B_{ax}^2	= core axial buckling (cm^{-2})
B_{rad}^2	= core radial buckling (cm^{-2})
d	= fuel-element rod diameter (cm)
d_h	= coolant-channel hydraulic diameter (cm)
D	= core diameter (cm)
H	= core height (cm)
H'	= core extrapolated height (cm)
P	= coolant-channel wetted perimeter (cm)
P'	= coolant-channel heat transfer perimeter (cm)
R_{sp}	= radius of equivalent spherical core (cm)
z	= distance from the core inlet of the coolant cross section considered (cm)
z_M	= distance from the core inlet of the coolant cross section where the fuel surface temperature is maximum (cm)

Coolant Physical Properties:

c_{pm}	= specific heat at constant pressure at temperature T_m and pressure p_m (cal/gm-°C)
c_{pM}	= specific heat at constant pressure at temperature $T(z_M)$ and pressure p_m (cal/gm-°C)
c_{pw}	= specific heat at constant pressure at temperature T_{wM} and pressure p_m (cal/gm-°C)
$I(z)$	= enthalpy at cross section z (cal/gm)
I_0	= enthalpy upstream the coolant circulator (cal/gm)
I_1	= enthalpy at core inlet (cal/gm)
I_2	= enthalpy at core outlet (cal/gm)
x	= steam quality

k_w	= thermal conductivity at temperature T_{wM} and pressure p_m (cal/cm-sec-°C)
μ_m	= dynamic viscosity at temperature T_m and pressure p_m (gm/cm-sec)
μ_w	= dynamic viscosity at temperature T_{wM} and pressure p_m (gm/cm-sec)
ρ	= density at temperature $T(z_M)$ and pressure p_m (gm/cm ³)
ρ_1	= density at temperature T_1 and pressure p_1 (gm/cm ³)
ρ_m	= density at temperature T_m and pressure p_m (gm/cm ³)
ρ_w	= density at temperature T_{wM} and pressure p_m (gm/cm ³)

Other Physical Parameters:

G	= coolant mass flow in the central coolant channels (gm/sec)
G_t	= total coolant mass flow through core (gm/sec)
h	= heat transfer coefficient between fuel-element surface and coolant (cal/cm-sec-°C)
N_1	= pumping power required by friction losses in the core (MW)
N_2	= pumping power required by losses due to supporting grids in the core (MW)
N_3	= pumping power required by acceleration losses in the core (MW)
N_t	= total pumping power required by the core (MW)
$N_{\text{sp},m}$	= nuclear density of material m in spherical blanket (atoms/b-cm)
$N_{\text{ax},m}$	= nuclear density of material m in axial blanket (atoms/b-cm)
$N_{\text{rad},m}$	= nuclear density of material m in radial blanket (atoms/b-cm)
p_0	= coolant pressure in the coolant circulator upstream (atm)
p_1	= coolant pressure at core inlet (atm)
p_2	= coolant pressure at core outlet (atm)
p_m	= $\frac{1}{2}(p_1 + p_2)$ (atm)
Δp_1	= pressure drop required by friction losses in the core (atm)
Δp_2	= pressure drop due to grids supporting fuel rods (atm)
Δp_3	= pressure drop due to acceleration losses in the core (atm)
Δp	= $\Delta p_1 + \Delta p_2 + \Delta p_3$ (atm)
q	= heat to coolant per coolant channel and per unit length (cal/cm-sec)
q_0	= maximum value of q (cal/cm-sec)
Q	= heat output per coolant channel (cal/sec)
Q_{th}	= total core heat output (MW)

T_1	= absolute coolant temperature at core inlet ($^{\circ}\text{K}$)
T_2	= absolute coolant temperature at core outlet ($^{\circ}\text{K}$)
T_m	= $\frac{1}{2}(T_1 + T_2)$ ($^{\circ}\text{K}$)
$T(z)$	= absolute coolant temperature at coordinate z ($^{\circ}\text{K}$)
$T_w(z)$	= absolute fuel-element surface temperature at coordinate z ($^{\circ}\text{K}$)
T_{wM}	= maximum absolute fuel-element surface temperature ($^{\circ}\text{K}$)
T_f	= average absolute temperature of fuel ($^{\circ}\text{K}$)
v	= coolant velocity (cm/sec)
χ	= heat produced per unit length of fuel rod (cal/cm-sec)
τ_w	= shear stress at the wall (dynes/cm ²)

Dimensionless Groups:

f_m	= $\tau_w/\rho v/2$ = Fanning friction factor
Nu_w	= hd_h/k_w = Nusselt number
Pr_w	= $\mu_w c_{pw}/k_w$ = Prandtl number
Re_w	= $\rho_w v d_h/\mu_w$ = Reynolds number
α	= coolant fraction in fuel boxes
η	= efficiency of coolant circulator
η_M	= mechanical efficiency of coolant circulator
η_t	= total net efficiency of power plant
φ	= $(T_{wM} - T_1)/(T_2 - T_1)$
ν	= $\frac{1}{\sqrt{\left(2\varphi \frac{c_{pM}}{c_{pm}} - 1\right)^2 \sin^2 \frac{\pi H}{2H'} - 1}}$
ξ	= $\Delta p_2/(\rho_m v^2)$ = pressure drop factor due to grids supporting fuel elements

Constants:

Δ_{Dop}	= constant for $1/T_f$ variation of Doppler coefficient
c_1	= $dH^{0.5}/Q^{0.25}$
c_2	= $P'/d = \begin{cases} \pi & \text{(square fuel-element rod array)} \\ \pi/2 & \text{(triangular fuel-element rod array)} \end{cases}$
K_0	= defined in equation (1)
K_1	=
K_2	=
K_3	=
K_4	= $K_1(P'/P')^{0.6} + K_2(P'/P)^{0.4} + K_3(P'/P)^{0.4}$

Acknowledgment

The author wishes to thank Professor K. Wirtz, Director of the Institut für Neutronenphysik und Reaktortechnik, who encouraged him to perform the work.

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Discussion

Mr. Fell (Winfrith): I wonder if you would comment on the question that pressure drop is not fully a matter of thermal calculations of this kind. In particular,

with a steam-cooled reactor you're much more interested in keeping the hydrogen content down; you are going to want to work with smaller passages.

Mr. Dalle-Donne: As far as steam is concerned, nuclear considerations will be discussed in detail in the next paper from Karlsruhe, by Dr. Ott. I think he will be able to answer your question.

Mr. Wirtz (Karlsruhe): Could you tell us, if you have a choice from an economical point of view of the three coolants, which one would you select now?

Mr. Dalle-Donne: From consideration of the heat transfer performance in the core the steam is the best.

Mr. Fenech (MIT): I think it's a little meaningless to make a study of a choice of coolants on the basis of heat removal from the core because of other important factors that come into it, such as economics and safety; one cannot dissociate one from the other. Therefore, the selection of coolant should be done as a result of an overall study of reactor design and organization.

Mr. Dalle-Donne: Yes, this is only one point of the picture, but it is something which is in favor of steam.

Mr. Häfele (Karlsruhe): I think the paper of Dalle-Donne indicates that the differences between the cases of several surface temperatures smoothes out in case of high pressure, and particularly for steam. Therefore, it was important to have such studies to fix the situation. We have decided that in the case of steam-cooled breeders you should go to high pressures.

Mr. Dalle-Donne: I forgot to say one thing. The pressure drops in the core are very important, not only because of pumping power, but because if you have high pressure drops there are important problems concerned with vibration of the fuel elements. The rods have to be supported firmly, and this leads again to an increase of pressure drops; you have a sort of divergency, I think.

Mr. Fenech: There is a minimum pressure drop in the core required for a good distribution of coolant across the core itself. The question of pressure drop, which has entered through the percentile that you use for pumping power, is not very important from the point of view of economics if your fuel-cycle cost is low; then

you just don't care too much how much energy you're spending for pumping.

Mr. Dalle-Donne: I agree that the pressure drops themselves are not an economic question. The more vital question is one of safety. The fuel elements must not vibrate too much.

Mr. Fell: I wonder if I can come back to the point on roughened fuel elements. I think it's unlikely that one would design a fuel element for gas cooling without roughening. Therefore I am not quite sure whether I understand your conclusions to the differences between helium and steam, if you roughen the fuel element.

Mr. Dalle-Donne: The roughening is a rather curious thing in the sense that it does not allow us to decrease the pressure in pumping power. If you roughen, you increase the Fanning friction factor about three times, but the Stanton number only about two times. Thus, roughening completely the fuel element leads to a relation of pumping power to thermal power output which is worse than without roughening. But by partial roughening the ratio of Fanning friction factor to Stanton number remains about the same. As far as pumping power is concerned, there is no gain, but you can use bigger rods.

Mr. Cohen (GE): Would you care to comment on the possibility that perhaps different coolants might tolerate slightly different maximum temperatures?

Mr. Häfele: This is a very touchy and crucial point indeed. According to our present evaluation it makes a big difference whether you run the cladding material at 650, 700, or more than 700°C. At higher temperatures the helium created by (n,α) processes can move inside the structure materials. These formidable helium bubbles lead to a sudden decrease of ductility, but this can be avoided according to our latest results and irradiation experiments in BR-II, the Belgian Reactor, if you stay within certain temperature limits. A reactor running at 700 or 750°C not only has to face the difficulty of these lower creep strengths but also of the higher radiation damages. Therefore, I think it does have a strong feedback on the reactor design.

Survey Parameter Study of Large Steam-cooled Fast Power Reactors*

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I. INTRODUCTION

This paper gives an excerpt of our neutronic studies on steam-cooled fast power reactors dealing mainly with the reactivity behaviour at different coolant densities and its dependence on various parameters. It is the physics part of a feasibility study.

At the beginning of this study, and even nowadays, information from the literature concerning steam-cooled fast reactors was very sparse, a situation quite different compared to that for sodium, for which some extensive theoretical and experimental studies have been published. Furthermore, there are additional parameters of larger influence on steam cooling. For example neutron poisons and fission products may change the safety behaviour considerably. Also, the steam density is not as well-defined as the density of fluid sodium.

Bearing in mind all these facts, we started our work on large steam-cooled fast reactors with an extensive survey parameter study to get an idea of the influence of the important parameters on the reactivity behaviour and to determine, at least with certain approximations the meaningful regions in the parameter field within which feasible combinations of parameters may be found. Since the number of parameters involved is still large, even if one excludes those which were supposed to be of minor importance, we began with zero-dimensional calculations. With the aid of a few one-dimensional calculations we determined the physical differences between zero- and one-dimensional calculations so that we are able to take into account these differences in further calculations by means of a variation of the savings depending on the variation of the mean steam density. In the presentation of our zero-dimensional results we have disregarded this effect for simplicity reasons, since the main conclusions drawn from these results will be unchanged or only slightly modified.

All results presented here have been obtained with use of the group-constant set of Bondarenko *et al.*⁽¹⁾ with minor corrections. All calculations were done for

the homogeneous case only. The inclusion of heterogeneity effects will mainly alter the results for the flooded case. For the normal and voided state the influence of this effect is small.

In this paper we discuss the influence of the following parameters on the reactivity behaviour of large fast steam-cooled reactors:

1. choice of the coolant (H_2O or D_2O);
2. isotopic composition of plutonium;
3. addition of small amounts of poisons;
4. accumulation of fission products.

We will analyse mostly the results for different reasonable values of

- a) the geometric buckling B^2 ;
- b) the coolant volume fraction α ;
- c) the mean steam density under normal operating conditions (ρ_N).

Since the feasibility of large steam-cooled fast reactors will mainly be influenced by their safety behaviour, a set of safety conditions (section II) was used to find the limitations in the parameter field. The procedure for this is described in section III. In section IV the results are discussed.

II. DISCUSSION OF SAFETY CRITERIA

The conditions outlined in Table 1 are based on the following arguments:

1. In a loss-of-coolant accident the reactor should remain at least below the prompt critical level. In this reactivity range, shutdown rods are normally fast enough. Since in an accident the pressure may decrease rapidly, this condition seems to be the most important one.

2. Flooding of the reactor may be desirable in some situations (emergency cooling, afterheat removal, refueling). The reactor can be considered as safe if it is subcritical in this state. One could also discuss $\Delta k_F < \beta$, but this condition would give only a small enlargement of the allowed parameter region. The possibility of safe shutdown, so as to compensate for a larger positive Δk_F , is not assumed in this paper.

3. At normal mean steam density, ρ_N , the density coefficient of reactivity, should be positive to get a

* This paper has been prepared within the framework of the association EURATOM—Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

TABLE 1. THE SET OF SAFETY CONDITIONS

1. Loss of coolant (normal coolant density = ρ_N):
$\Delta k_L = k(\rho = 0) - k(\rho_N) < \beta$.
2. Flooding:
$\Delta k_F = k(\rho = 1) - k(\rho_N) < 0$.
3. Density coefficient under normal operating conditions
$(dk/d\rho)_N > 0$.
4. Maximum reactivity:
$\Delta k_{\max} < \beta$.

(We assumed in our analysis for the effective fraction of delayed neutrons $\beta = 0.0035$, disregarding a system dependence for simplicity.)

self-stabilizing behaviour of the reactor with respect to small density or temperature fluctuations. In accidents a negative temperature coefficient of the coolant may also be helpful.

4. The reactivity will have a relative maximum between the normal density and the flooded case if conditions 2. and 3. are satisfied. It may be desirable to have this maximum reactivity lower than \$1. However, the increase of the coolant density from normal operation density to the flooded state can presumably occur only artificially, so that sufficient control during this procedure may be guaranteed. Therefore, condition (4) is the weakest one and is ignored in most cases.

III. TYPE OF ANALYSIS OF THE RESULTS

It seemed instructive to represent the subregions in which the safety conditions are fulfilled basically in an (α, B^2) -plane, where α is the coolant volume fraction and B^2 the buckling. All the other parameters are kept constant for such a diagram. Different combinations of the other parameters lead to different diagrams in the (α, B^2) -plane. Criticality is achieved by a suitable fissile-to-fertile ratio.

Determining the curves in the diagrams we proceeded along the following line:

1. Calculation of $k(\rho)$, the multiplication factor for different coolant densities in the range of $0 \leq \rho \leq 1$ g/cm³ for any set of the parameters α, B^2 , etc.
2. From these curves we read the quantities involved in the safety conditions [$\Delta k_L, \Delta k_F, \Delta k_{\max}, (dk/d\rho)_N$]. From a plot of these quantities as functions of B^2 , the special values of B^2 where the safety conditions are matched are determined by extrapolation or interpolation.
3. A plot of these values of B^2 for different α -values then gives the limiting curve in the (α, B^2) -plane.

We have used here only two different values of α , so the limiting curves are straight lines in this approximation. However, this may be improved easily.

IV. DISCUSSION OF THE RESULTS

A. Zero-dimensional Results

1) Choice of Coolant (H_2O or D_2O)

The two most important safety conditions are $\Delta k_L < \beta$ and $(dk/d\rho)_N > 0$. As the example in Fig. 1 shows, these two most important conditions are heavily shifted in an unfavourable direction by replacing H_2O with D_2O . Contrary to this, Δk_F is influenced favourably. However, this can better be reached by accumulation of fission products or by additional neutron poisons.

From these results we decided to consider only H_2O in the further investigation.

The physical reasons for this different behaviour of H_2O and D_2O may be understood from the following arguments:

At a first glance, one should expect a somewhat lower value for Δk_L in the case of D_2O than in the case of H_2O because of the less pronounced spectral shift. The calculations showed that for k_∞ (i.e., buckling $B^2 = 0$) one indeed gets the expected result. For a reasonable value of B^2 , however, the change in the leakage probability, which gives a negative contribution to Δk_L , is much smaller for D_2O than for H_2O because of the smaller D_2O transport cross section and the less softened spectrum in the case of D_2O steam. This effect prevails over the k_∞ -effect, and in this way we get better results with respect to Δk_L and $(dk/d\rho)_N$ for H_2O than for D_2O steam as the coolant. This was the main reason for preferring H_2O to D_2O as the coolant for our study, with the costs being a second argument.

In connection with Fig. 1 it should be noted that the addition of D_2O decreases Δk_F to a large extent. This is mainly due to the fact that in the flooded case for D_2O the bulk of the neutron spectrum is in the range of low η values, whereas for H_2O a large number of neutrons has passed this range and is able to take advantage of the higher η value in the thermal range. A second fact which acts in the same direction is the higher leakage rate for the flooded D_2O reactor.

2) Isotopic Composition of Plutonium

The influence of the isotopic composition of plutonium on the reactivity and safety behaviour of steam-cooled reactors may be illustrated by comparison of the curves in Fig. 2. The calculations were performed for the same volume ratio of fissile to fertile atoms, all parameters other than the plutonium composition being equal. It can be seen that for pure Pu-239 there is the well-known reactivity increase in the flooded case of about 20–30 %, whereas for the “dirty” plutonium in a certain range of isotopic compositions the reactivity

FIG. 1. k_{eff} as Function of Steam Density for Different H_2O - D_2O Mixtures. FIG. 2. k_{eff} as Function of Steam Density for Different Plutonium Compositions.

behaviour is essentially flat for all coolant densities. This effect is mainly due to the high absorption rate in the 1-eV resonance of Pu-240, as has been shown by detailed calculations. In the early study of Sofer,⁽²⁾ this

effect was not found, perhaps because of a too coarse group structure.

Calculations were made for two isotopic compositions

$$\text{Pu}^{239}:\text{Pu}^{240}:\text{Pu}^{241}:\text{Pu}^{242} = 63:30:5:2$$

FIG. 3. In the Shadowed Area of This (α, B^2) Plane the Safety Conditions for Flooding and Loss of Coolant Are Fulfilled, (α = volume fraction of coolant; B^2 = buckling; operational steam density $\rho_N = 0.04$ g/cc.)

and

$$\text{Pu}^{239}:\text{Pu}^{240}:\text{Pu}^{241}:\text{Pu}^{242} = 76:21:2.3:0.7.$$

These compositions have been calculated for two representative refueling conditions:* one in which plutonium bred in the blanket is fed to the core composition in an amount which is necessary to keep the reactor just critical, and one in which plutonium of the core and all the plutonium bred in the blanket is mixed during reprocessing, this mixture giving the new plutonium composition of the core.

As a result of our calculations and the evaluation schema described above, we obtained Figures 3, 4, and 5, for mean steam densities of 0.04, 0.0735, 0.125 g/cm³, respectively, corresponding to pressures of 100, 150, and 250 atm. The plutonium composition used was 63:30:5:2. The other one gave a positive flooding coefficient in all interesting cases and was therefore disregarded in this first step.

The fact that the safety behaviour (of Δk_L , Δk_F ...) is essentially combined with the long-time behaviour (of the plutonium composition) gives rise to further complications in the design of steam-cooled reactors.

Each of these figures shows the (α, B^2) region in which

* For these calculations the formulae of Ref. 3 were specialized for 1000-MWe fast reactors by the use of improved cross sections.

the conditions concerning Δk_L and Δk_F can be satisfied. The density coefficient is positive in all these areas, thus giving no additional limitation. The wish to have also $\Delta k_{\max} < \beta$ can only be realized for the highest steam pressure in a small subregion of the area shown in Fig. 5.

The main results of these three figures are:

- Using very dirty plutonium, it is possible to satisfy the conditions $\Delta k_L < \beta$ and $\Delta k_F < 0$ without using poisons.
- The area where it is possible to fulfil conditions 1-3 of Table 1 grows considerably with increasing steam pressure; at the highest pressure all conditions 1-4 may be fulfilled in a small subregion of the interesting (α, B^2) region.

The results of our calculations for the other plutonium composition showed that, apart from the fact that $\Delta k_F > 0$, it is the more favoured one with respect to Δk_L and $(dk/d\rho)_N$, a result which may already be concluded from Fig. 2.

3) Addition of Small Amounts of Absorbers

Plutonium of an isotopic composition in the neighbourhood of 76:21:2.3:0.7 is interesting since it belongs to a combined core-blanket reprocessing. As was mentioned in the last section, such a plutonium composition gives a reactivity increase in the flooded case due to the

FIG. 4. In the Shaded Area of This (α, B^2) Plane the Safety Conditions for Flooding and Loss of Coolant Are Fulfilled. (α = volume fraction of coolant; B^2 = buckling; operational steam density $\rho_N = 0.0735 \text{ g/cc.}$)

FIG. 5. In the Shaded Area of This (α, B^2) Plane the Safety Conditions for Flooding and Loss of Coolant Are Fulfilled. (α = volume fraction of coolant; B^2 = buckling; operational steam density $\rho_N = 0.125 \text{ g/cc.}$)

smaller amount of Pu-240 present. It is natural that one looks for other thermal or epithermal resonance absorbers which will produce a similar effect as Pu-240 when the coolant density is near or equal to the maximum value of 1 g/cm^3 . Such poisons may be, for example, B, Cd, Eu, Gd, Hf, and Sm. We studied the changes in the reactivity behaviour caused by the

addition of Eu, Gd, and Hf, and will present here only the results obtained for Gd_2O_3 . Gadolinium has been chosen for several reasons:

- a) It has the best influence from the physics point of view, i.e., high influence on Δk_F , low influence on Δk_L —this fact also indicates a low reactivity penalty in the normal states.

b) The amount necessary to obtain the desired influence on Δk_F is the smallest one compared with Hf and Eu; in the calculations discussed below the volume fraction of Gd_2O_3 used was $5 \cdot 10^{-4}$ of the whole core volume.

c) It is comparatively cheap poison, with respect to the fraction necessary perhaps the cheapest one.

The results obtained for the addition of 0.05 % Gd_2O_3 are given in the Figures 6, 7, and 8. As in Figures 3, 4,

and 5, the results are shown for three mean steam densities corresponding to three different pressures (100, 150, and 250 atm). It can be seen that by the addition of poison a region in the (α, B^2) plane can be found where it is possible to fulfil the safety criteria 1-3 or 1-4 of Table 1. Comparing Fig. 3 with Fig. 6, or 4 with 7, or 5 with 8, one recognizes that the "less dirty" plutonium together with some suitable poison has a larger "allowed" region in the (α, B^2) plane than the "more

FIG. 6. In the Shadowed Area of This (α, B^2) Plane the Safety Conditions for Flooding and Loss of Coolant Are Fulfilled. (α = volume fraction of coolant; B^2 = buckling; operational steam density $\rho_N = 0.04 \text{ g/cc.}$)

FIG. 7. In the Shadowed Area of This (α, B^2) Plane the Safety Conditions for Flooding and Loss of Coolant Are Fulfilled. (α = volume fraction of coolant; B^2 = buckling; operational steam density $\rho_N = 0.0735 \text{ g/cc.}$)

FIG. 8. In the Shadowed Area of This (α, B^2) Plane the Safety Conditions for Flooding and Loss of Coolant Are Fulfilled. (α = volume fraction of coolant; B^2 = buckling; operational steam density $\rho_N = 0.125$ g/cc.)

dirty" plutonium under the same safety conditions. This fact is due to the fast fission in Pu-240.

4) Accumulation of Fission Products

In the discussion above we have excluded the influence of fission products. These have partially similar nuclear properties as the poisons considered—some fission products are nuclei identical or very similar to the poison material. Because the burnup of ceramic-fueled fast power reactors is very high, one should include the fission products in the consideration of the reactivity behaviour under normal operating conditions. In the following calculations 50,000 MWd/to is used as an average burnup of the fuel in the core.

We give the following comments with respect to this section: The group constants used for the fission products of Pu-239 have been taken from the group-constant set of Bondarenko.⁽¹⁾ As cited there, the following isotopes were omitted: Cd-113, Sm-149, Sm-151, Gd-155, and Gd-157. These fission products have very high thermal cross sections and are stable or long-lived. Their concentrations in an equilibrium state depend strongly on the spectrum of the reactor. Detailed calculations for every reactor considered will be necessary to get the right concentrations. Since we are at present only looking for a feasible reactor and even the pressure, which influences the spectrum markedly, is not yet fixed, it seems an unnecessary complication to determine these concentrations exactly.

Bearing in mind the partially large uncertainty connected with our knowledge of the cross sections for

fission products in the fast-neutron range, we proceeded in the following manner. First, we tried to determine a favourable region in the (α, B^2) plane with the same method as for the addition of poison. As we expected, the buckling had to be increased considerably to satisfy the condition $\Delta k_L < \beta$. With increasing buckling the value of Δk_F increases, too, keeping all the other parameters but the enrichment constant. At these high values of the buckling (about $2 \cdot 10^{-3} \text{ cm}^{-2}$), it turned out that even with the addition of the fission products corresponding to 50 000 MWd/to but without the above-mentioned isotopes (with high thermal cross sections), there may occur a slight reactivity increase upon flooding. The omission of these isotopes gives a too pessimistic value for Δk_F and a slightly too optimistic one for Δk_L . We simulate these thermal poisons by an addition of $1.6 \cdot 10^{-2}$ v/o Gd_2O_3 of natural isotopic composition. In other words, this means that about one per cent of the fission products are assumed to be natural gadolinium or about 0.3% to be either Gd-155 or Gd-157, a figure not too unrealistic if one considers that the other thermal poisons have been neglected.

The results for high pressure (250 atm) are given in Fig. 9 as an allowed region in the (α, B^2) plane. For low pressure there does not exist such a region, and for the intermediate pressure the region is very small.

For the exact determination of the reactivity behaviour of a steam-cooled fast reactor with moderate or high burnup, the cross sections for the fission prod-

FIG. 9. In the Shadowed Area of This (α, B^2) Plane the Safety Conditions for Flooding and Loss of Coolant Are Fulfilled. (α = volume fraction of coolant; B^2 = buckling; operational steam density $\rho_N = 0.125$ g/cc.). Average burnup = 50,000 MWd/to.)

ucts should be known with higher accuracy. Especially for the flooded case the yield of fission products which leads to thermal or epithermal absorbers should be known reasonably well, so that by means of the known cross sections and the spectrum of the reactor considered their concentrations may be calculated.

B. One-dimensional Results

The Variation of Savings with Coolant Density

We confine ourselves only to that part of our one-dimensional calculations concerned with or to be interpreted as modifications of the zero-dimensional calculations discussed above. As is well-known, one may calculate at least approximately the criticality of a blanketed reactor by means of zero-dimensional calculations using the right savings corresponding to the blanket considered. Unfortunately, the savings varies appreciably with coolant density, so that instead of a constant buckling one should use in the zero-dimensional calculations a buckling varying with the density of the coolant. For the determination of the buckling or savings variation, we performed one-dimensional calculations for a few standard blankets having the same core composition and varying only the coolant density.

The variation of the savings with steam density has been determined by adjusting at each coolant density the buckling in the zero-dimensional calculations to give the same value of k_{eff} as was obtained by the one-dimensional calculations. Knowing the buckling, it is quite easy to calculate the saving and its variation

with coolant density for the special blanket considered. Two typical results are shown in Fig. 10.

It is very instructive to recognize how sensitively the Pu-239 accumulated in the blanket influences the savings at high coolant densities. In the flooded case the blanket containing Pu-239 acts mainly as a thermal "core," thus producing a considerable reactivity increase. Since buildup of Pu-239 and some Pu-240 in the blanket will occur during one core life, the dependence of the savings upon coolant density will change during one core life too. This will cause an increase in Δk_F and a decrease of Δk_L during the core life. However, this will be compensated or over compensated by the simultaneous build-up of fission products in the core.

FINAL DISCUSSIONS

A. Remarks Concerning the Accuracy

It has been mentioned in section III that we have used only two values of the coolant volume fraction and two or three values of the buckling to determine the allowed regions in Figures 3–9, so that the boundaries are straight lines.

The value of Δk_F is important for the determination of the allowed region in the (α, B^2) plane. This implies that one has to take into account quite accurately the heterogeneity effect, since it tends to increase the reactivity in the flooded case and in this way also Δk_F . In the normal and voided cases the influence of the heterogeneity effect will be smaller and, fortunately, tend to decrease Δk_L .

Fig. 10. Savings as Function of Steam Density for a Typical Large Steam-cooled Fast Reactor. Blanket 1 includes 1.59% bred plutonium-239; blanket 2 is without fissionable material.

Apart from these simplifications, which may be easily improved, there are other effects which result in inaccuracies or influence the reliability of the results. Two important ones will be discussed in the following.

1. From Fig. 10 one recognizes that the buckling is not really independent of coolant density as it was assumed to be in all our calculations. If a density-dependent buckling is considered by use of density-dependent savings, it is possible to improve the accuracy of the zero-dimensional results. However, as may be seen from Fig. 10, the savings strongly depend on the plutonium content of the blanket. The amount and the isotopic composition of plutonium bred in the blanket vary remarkably during core life, and this variation is not known a priori. To determine this plutonium variation in the blanket one has to treat the full burnup problem. Since it seems unreasonable to do so in a parameter study, there remains some uncertainty due to this effect which may not simply be eliminated using one- or two-dimensional calculations.
2. With respect to the fission products one should bear in mind that the cross sections are fairly uncertain in the high-energy region. With respect to quantities as Δk_L and $(dk/d\rho)_N$ this generates uncertainties coming mainly from this energy region. Furthermore, the yield and concentration of long-lived fission products has to be known

with higher accuracy to get reliable results for Δk_F .

B. Summary of Results and Conclusions

The most important quantities with respect to the safety behaviour of a steam-cooled fast power reactor in our opinion are Δk_L and $(dk/d\rho)_N$, although Δk_F also has great importance.

Since H_2O gives more favourable values for Δk_L and $(dk/d\rho)_N$ than does D_2O , we chose H_2O -steam as the coolant medium for further investigations.

We found that the presence of Pu-240 has a marked influence upon the quantities used for the safety criteria: Δk_L and $(dk/d\rho)_N$ will be changed in the unfavoured, Δk_F in the favoured direction with increasing Pu-240 concentration.

The desired change in Δk_F which is caused by the epithermal resonance absorption in Pu-240 may also be obtained by the addition of poisons or the accumulation of fission products, but with a less pronounced unfavourable influence on Δk_L and $(dk/d\rho)_N$. The last fact is due to the absence of fast fission in these materials.

The results have shown that one may influence the two most important quantities mentioned above in the desired direction if one

1. increases the buckling;
2. increases the coolant volume fraction;
3. increases the pressure or the mean steam density under normal operation conditions;

4. uses "less dirty" plutonium;
5. reduces the average concentration of fission products.

As a general result one concludes that the allowed region increases with increasing operating pressure and is shifted towards higher values of buckling with increasing burnup. The last fact presumably leads to three alternatives:

1. At high power and high burnup, an annular or modular core has to be used.
2. With a normal core at high power, the burnup has to be reduced.
3. With a normal core at high burnup, the power has to be reduced.

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Discussion

Mr. Fell (Winfrith): From the earlier meetings I understand that you have replaced the Russian set by a new data set. Were the conclusions of this study stable to the exchange of data sets? Secondly, in any analysis of coolant condition requirements, I would have expected to see some requirement for maintaining a breeding gain. This could leave you, for example, with bigger areas if you increase the density of the coolant as you increase the pressure.

Mr. Ott: The Karlsruhe group-constant set was prepared by weighting a spectrum typical for a 1000-MW sodium-cooled reactor, and here we have a steam-cooled reactor. Such a spectrum is also somewhat different from zero steam density to the full flooding state. Therefore, in this respect it is not easy to make a good group-constant set for such reactors by use of a typical weighting spectrum. We started these calculations before the Karlsruhe group-constant set was in operation; that is why, for all these calculations, the Russian group-constant set was used. Clearly the overall criticality will change if the Karlsruhe cross sections are used; however, the density coefficients would presumably not change too much. To your second question, we are fully aware of the fact that the breeding ratio and especially the breeding gain are very important quantities for fast reactors, and that all of our calculations are zero-dimensional. By these we can only get

the internal breeding ratio, and we have in this case calculated that the internal breeding ratio is larger than one. Therefore, we hope that a reasonable blanket will give at least more than 1.2 for the breeding ratio. However, the real value depends on the choice of shape, if annular or not. The two-dimensional calculations are under way.

Mr. Greebler (GE): The void calculations you made were all made with a constant buckling, that is, the buckling with the steam in and the steam out were the same. When we did such calculations and then went to one dimension, the effect of the steam on the so-called reflector effectiveness was very very large. One gets a substantial increase of the buckling when one considers the removal of steam from the core, and we found that while core thicknesses of the order of $1\frac{1}{2}$ to 2 ft were adequate, to get zero steam void conditions, one had to go to much thinner cores depending on the volume fraction of steam and pressure, etc., than what we had estimated from the zero-dimensional calculations. Do you have results?

Mr. Ott: Those are the results shown in Fig. 10. The savings are given as a function of the steam density in the core between zero and one gram per cubic centimeter for two kinds of blankets. The savings as a function of steam density for a clean blanket is given in the lower curve. Going from normal operation density to the voided state one has a decrease in savings; this means a decrease in reactivity and an increase in voiding Δk . However, if one considers the case in which a reasonable amount of plutonium is already bred in the blanket, one gets the upper curve of this diagram, so that savings as a function of the density is strongly dependent on the plutonium content of the adjacent blanket, that is, of the blanket which is very near the core.

Mr. Heck (WAPD): I wanted to concur with Dr. Geebler; we found the same thing in voiding calculations. It was necessary to adjust the buckling for steam, and we also found fairly significant changes when we switched from a sodium-suitable set to a water-suitable set of cross sections. Not only did we have to change the magnitude of the cross sections, we found it was also quite worthwhile to change the entire energy structure of the group set in order to describe better what was going on in this sort of a pseudo-thermalized system.

Mr. Ott: We also have a group structure which goes to thermal energies.

Mr. Wall (GE): In our studies we find that the savings or the change in the bucklings when one goes from the voided state to the operating state depend on the energy: the high-energy bucklings do not

change very significantly, but the low-energy bucklings do. What were your savings and your B^2 changes, thermal, fast, and average? Secondly, when you allow for this, does your system still look attractive?

Mr. Ott: We have used only one effective average savings.

Mr. Thompson (MIT): I gather that at some time in its life, the reactor goes from the completely voided conditions for normal operation into the flooding region. If so, as a practical matter, how will you determine

that you don't get a reactivity jump greater than the delayed neutron fraction in the reactor after you've actually built it?

Mr. Ott: In most cases presented here, $\Delta k_{\max}$ is larger than a dollar. Some are two dollars or more, and this may be still more if you have not a homogeneous density distribution as we calculated here. However, these cases can occur only if a controlled flooding is carried out, so that good control during this short procedure may be guaranteed.

The Flood Safety of Steam- and Gas-cooled Fast Reactors*

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1. Introduction

An important safety consideration in steam- and gas-cooled fast reactors is the change in reactivity for various degrees of steam-water density. Fast reactors employing these coolants have a potential flooding accident, and deliberate flooding of the reactor core is frequently proposed for refueling. Thus, the safety feasibility of these concepts depends upon the neutronics of flooding and the degree to which the associated reactivity effects can be minimized.

The reactivity effects resulting from an increase in hydrogen density are twofold: a decrease in neutron leakage from the core, and a softening of the neutron spectrum. The neutron-leakage effect is always positive and is most important in small systems with their attendant high leakage. The sign and magnitude of the spectrum effect depends upon the fuel type (uranium or plutonium), composition (Pu-240 fraction), and the floodable fraction of the core. Hence, the magnitude and even the sign of the net resultant reactivity change depends on a subtle balance between contributing terms, and its accurate prediction for a wide variety of cases is, therefore, a stringent test of calculational methods and basic cross-section data.

For most first-generation steam- and gas-cooled fast reactors of potential economic interest,⁽¹⁻⁴⁾ the system sizes are such that upon uniform flooding, the reactivity would increase appreciably because the decrease in leakage is the predominant effect. In order to reduce or eliminate such a reactivity increase, the incorporation of resonance and/or thermal poisons into the fueled regions has been proposed.⁽⁵⁻⁸⁾ The addition of poison materials greatly increases the overall capture-to-fission ratio in the softer flooded spectra. It is believed in principle that by the incorporation of suitable poisons in the correct amounts and distributions, the reactivity of any reasonable system can be held within tolerable limits for all degrees of uniform flooding.

Figure 1 illustrates some of the effects involved. We have compared calculated results from various sources for three systems of varying sizes:

1. a relatively small, steam-cooled Mixed Spectrum Superheat Reactor (MSSR);⁽⁹⁾
2. a large MSSR;⁽¹⁰⁾
3. a gas-cooled fast reactor studied by General Atomics.⁽²⁾

The effect of various amounts of uniformly distributed poisons such as boron, europium oxide, and gadolinium is shown in Fig. 1. The choice of control poison depends upon the fuel type and the system under consideration. The ideal poison should have zero reactivity worth under operating conditions and a high absorption under flooded conditions. A low concentration is also a very desirable feature.

In a prototype steam-cooled reactor, due to its small size, the decrease in leakage predominates, and flooding without poison present would cause a large reactivity increase. In such a system the floodable volume fraction would not be very large, so that upon flooding the spectrum softens into the resonance region, but the thermal flux remains relatively insignificant. Thus, poisons having large epithermal resonances, such as europium and hafnium, are more effective than thermal poisons. "Effective" here means high hold-down under the flooded condition with a relatively small penalty in the operating condition.

In a large steam-cooled fast reactor fueled with equilibrium recycle plutonium (~30% Pu-240), the neutron-spectrum effect is much more important than for a smaller U-235-fueled system. The coolant volume fraction would probably be higher in such a system, which, in turn, causes substantial thermal flux in the flooded condition; for U-235 or pure Pu-239 this gives rise to a reactivity increase. However, calculations show that the presence of Pu-240, with its 1-eV resonance, effectively reduces reactivity change to a small value. Finally, a gas-cooled fast reactor has an even larger coolant volume fraction, and the spectrum change from the operating state to fully flooded is very great. Calculations have suggested that gadolinium is probably the most effective poison for such systems.

* Work sponsored by U. S. Atomic Energy Commission under Contract AT(04-3)-189, P.A. 13.

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FIG. 1. Flooding Effect in Fast Reactors.

Calculations of the flooding effects are very difficult to perform with any great reliability because of all the different effects involved; handling the spectrum calculations is itself a large problem. The uncertainty in resonance poison cross sections and the generation of reliable spectrum-averaged group constants are the two main difficulties.

The above discussion shows that the reactivity change upon flooding of steam- or gas-cooled fast reactors is a complicated effect. The sign and magnitude of the reactivity changes influence the design and even the feasibility of a concept. It is, therefore, obvious that before any serious consideration is given to this use of resonance poisons, sufficient detailed experimental information should be obtained to test thoroughly the feasibility of the technique and the calculational methods. The only experimental information available to date is that generated as part of the Mixed Spectrum Superheat Critical Experiment (MSSCE) program.⁽¹¹⁾

2. Experimental Information

2.1 Measurement System

In the work on the Mixed Spectrum Critical Assembly (MSCA), the incorporation of resonance poisons into a U-235-fueled fast core has been studied experimentally and theoretically as a means of ensuring flooding safety. Although the experiments and calculations were directed toward a specific reactor concept, the results are of general interest in the field of flood safety of steam- and gas-cooled fast reactors. In

particular, these are the first experimental measurements demonstrating the relative effectiveness of resonance poisons such as hafnium, europium, and boron.

The experiments have ranged from relatively small poisoned-flooded regions within a fast core to the flooding of relatively large fractions of the fast core containing resonance poisons.

Figures 2 and 3 show the general features of the MSCA which was built at Vallecitos Atomic Laboratory in support of the Mixed Spectrum Superheat Concept. It contains four distinct fuel regions:

- a central fast core, nominally 23 w/o enriched uranium as UO_2 , Inconel clad;
- a fast buffer, 1.3 w/o enriched uranium as UO_2 , stainless steel clad;
- a thermal buffer, natural UO_2 clad with aluminum and slightly boron poisoned;
- a thermal core, 1.8 w/o enriched uranium as UO_2 , aluminum clad.

Figure 4 shows a typical fast-core fuel bundle when loaded with polyethylene and poison rods. The total floodable volume within a fast-core bundle is about 19%. Polyethylene in the form of small-diameter rods and tubes could be introduced into the interstitial regions between fuel rods. In many cases, the bundles were flooded with water. By using combinations of polyethylene and water, the reactivity of the system could be measured with various volume fractions of water equivalent between 0 and ~19%.

Three types of poison rods were used in the MSCA fast-core flooding experiments. The poisons were boron

FIG. 2. Plan View of MSCA.

carbide clad in stainless steel, europium oxide similarly clad, and hafnium in metal wire form. Figure 5 shows the construction of a typical poison rod. The poison length was 30 in. corresponding to the length of the fueled region in the fast core. Each end was closed by 9-in.-long stainless steel filler plugs, corresponding to the axial Inconel reflector regions of the fast core.

2.2 Results of Measurements and Calculations

In the initial experiments, polyethylene was inserted in various radial and axial locations in the fast core. The polyethylene was in the form of sixteen rods (64.97 gm total), each 6 in. long and of 0.235-in. diameter. The polyethylene was placed in the interstitial regions between fuel rods as shown in Fig. 4. Pieces of the regular Inconel filler rods of various lengths were used to support the polyethylene at different axial locations.

Reactivity measurements were made with the polyethylene sections at levels 1 (0–6 in.), 3 (12–18 in.), 5 (24–30 in.), and 7 (36–42 in.) as shown in Fig. 6, and were repeated at different radial locations to determine the radial dependence.

Figure 7 shows the data obtained along with corresponding values obtained from four-group, two-dimensional calculations. The reactivity values have been expressed in cents/kg polyethylene. Both experiment and calculation show that the worth of hydrogenous material is a very strong function of location within the fast core. It is also of interest to note that the worth of hydrogenous material actually goes negative in the axial reflector regions. This effect has been noticed before for other fast systems⁽¹²⁾ and is due to the thermalization of neutrons in the reflector region. Once a neutron is thermalized in the reflector,

FIG. 3. Elevation of Mixed Spectrum Critical Assembly.

its chances of parasitic capture are very great, especially in the MSCA, where the reflector is Inconel.

Figure 8 shows the results of four-central-bundle flooding measurements, with hafnium as a poison, for reactivity hold-down. It is clear that the flooding characteristic is favorable in that for a relatively small reactivity penalty, the amount of reactivity hold-down is very large. Figure 9 shows similar sets of data when boron carbide and europium oxide were used. The calculated data are shown as squares on the graph; the excellent agreement between calculation and experiment is most encouraging. Perturbation-theory calculations were found to be satisfactory for flooding small regions without poison, but a separate diffusion calculation was required for the flooded-poisoned state, especially for the larger perturbed regions.

The results of the measurements with four central bundles make a comparison of the effectiveness of the three poisons possible. It is clear that hafnium has the greatest flooding reactivity hold-down power and also the least penalty in the dry case; therefore, the "figure of merit" on this basis is highest for hafnium. Europium oxide appears somewhat less effective than hafnium, whereas boron carbide seems to have a relatively large penalty and relatively less hold-down power.

Table 1 shows various aspects of these poisons. Although hafnium looks best from the physics standpoint, it is relatively weak, that is to say, the required volume is quite large, so that if this poison were to be used in the form of hafnium oxide, 5 to 6% of the total core volume would be occupied by poison, which is clearly a large economic penalty in that proportionally

FIG. 4 Typical Bundle Loading

FIG. 5 Details of MSCA Poison Rods

less fuel can be accommodated. The same table also presents some rough estimates of the cost effectiveness of the three poisons (cost multiplied by the amount required for a given reactivity hold-down). On this basis B_4C is very favorable, however, there are doubts about the compatibility of B_4C in fuels, and its migration during fuel manufacture and under operating conditions. Perhaps an arrangement whereby the poison and fuel are kept separate, as suggested recently by Edlund,⁽³⁾ would be most appropriate for application of B_4C .

The next series of experiments investigated the flooding effects in sixteen central bundles as shown in Fig

10. These experiments determined how the flooding compensation effect of europium oxide varied as a function of the size and shape of central regions which were both poisoned and flooded. The results are shown in Fig. 11. Each standard bundle was loaded with about $0.093 \text{ gm Eu}_2\text{O}_3/\text{cm}^3$ and about 18 v/o water. The reactivity change (per bundle), from the unperturbed condition, is plotted as a function of the size and shape of the flooded-poisoned region. At the top of the figure the various flooded regions are illustrated relative to core center. It is clear that as the size of the flooded region increases the effectiveness of the poison gets greater. On this basis, it is expected that a considerably

FIG. 6. Schematic Diagram of Experimental Bundle Used or Flooding-Poisoning Experiments.

FIG. 7. Worth of Polyethylene vs Location.

smaller poison concentration would be needed for the case of whole-core flooding.

In order to verify that less poison density would be needed for reactivity hold-down at the edge of the fast core than at the center, an annular region of flooded and poisoned bundles was constructed as shown in Fig. 12. The net (per bundle) reactivity change due to flooding and poisoning a bundle at the edge of the fast core was only about +2 cents even though a much smaller poison concentration was used.

A summary of some experimental and calculated results is presented in Table 2. The calculations were performed with the use of 24-group diffusion theory in one-dimensional cylindrical geometry. The fissile and fertile cross-section data used were essentially those reported up to the 1964 Geneva Conference. Due to the

FIG. 8. Four-bundle Flooding with Hafnium Poison.

FIG. 9. Four-bundle Flooding with Eu₂O₃ and B₄C Poisons.TABLE 1. RESONANCE POISON EFFECTIVENESS
(Based on Data from 4-central-bundle Flooding Experiments)

Poison	Amount ^(a) (gm/cm ³)	Relative Penalty ^(b)	Volume Fraction ^(c) (%)	Relative Cost ^(d)
Hf	0.46	1.0	3.5	120
Eu ₂ O ₃	0.098	1.3	1.3	190
B ₄ C	0.047	1.5	1.9	1.0
HfO ₂	0.54	1.0	5.5	120

^(a) Denotes amount for same reactivity hold-down at 19 v/o H₂O.

^(b) Relative reactivity penalties in dry condition.

^(c) Derived assuming *theoretical densities*.

^(d) Relative cost of material multiplied by the amount required for equivalent hold-down.

hardness of the spectrum, even when flooded, a conventional fast reactor group structure with only one thermal group was employed. The resonance fuel isotopic cross sections were shielded according to the single-level narrow-resonance approximation over the resolved regions. The resonance poison cross sections were mostly based on BNL-325 data with blackness-theory self-shielding applied to the predominant resonances.

In all the experiments described above the entire height of the fuel bundles was flooded; however, flooding of an actual reactor would occur continuously from the bottom to the top of the core. It is therefore important to establish that during this process no large, unpredicted reactivity swings occur. For this reason, the change in reactivity as the water level in four central bundles was increased from zero to full core height was studied. The experimental technique is shown in Fig. 13. Each of the four bundles was poisoned with hafnium so that the flooding reactivity swing was con-

trolled to a reasonable magnitude. The water height was increased in increments of 3 in. at a time from 0 to 48 in.

Figure 14 shows the results obtained along with a corresponding calculation for the case of flooding without poison present. Both the experimental and calculated results show that the axial reflector regions contribute a significant negative reactivity component. In the poisoned case, flooding the fueled region alone increased the reactivity about 85 cents, whereas flooding both axial reflectors and fueled region resulted in an increase of only about 59 cents. These results confirm the earlier data which showed the worth of polyethylene to be negative in the axial reflector regions. Again, both experiment and calculation show the same basic trends.

3. Conclusions

As a result of our experimental and calculational effort it can be concluded that it is feasible to achieve flood safety through incorporation of selected resonance poisons into small U-235-fueled steam-cooled fast reactors. The rather good agreement between calculated and experimental data confirms the validity of the multigroup-diffusion-theory approach. It should be noted, however, that although reactivity differences between unflooded and flooded conditions are fairly accurately predicted, the absolute values are not so well predicted, and there may be systematic errors in all cases which then disappear on taking differences. Furthermore, until data are obtained and evaluated for softer spectra and for plutonium fuel, it is not possible to guarantee the validity of the model for all regimes of flooding which can be encountered in practical systems.

FIG. 10. 16-Central-bundle Flooding Measurement.

FIG. 11. Flooding Compensation as a Function of the Size and Shape of Central Flooded-poisoned Region.

TABLE 2. MSSCE FLOODING EFFECTS
Experimental and Calculated Values—33% Power Split

Case			Reactivity Change (cents)		
Location	v/o H ₂ O	gm/cm ³ Poison	Experiment	Calculation	Difference (Exp - Calc)
One Central Bundle	18.4	None	+129	+110	+19
	18.4	0.36 Hf	+37	+59	-22
	18.4	0.093 Eu ₂ O ₃	+21	+25	-4
	18.2	0.031 B ₄ C	+42	+72	-30
Four Central Bundles	17.0	0.46 Hf	+4.4	+25	-20
	18.0	0.093 Eu ₂ O ₃	+24	-17	+41
	17.3	0.044 B ₄ C	+20	+53	-33
Twelve Central Bundles	17.8	0.093 Eu ₂ O ₃	+4.6	-95	+100
Sixteen Bundle Annulus	18.7	0.070 Eu ₂ O ₃	+32	-0.7	+33

FIG. 12. 16-Flooded-bundle Annular Region.

FIG. 13. Water-handling System.

FIG. 14. Reactivity vs Flooded Height of Four Centrally Located MSCA Bundles.

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Discussion

Mr. Peterson (Battelle-Northwest): I was wondering, in view of your present experimental data, how you would consider the calculations of the previous papers that were presented and the precision of the indicated void and flood calculations.

Mr. Hallam: I think the experiments show that there are a lot of effects involved which may not be caught

in the kind of calculations which were shown previously. I think the method of displaying the results in the previous paper was quite new to me, and I haven't quite digested exactly what it all means. I think the size effect was the one which bothered me most about the last paper. I was never quite sure what size of system was being considered.

Mr. Häfele (Karlsruhe): The question of the accuracy of our calculation is indeed more or less open. We indicated that there are large areas of uncertainties: however, attention should be paid to the effect of

the 1-eV resonance of Pu-240. If you deal with that correctly, then you are already in rather good shape.

Mr. Hallam: Yes, it is definitely of very great importance.

Mr. Wall (GE): I would like to point out to Mr. Peterson that the calculations for the axial and radial polyethylene worth were two-dimensional calculations. To Dr. Ott I point out that we have found in our calculations the two-dimensional effect in the loss of coolant is very important. You can be grossly misled with zero-dimensional calculations.

Session VIII-A
October 14, 1965

Fission Products, Reactions and Interactions

Chairman: J. A. LIEBERMAN
U. S. Atomic Energy Commission

Secretary: L. Baker, Jr.
Argonne National Laboratory

Experimental Studies of the High-temperature Interaction of Fuel and Cladding Materials with Liquid Sodium

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A series of experiments were performed to determine what interactions occur when abnormally hot fast reactor materials contact liquid sodium. The purpose of the investigation was to observe chemical or physical effects which would occur in a fast reactor meltdown accident. The program, thus far, has been a survey study to discover unusual interactions such as exothermic chemical reactions, extensive surface attack, frothing disintegration, or alloy formation. In order to simulate the contact of coolant with reactor materials in a meltdown accident, small samples of reactor material were heated to temperatures as high as 2400°C and dropped into a pool of liquid sodium. The quenched sample of reactor material was removed from the sodium, cleaned with alcohol, and examined for evidence of interaction.

Figure 1 shows the glovebox in which the experiments were performed. The glovebox contained an argon atmosphere in which the concentrations of oxygen and water vapor were maintained at about 15 ppm. Thus far, the reactor materials studied have been metals and UO₂ pellets. The metals were supported and heated in an induction-levitation coil shown in the figure. Samples were heated to as high as 2400°C and then dropped into a vessel containing sodium at 250°C. The samples were generally short cylinders which on melting formed nominal ¼-in. spheres. UO₂ pellets could not be levitated; after heating by radiation from an inductively heated tantalum susceptor they were similarly dropped into the sodium container. In most of the experiments, the molten sodium was contained in a two-liter beaker placed on the floor of the glovebox rather than in a depression as shown in the figure. Sample temperatures were measured by a recording two-color optical pyrometer (Pyro-eye). Heat transfer calculations indicated that pellet temperatures decreased only about 100°C during the nominal 24-in. drop into the sodium.

The first metals studied were not melted during levitation and heating. These were potential cladding metals as shown in Figure 2. The specimens shown

were heated to the indicated temperature, V-20 w/o Ti to 1800°C, Nb to 2250°C, Nb-1 w/o Zr to 2175°C, and V to 1950°C, while remaining in the solid state. As these solid specimens were dropped into the molten sodium, there was a brief period of bubbling, but the samples immediately disappeared from view. Fastax motion pictures showed only moderate surface turbulence as the solid metal samples entered the sodium. The quenched samples showed no evidence of chemical attack by the sodium. Samples were sectioned and mounted for metallographic examination. The photomicrographs gave no evidence of interaction. Similar samples of tantalum and molybdenum at 2200°C also gave no evidence of interaction.

We next studied some of the same metals, but at temperatures where they were molten. Figure 3 shows the residue from V at 2200°C, Ti at 2050°C, and V-20 w/o Ti at 2100°C. These materials fragmented upon contact with the liquid sodium. The contact brought about the expulsion of some of the sodium. The fragmented material, after being freed of sodium by immersion in alcohol, had a shiny metallic appearance, suggesting that no chemical reaction had occurred.

Figure 4 shows the results of drop tests with zirconium at a series of initial zirconium temperatures. Violent fragmentation occurred only at 2350°C, the highest drop temperature studied in this series. At 2100°C, the particle was hollow and some material had been ejected from the center. At 2000°C, the residue was a nearly perfect sphere. At lower temperatures melting had been incomplete. In all cases the residue showed no evidence of chemical interaction.

The most violent interactions that we have observed occurred with stainless steel. The residue from some drops with stainless steel and mild steel are shown in Figure 5. In one of the early experiments in which a 2100°C molten stainless steel sphere was dropped into a small sodium receiver (see Fig. 1), a ragged plate-like residue was formed. At 2300°C, the stainless steel formed particles having an average diameter of about 10 mils. A number of experiments with

FIG. 1. Glovebox for Studies of Interaction of Molten Fuel and Cladding Materials with Liquid Sodium.

stainless steel have been performed at drop temperatures of about 2300°C , and the results were quite reproducible. Many small particles of sodium, 2–3 mm in diameter, were expelled from the molten sodium container and adhered to the walls and ceiling of the glovebox, a maximum distance of 4 ft from the

container. Up to 15% of the stainless steel in a given experiment was also ejected during the violence created by the fragmentation. Experiments with stainless steel were performed not only with the standard 2-ft drop from the levitation coil into the sodium, but also with a 6-in. and a 2-in. drop.

Equally violent and extensive fragmentation occurred regardless of the drop distance. It is therefore concluded that the impact velocity is not a critical parameter for drop fragmentation.

Experiments in which molten mild steel and molten nickel were dropped 2 ft into 250°C sodium showed similar behavior; however, the fragmentation appeared to be slightly less violent. Chemical analyses of residue from drop experiments with zirconium, stainless steel, mild steel, and nickel indicated that no sodium was retained by the metal. This, combined with the shiny metallic appearance of the particles, indicated that there had been no chemical interaction.

The only fuel materials studied to date have been uranium metal and UO_2 pellets. Uranium metal spheres fragmented over a wide range of temperatures, as shown in Fig 6. The fragmentation was somewhat less violent than with stainless steel. Although stainless steel drops broke up into more or less spherical particles, the uranium samples formed a fragile filamentary net work.

UO_2 dropped as solid pellets, as shown in Fig 7, did not fragment, however, extensive cracking occurred in an experiment in which the UO_2 pellet was heated to 2100°C. Sodium was absorbed into the cracks and was not removed by a brief alcohol rinse. Sodium oxide appeared to exude from the cracks for several days after the experiment.

The principal finding of these experiments has been the violent fragmentation which occurs with molten metals heated well above their melting points. This spontaneous breakup was surprising because it appeared to contradict our previous experience upon dropping very hot molten metals into water. Molten pellets of zirconium, uranium, stainless steel, and aluminum simply dropped into water do not fragment. The results of dropping zirconium, uranium, and stainless steel into 250°C sodium have already been described.

The results with aluminum are shown in Fig 8. Aluminum at 800°C formed a sheet-like residue; at higher temperatures, very extensive fragmentation occurred. It was believed, at first, that the oxide film

FIG 2 Residues of Several Solid Metals Dropped into Molten Sodium

FIG 3 Residues of Several Molten Metals Dropped into Molten Sodium

Zirconium (M. P. 1860°C)

FIG. 4. Zirconium Residues from Sodium-interaction Experiments at Various Drop Temperatures.

formed in drops into water may have helped to prevent fragmentation in that medium. To test this hypothesis we dropped spheres of silver both into water and into sodium; the results are indicated in Fig. 9. Although silver and gold do not form oxide films in water, they did not fragment; however, silver dropped into sodium from 1900°C did suffer fragmentation. Thus we concluded that the difference in behavior between water and liquid sodium is not caused by oxide films and is due to differences in physical properties of water and sodium.

In looking for an explanation for the fragmentation process, it occurred to us that the driving force for breakup might be the violent growth and collapse of sodium vapor bubbles which are characteristic of subcooled boiling in the transition and nucleate regimes. In general, it would be expected that the transition and nucleate boiling regimes would extend from nearly the boiling point to around the critical point. For sodium,

this is a range of temperature from 900 to about 2300°C. All fragmentations occurred with molten metals cooling through some part of this temperature range. According to this hypothesis, fragmentation would also be expected to occur with molten metals in water but only in the temperature region of violent boiling. For water at one atm, the boiling point is 100°C and the critical point is 373°C. Figure 10 shows the residue from some experiments in which small samples of some low-melting metals were dropped into water. All samples were initially molten at 825°C. Bismuth, tin, and lead (with melting points below the critical temperature for water) did fragment, while zinc and aluminum (with melting points above the critical temperature for water) did not fragment.

According to our present view, the fragmentation process occurs as a molten material cools through the region of boiling where there is violent growth and collapse of vapor bubbles. Metals useful for con-

304 Stainless Steel

Mild Steel--Cold Rolled

FIG. 5. Stainless Steel and Mild Steel Residues from Sodium-interaction Experiments at Various Drop Temperatures.

struction, such as aluminum, stainless steel, zirconium, and some of the more refractory metals, will fragment on cooling in molten sodium. However, they will not fragment on quenching in water because cooling and solidification occur in a nonviolent film-boiling regime, and these metals are fully solidified before they reach the temperature of violent boiling in water.

A detailed sequence of events during the quenching and fragmentation of stainless steel in sodium is given in Fig. 11. Here are recorded the times corresponding to observations made with high-speed motion pictures of the molten pellet entering sodium. Beginning with the contact of molten stainless steel with the sodium, there is the appearance of a parabolic depression in the sodium which deepens over a period of about 5 msec. Then there is the appearance of a cloud which is presumably sodium vapor. It is at this time that it is presumed fragmentation occurs. The small particles of hot steel then transfer

heat rapidly to a localized region of the sodium, resulting in a burst of vapor which causes ejection of some of the sodium and some of the hot steel pellets. At 5.5 msec, there is the first sign of particle ejection. Violence reaches a peak at about 7 msec and is completely subsided at about 12 msec. Fastax films of several other stainless steel-sodium interactions at similar drop temperatures indicate, however, that the timing of events is somewhat variable.

In conclusion, it should be pointed out that these preliminary studies of the interaction of abnormally hot reactor materials with liquid sodium have identified a fragmentation mechanism which results in extensive subdivision of molten cladding metals. This knowledge should be useful in the interpretation of the results of in-pile meltdown tests of fast reactor fuel elements. It appears that chemical attack and alloy formation do not occur to a significant degree during the brief time available in a rapid quench with the reactor materials studied.

Uranium (M.P. 1132°C)

FIG. 6 Uranium Residues from Sodium-interaction Experiments at Various Drop Temperatures

Discussion

Mr. Stachura (AI): Do you have any motion pictures of the water tests? Was the time sequence similar to the sodium?

Mr. Baker: The water was not nearly as violent. We had a more or less quiet breakup. There was no ejection of hot material.

Mr. Stachura: Our considerations have led us to think that radiation at the high temperatures is quite an additional contribution of thermal energy transfer

from fuel to coolant, one that is unique at the melting points of these refractory materials. Perhaps this may explain why the water tests were not quite as violent.

Mr. Baker: I think that is a very likely point.

Mr. Knight (GE): You had a summary statement with regard to the interaction of sodium with cladding material. Can you make any summary statement about the tests with UO_2 ?

Mr. Baker: The number of our experiments has

FIG 7 Uranium Dioxide Residues from Sodium interaction Experiments at Various Drop Temperatures

Aluminum (M.P. 659°C) in 250°C Sodium

FIG 8 Aluminum Residues from Sodium interaction Experiments at Various Drop Temperatures

been quite limited. However, it has been observed by other people that there is a temperature to which UO_2 may be heated in sodium when wetting begins. If there are cracks, sodium will be absorbed in a slow experiment. We had sufficient temperature to permit wetting and we did get sodium absorbed in the cracks.

Mr Knight: Would you anticipate or have you considered that there might be some difference in the interaction of sodium with UO_2 or mixed oxide as a function of the stoichiometry, either on a transient situation like this or a long-duration test?

Mr Baker: We haven't studied mixed oxides, but

I wouldn't expect any marked difference unless perhaps the mixed oxide were more plastic and could withstand the quench without developing cracks.

Mr Knight: I was a little surprised that the UO_2 pellet hadn't fragmented on this sudden quench. Would you care to comment on that?

Mr Baker: No, I have no comment.

Mr Stachura: There is one comment we might make about UO_2 . When heat transfer begins to occur from a molten pellet to the coolant, so that surface cooling occurs, the conductivity of the material itself may not produce quite the same sustained surface temperatures on the solid oxide that it might on

Gold (M.P. 1063°C)
2600°C Drop into
25°C Water

Silver (M.P. 961°C)
1900°C Drop into
25°C Water

Silver 1900°C Drop
into 250°C Sodium

FIG. 9. Residues of Gold or Silver Dropped into Either Water or Molten Sodium.

Bismuth (M.P. 271°C)

Tin (M.P. 232°C)

Lead (M.P. 327°C)

Zinc (M.P. 420°C)

Aluminum (M.P. 659°C)

FIG. 10. Residues of Low-melting Metals Dropped into 0°C Water (Drop Temperature 825°C in Each Case).

materials of higher conductivity. It would be interesting to get the UO_2 molten and see whether it disperses similarly as do the metals.

Mr. Blumenthal (ANL): Did you have any experiments in which you varied the volume of the pellet?

Mr. Baker: No, the levitation method is fairly

specific with a given coil design and you can't vary the pellet size very much.

Mr. O'Neill (GE): You seem to make a point about the sodium entering the cracks of the fuel of the UO_2 . We have had some experience with sodium interactions with fuel under reactor operating condi-

FIG. 11. Time Sequence of Interaction of Type 304 Stainless Steel (2400°C) and Sodium (250°C)
(from Fastax Film of Event).

tions, and there is, of course, a very high thermal gradient which forces the sodium in the opposite direction.

Mr. Levenson (ANL): Do you think there's any significance to the fact that the oxide pellets appeared to survive this test since all the metals survived when they weren't molten and the only things that fragmented were the things that went in molten? Would you expect the oxides to react any differently if they were molten?

Mr. Baker: If we could get well above the melting point, I would expect fragmentation; on the other hand, if we're over the critical temperature of sodium such that we could freeze the UO_2 before getting into

a violent boiling region (in other words, in a slow boiling region), then it might not fragment.

Mr. Kelman (ANL): I didn't understand the comment that was just made here about the temperature gradient as related to the sodium getting into these cracks. It seems to me that the material cracks under the sodium. It cools off. By the time the sodium enters, there is no longer a temperature gradient to be concerned about. If fact, I would think it might create a kind of a vacuum situation which sucks the sodium back in.

Mr. Baker: I think that Mr. O'Neill referred to a reactor in which there is a gradient during normal operation.

Chemical Considerations in Fast Reactor Safety*

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Introduction

The hazard associated with any reactor depends, to a large extent, on the nature of the radioactive nuclides likely to be released in the event of an accident. A thorough knowledge of the chemical states of the fission products as a function of temperature, concentration, and environment can be used to predict behavior under a wide variety of conditions which would be virtually impossible to investigate in large-scale tests.

This paper is divided into two sections. The first summarizes the results of our studies on the chemical states of iodine released from uranium and UC into helium between 1200 and 2500°C. The second section gives the chemical states of iodine formed upon release from these fuels into air and steam. In addition, calculations, based on thermodynamic considerations, are included which predict the behavior of iodine in sodium.

Chemical State of Iodine Released from Metallic Uranium and Uranium Monocarbide

The studies presented in this section deal primarily with fission product-fuel reactions at elevated temperatures in a helium atmosphere. Samples of metallic uranium and hypostoichiometric UC (4.6 w/o carbon) irradiated over the range from 1.0×10^{15} to 5.4×10^{19} nvt were used. The apparatus and the experimental procedure have been described elsewhere.⁽¹⁻³⁾ Briefly, the method consists of separating the fission products released from a heated fuel sample by condensing them or their compounds in a quartz tube having a linearly descending temperature profile along the wall. The deposition temperature of the fission products was determined by a gamma scan of the tube at the completion of a release experiment.

The experimental deposition profiles shown in Figure 1 indicate that at least three distinct iodine species are released from fuel samples heated to temperatures ranging from 1200 to 2500°C. It is evident that the relative quantity of each released species varies with the fuel temperature. Most of the iodine released from molten uranium at 1240°C deposits at 275°C. As the fuel

temperature is raised to $\approx 2000^\circ\text{C}$, a second iodine species which collects below 100°C forms at the expense of the 275°C deposit. A third iodine species, which deposits at $\approx 200^\circ\text{C}$, becomes progressively predominant at fuel temperatures above 2150°C.

Although Fig. 1 shows only the results from the studies with metallic uranium, the same general trend was observed upon heating UC fuels within the temperature range from 2300 to $\approx 2500^\circ\text{C}$. In the case of UC (m.p., $\approx 2350^\circ\text{C}$) no experiments were made at temperatures below 2300°C. At temperatures far exceeding 2500°C, the released iodine was observed to collect largely below 100°C.

A specific identification of each of the released species by conventional analytical methods is hindered since the quantities are generally below the picogram level. A combination of techniques involving neutron activation, observing the behavior of known species and the effect of varying chemical environment, together with a thermodynamic analysis of the system, have proved useful in their identification.

The nature of the deposition profiles suggested that the iodine was either in chemical combination with uranium or some other fission product. If the iodine had reacted with another fission product, combination with cesium would be the most likely possibility, a conclusion based on free energy of formation values and relative fission product concentrations. Recent studies have shown that cesium and iodine are not released as CsI in these experiments but are transported independently.⁽⁴⁾ The presence of uranium in the 250 to 350°C iodine deposition zone was detected by means of neutron activation analysis, though a definite U/I ratio was not established.

Although largely speculative, thermodynamic calculations based on estimated values given by Brewer⁽⁵⁾ indicate that a uranium subiodide such as UI or UI₂ should be stable under the conditions of these experiments and, indeed, should be the major species at 1200°C. A uranium subiodide having a U/I ratio of 1.1 was successfully synthesized by passing elemental iodine tagged with I-131 over nonirradiated uranium maintained at 1200°C. This subiodide, carried by a

* Work performed under the auspices of the U. S. Atomic Energy Commission.

FIG. 1. Deposition of Fission-product Iodine Released into He.

helium stream into the deposition tube, was found to deposit at 275°C. This result suggested that the 275°C fission product iodine deposit was UI or some multiple thereof. The 200°C deposit has not yet been identified, but it is also probably a uranium subiodide. The deposit at 100°C and below was found to be essentially elemental iodine. This was concluded from the behavior of known samples of elemental iodine passed into the deposition tube and from experiments conducted in air, as discussed in the next session.

The temperature dependence of the species discussed above is in qualitative agreement with the behavior of many high-temperature systems as explained by Brewer's Rule.^(6, 7) * This is seen by referring to Fig. 2, which is a logarithmic plot of the relative partial pressures of the released iodine species versus $1/T$. This was constructed by taking the relative partial pressures to be proportional to the peak areas in Fig. 1. The three straight lines in Fig. 2 show that all of the experimental results up to $\approx 2500^\circ\text{C}$ can be explained solely on the basis of a different heat of vaporization for the individual species. Based on this plot, the species deposited at 275°C (which predominates at lower temperatures) should have the lowest heat of vaporization, whereas the species deposited at 200°C should have the highest heat of vaporization. Therefore, as the temperature is increased, the species which deposited at 100°C should become progressively more important until a point is reached where its partial pressure is equivalent to that of the 200°C species ($\approx 2400^\circ\text{C}$). At temperatures $>2400^\circ\text{C}$ the 200°C deposit should then be the predominant species. This

* This rule, based on the fact that the entropies of evaporation of most gaseous species are approximately equal, states that in general, for the case of a saturated vapor, the relative concentration of a minor species with respect to the major species can often be expected to increase with temperature.

model offers a satisfactory explanation for the general behavior of the iodine species released from metallic uranium and UC heated to $\approx 2500^\circ\text{C}$. The reason why primarily atomic iodine is released from UC when heated to a temperature greatly exceeding its melting point is not certain. However, on the basis of the above dimension, this would be expected where the partial pressures of the iodine species are below their equilibrium saturation pressures.

A summary of these results is given in Table 1. In an inert atmosphere both elemental iodine and iodine in combined forms are released; the relative quantity of each depends on the fuel temperature. At temperatures slightly above the melting point of metallic uranium, most of the iodine is converted to UI, which plates out at 275°C. At temperatures in excess of 2000°C , partial dissociation of UI to elemental iodine occurs. As the temperature is further increased to $\approx 2500^\circ\text{C}$, a second released iodide, which plates out at 200°C, becomes the major iodine species. Primarily atomic iodine is released in the case of UC heated far above its melting point.

FIG. 2. Relative Partial Pressures of Released Iodine Species as a Function of Temperature.

TABLE 1. SUMMARY OF STUDIES WITH METALLIC URANIUM AND UC IN INERT ATMOSPHERE

Fuel Sample	Probable Iodine States	Conditions and Remarks
U	UI	~1200-2000°C Plate-out temperature: 275°C Synthesized deposit composition UI _{0.9}
	UI + I	~2100-2500°C
	(U _x I) + I	>2500°C (U _x I) plates out at 200°C
UC	Species generally similar to metallic U	Primarily atomic iodine released at temperatures in great excess of the melting point of UC

Environmental Behavior

This section deals with the effect of the environment on the chemical states of fission products. The iodides released into an inert environment, as discussed above, were tenaciously held to surfaces at their respective deposition temperatures. Additional experiments were carried out to determine the effect of flowing air over these iodides. In these experiments, the iodide was nearly completely dissociated and the evolved elemental iodine was carried into the cooler regions of the deposition apparatus. At ambient temperature the iodides were also oxidized; however, free iodine was only very slowly evolved. These results are consistent with the known behavior of the uranium iodides, which readily oxidize in air by the following reactions:

Since the uranium iodides are quite unstable in air, only elemental iodine was expected to be released upon heating the irradiated fuels directly in air. This was verified experimentally.

The effect of steam on the chemical states of iodine was also investigated. Experiments were carried out with metallic uranium fuels irradiated over the range from 3.0×10^{14} to 5.4×10^{19} nvt and heated within the temperature range from 1000 to 1300°C. The uranium iodides are quite unstable in the presence of moisture and would readily hydrolyze, liberating elemental iodine. Therefore, these iodides would not be expected to be important in this case. In this system, the important species are those formed by reactions of the evolved iodine with the hydrogen produced by the metal-water reactions.⁽⁸⁾ The relative distribution of the iodine (I⁰), iodide (I⁻), and iodate and periodate

(I⁺⁵, I⁺⁷) states released from metallic uranium are shown in the first column of Table 2. It is evident that the iodine released from metallic uranium is primarily in the reduced state (avg of 83.2%) with only an average of 13.4% present as elemental iodine. In the case of the UO₂ fuels where little hydrogen is present in the environment, primarily elemental iodine is released, as shown in the second column of this table.

The majority of the iodine released from uranium is rapidly converted to HI by the reactions shown in Fig. 3. The first reaction expresses the release of fission products (F.P.) from uranium during its oxidation in steam. The released iodine reacts with hydrogen to form HI by reaction (2). At high temperature, the equilibrium expressed in reaction (3) is far to the left, so that little molecular iodine is present within the cell. A small quantity of iodine dissolves in accordance with reaction (4); however, this is reversible and most is analyzed as elemental iodine. In solution, some HIO disproportionation takes place at elevated temperatures, giving rise to iodates and periodates via reaction (5). About 1 to 2% of the iodine released from uranium reacts to form methyl iodide.

The equilibrium between H₂, I, and HI was calculated for the iodine concentration range from 10⁻¹⁴ to 10⁻⁸ mole/liter, H₂ partial pressures ranging from 10⁻⁵ to 1 atm, and temperatures between 298 and 2000°K (see Fig. 4). This figure is useful for predicting con-

TABLE 2. AVERAGE CHEMICAL STATE OF IODINE RELEASED INTO STEAM

State	Relative Amount, %	
	U	UO ₂
I ⁰	13.4	84.6
I ⁻	83.2	10.5
I ⁺⁵ , I ⁺⁷	1.8	0.5
Others	1.6	4.4

FIG. 3. Iodine Reactions in a Steam Environment.

FIG. 4. Hydrogen Iodide Equilibrium.

version of iodine to hydrogen iodide. The experimental results of the aforementioned study are consistent with these calculations.

Although primarily elemental iodine is released from UO_2 into steam, this has been observed to react with organic impurities, forming substantial quantities of methyl iodide. In these experiments, in which distilled water boiled from an alkaline permanganate bath was used, only small quantities of the iodine were found to be converted to organic compounds as listed in Table 2 under "Other" states. Similar experiments carried out under less stringent conditions produced up to 21 % of these organic compounds. However, in other studies, we have shown that methyl iodide is relatively unstable in a combined oxidizing and radiation environment, and these compounds are expected to be substantially less significant under reaction accident conditions.

Most of the fast reactor concepts under study in this country involve the use of liquid metal coolants. At present sodium is the primary liquid metal under consideration. Some studies of fission product retention in sodium have been carried out elsewhere. However, there has been no satisfactory interpretation of the data, and the quantitative retention characteristics of sodium are still uncertain. We have recently calculated the probable chemical states and volatility of iodine in sodium to predict its behavior at high temperatures. These results are summarized below.

A comparison of the estimated standard free energies of formation of the uranium iodides and sodium iodide shows that UI_4 , UI_3 , and UI_2 would undoubtedly react with sodium to form NaI. However, this cannot be stated with complete certainty in the case of some other

subiodide such as UI. In considering reactions of iodine with other fission products, compound formation with cesium is found to be among the most probable based on relative concentrations and thermodynamic values. However, because of the low chemical potential of fission-product cesium in a large volume of sodium, compound formation with cesium would be minimal where concentrations are of the order of parts per million. Therefore, the iodine is almost certainly combined with sodium in dilute solutions.

Further calculations were done to predict the behavior of sodium iodide in sodium. In these calculations the standard free energy of formation values were taken from the JANAF tables;⁽⁹⁾ the activity coefficient of sodium iodide in sodium was estimated by Wagner's method⁽¹⁰⁾ and the Na-NaI phase diagram published by Bredig and Sutherland.⁽¹¹⁾

The free energy of mixing (F^m) for NaI in sodium with mole fractions of X_2 and X_1 , respectively, was represented by a power series. By use of the phase diagram, values of the four arbitrary constants were evaluated to give the following equation:

$$F^m = 2062 X_1^4 X_2 + 61180 X_1^3 X_2^2 + 1788 X_1^2 X_2^3 + 8525 X_1 X_2^4 + RT(X_1 \ln X_1 + X_2 \ln X_2).$$

The free energy of mixing of NaI is related to the total free energy of mixing, concentration, and activity coefficient in the following manner:

$$F_2^m = F^m + (1 - X_2) \left(\frac{\partial F^m}{\partial X_2} \right) = RT \ln \gamma_2 X_2.$$

The activity coefficient of NaI at infinite dilution was calculated from these two equations. This value, to-

gether with the vapor pressure of pure NaI, was used to develop the following expression for the partial pressure of NaI in terms of its mole fraction, X_2 , and temperature, T , in degrees Kelvin:

$$P_{\text{NaI}} = \left[1.316 \times 10^{-3} \exp \left(-\frac{18822}{T} + 19.28 \right) \right] X_2.$$

This equation can be used to calculate the partial pressure of sodium iodide in the gas phase, which in turn can be related to percentage iodine release knowing the volumes of the gas phase and liquid phase, respectively. Attempts to measure percent release as an intensive property of the system are obviously meaningless. A consideration of the standard free energy of

FIG. 5. Calculated Equilibrium Vaporization of Dilute Sodium Iodide Solutions in Sodium.

formation for NaI shows that dissociation is negligible at least up to 1500°K, and thus the iodine in the gas will be almost entirely NaI.

Using this data, a Rayleigh equation expressing the percent vaporization of NaI as a function of the percent sodium vaporized was developed. The equation and a plot for 700, 800, and 900°C are shown in Fig. 5. It is readily apparent that a significant fraction of the sodium can be vaporized without large losses of NaI.

Finally, it is of interest to consider the consequences of oxidizing sodium containing iodine. Three possible reactions with the environment (oxygen and water vapor) are shown in Fig. 6. The first reaction has a more positive standard free energy of reaction than the latter two and can be neglected. The standard free energy for the second reaction is the smallest, and the reaction appears to be more favorable than the third. It turns out, however, unless a large fraction of the environment is water vapor, reaction 3 actually predominates.

The usual assumption of taking the activity of the two components Na_2O_2 and NaI as unity, suggests that no appreciable decomposition to free iodine takes place. However, it was found experimentally that during the initial stages of NaI oxidation, its activity was far less than unity, and decomposition of the iodide did indeed take place. An addition of excess Na_2O_2 substantially suppressed decomposition. Therefore, in the case of a reactor environment containing large quantities of sodium also being oxidized to Na_2O_2 , the decomposition of NaI should be negligible.

Chemical Reaction	Equilibrium Constant
1) $\text{NaI} + \frac{1}{4} \text{O}_2 \rightleftharpoons \frac{1}{2} \text{Na}_2\text{O} + \text{I}$ $\Delta F_{1000^\circ\text{K}}^\circ = 28.82 \text{ kcal/mole}$	$K_1 = \frac{a_{\text{Na}_2\text{O}}^{1/2} a_{\text{I}}}{a_{\text{NaI}} a_{\text{O}_2}^{1/4}} = 4.9 \times 10^{-7}$
2) $\text{NaI} + \frac{1}{4} \text{O}_2 + \frac{1}{2} \text{H}_2\text{O} \rightleftharpoons \text{NaOH} + \text{I}$ $\Delta F_{1000^\circ\text{K}}^\circ = 17.56 \text{ kcal/mole}$	$K_2 = \frac{a_{\text{NaOH}} a_{\text{I}}}{a_{\text{NaI}} a_{\text{O}_2}^{1/4} a_{\text{H}_2\text{O}}^{1/2}} = 1.4 \times 10^{-4}$
3) $\text{NaI} + \frac{1}{2} \text{O}_2 \rightleftharpoons \frac{1}{2} \text{Na}_2\text{O}_2 + \text{I}$ $\Delta F_{1000^\circ\text{K}}^\circ = 26.93 \text{ kcal/mole}$	$K_3 = \frac{a_{\text{Na}_2\text{O}_2}^{1/2} a_{\text{I}}}{a_{\text{NaI}} a_{\text{O}_2}^{1/2}} = 1.2 \times 10^{-6}$
	$P_{\text{Iatm}} \cong 5.5 \times 10^{-7} \left[\frac{a_{\text{NaI}}}{a_{\text{Na}_2\text{O}_2}^{1/2}} \right]$

FIG. 6. Reactions and Thermodynamic Values for the Oxidation of Sodium Iodide.^(a)

^(a) JANAF Thermochemical Tables, The Dow Chemical Company, Midland, Michigan.

Conclusions

In an inert atmosphere, two iodides and atomic iodine are released from metallic uranium and UC fuels. The relative quantities of each species are primarily a function of fuel temperature. The two iodides are tenaciously held on surfaces at temperatures of ≈ 275 and $\approx 200^\circ\text{C}$ in an inert atmosphere, but readily evolve elemental iodine into an oxidizing environment.

Where iodine is released during a metal-water reaction, conversion to HI can be validly estimated from thermodynamic considerations. In a sodium environment, essentially all of the iodine will be converted to NaI when the fission product concentrations are of the order of parts per million. Expressions are given for calculating the volatility of NaI in sodium. Subsequent exposure to an oxidizing environment should not result in substantial NaI oxidation in the presence of excess Na_2O_2 .

Acknowledgments

The authors wish to thank Mr. Harry R. Munkelwitz and Mr. George Hardman for assistance in performing many of the experiments. The authors are indebted to Dr. J. J. Egan for valuable discussions regarding thermodynamic considerations in these studies.

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Discussion

Mr. Stachura (AI): What were the values of the activity coefficient of NaI in the 500-1000°K range?

Mr. Castleman: They were of the order of something less than 10, perhaps about 3 to 5.

Mr. Stachura: I remember seeing an Argonne report

on the EBR-II refining process in which they discussed some of their differences and the possibility that they observed cesium iodide.

Mr. Castleman: Their work actually led us into this. We had originally also noticed that cesium and iodine come down together. We spread out our thermal gradient in a much larger apparatus and were able to show that they are transported independently. This doesn't mean that you can't get reaction on the surface when they do come together, but we don't believe they are released from the fuel together. Many of the Argonne studies were with synthetic mixtures of a uranium iodide and a cesium iodide; this may not lead to the same release as from a very dilute solution which one has in a fuel.

Mr. Stachura: I thought their contention was just the opposite—that since they had microbubbles and highly irradiated fuel the opportunity for the formation of the iodide might exist. The other point is in the case of your studies of the activities of iodine and sodium. Would not cesium be likely to influence the behavior you observed?

Mr. Castleman: Actually we looked into this on paper. It turns out that even though the free energy of reaction between cesium and iodine is more negative, the chemical potential of the cesium is so low at the concentrations involved that the cesium-iodine reactions would not predominate over those of sodium and iodine.

Mr. Stachura: In other words, you determined the activity coefficients for cesium and sodium?

Mr. Castleman: No, but we estimated that they would have to be of the order of at least over 10^4 . I know of no activity coefficient of anything in anything that is above 100.

Mr. Stachura: In other words this occurs because the cesium concentrations are so dilute.

Mr. Castleman: The cesium concentrations are so dilute, and also there is a negative deviation with respect to an ideal solution for cesium in sodium rather than a positive deviation. One would need a very large positive deviation to expect these reactions to be predominant when we're talking of the order of parts per million now and not discussing higher concentrations. With an excess of cesium, we have nearly unit activity of cesium, and the cesium-iodine free energy of reaction is much more favorable than that between sodium and iodine.

Mr. Runnalls (AECL): Could you account for the shape of the cesium distribution you obtained?

Mr. Castleman: In the case of quartz, the cesium is highly adsorbed. We are seeing the movement of an adsorption wave, and by extending the length of the run, the cesium will move down and peak at about

450°C. In the particular run plotted, the run was stopped before all of the cesium had desorbed, moved down, and given one peak position as the iodine did. However, we've run these experiments for fairly long periods of time and have found that the uranium iodide is very tenaciously held to the surface at 275°C. The cesium will work its way down, but it is not moving with the iodine.

Mr. Runnalls: Well, the puzzle was that the cesium did not seem to be moving appreciably from the hot end. Do you not expect that it would evolve as cesium gas?

Mr. Castleman: Yes, but it is adsorbing on the surface. On that particular plot it was starting out high and it was peaking at around 450°C. It was actually moving as an adsorption wave down the tube.

Fission Product Retention in Sodium and Applications to Vented Fuel Element Design*

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(PRESENTED BY W. KUNKEL)

The use of a vented fuel element reduces cladding strain from released fission gas pressure, thereby increasing the potential of the fuel for extended burnup. Since there is a gradual release of fission gas as a result of venting, the possibility of a sudden, abrupt fission product release from the element upon failure during operation, handling, storage, or shipping is eliminated. In addition, for sodium-bonded fuel elements, the bond is not lost as a result of a cladding failure and pressure differential, thus eliminating the overheating problem associated with bond loss. Finally, the consequence of the fuel-slumping accident is reduced since the fuel density can be increased as a result of the decreased requirement for gas-holdup space.

Although there are some radiological problems in most vented fuel-element designs, the successful operation of the Dounreay Fast Reactor as reported recently has indicated that these problems can be satisfactorily resolved.⁽¹⁾ This paper will indicate some of the safety problems associated with the vented-fuel-element concept and apply the results of recent experimental and analytical studies to those problems.

A. Some Problems Associated with Vented Fuel Element Design

Figure 1 illustrates schematically some of the various paths by which fission products may be transported from a vented fuel element. Also shown are some of the inherent and engineered control measures which might be utilized in a reactor employing vented fuel elements.

The fission products released from a vented fuel element to the sodium coolant may increase the operational radioactivity levels of the sodium or reduce the accessibility of the system for component replacement or maintenance operations after shutdown. The high specific activity of the sodium during operation would mask most of fission product radiation until the system is shut down. However, an increase in the fission product contamination

level of the sodium in the reactor must be considered in the accident evaluation of a sodium spill or a sodium fire. The radiological effect of the fission product isotopes would probably greatly increase the environmental hazard as compared to normal sodium activation.

In a similar manner, the operational contamination of the reactor cover gas resulting from the release of fission products from a vented fuel element can be transported to the high bay as a result of small but finite leakage. This constitutes an environmental hazard in the region of the operating floor. In addition, the increase in the fission product gas in the holdup system necessitates careful design of holdup and filtration devices. Of special significance in this case is the possible effect of the decay of short-lived xenon and krypton isotopes into daughters of relatively lower volatility, cesium and strontium, with consequent increase in the radiological hazard. This effect was noted in analysis of the radioactivity levels in the Dounreay sphere.⁽¹⁾ Analysis of the airborne radioactivity showed that rubidium-88 and cesium-138 were the principal constituents.

B. Application of Available Data to these Problems

Many of the results of recent AI studies⁽²⁾ of fission product retention in sodium are applicable to the behavior of fission products released into sodium from a vented fuel element. For example, the extent of isotopic retention in sodium is shown in Table 1. These data, which were acquired from several experiments, indicate the considerable extent to which iodine, and the relatively lower volatility isotopes such as barium, zirconium, etc., are retained in a sodium environment following release from a fuel rod. These data were obtained from a variety of experiments ranging from the in-pile melting and vaporization of uranium discs in sodium, to out-of-pile experiments in which iodine and sodium iodide were injected into sodium columns and the off-gas sampled for the presence of iodine.

In a typical fast reactor system, there are many

* Work performed under Contract AT(11-1) GEN-8 for the U. S. Atomic Energy Commission.

FIG. 1. Fission Product Control from Vented Fuel Elements.

additional factors which may influence the transport behavior of fission products released from a vented fuel element. Some of these may be deliberately designed into the system, such as the coolant- and gas-cleanup systems shown schematically in Fig. 1. Other factors are associated with sodium reactor components and piping. For example, past experience with fission products released into the Sodium Reactor Experiment (SRE) as a result of a partial fuel melting indicated that there is a considerable tendency for radioactivity to deposit at various points throughout the sodium system, thus depleting the amount available in the sodium in the event of fires or spills. Table 2 shows the radioactivity levels measured by Hart from sodium samples in the SRE primary system.⁽⁵⁾

TABLE 1. COMPARISON OF FISSION PRODUCT RETENTION IN SODIUM EXPERIMENTS

Source of Data	Bulk Sodium Temperature (°F)	Fraction of Activity Released into Sodium			Fraction of Activity in Sodium Released to Cover Gas			Remarks
		ZrNb ⁹⁵	I ¹³¹	BaLa ¹⁴⁰	ZrNb ⁹⁵	I ¹³¹	BaLa ¹⁴⁰	
IP-1	500	(a)	(a)	0.246	—	—	(a)	Fuel not preirradiated.
IP-2	500	0.02	0.01 to 0.10	0.136	(a)	2×10^{-5}	1×10^{-6}	Preirradiated fuel in this experiment.
IP-3	1000	0.10 to 0.24	0.21 to 0.72	0.024 to 0.054	(a)	2×10^{-4} to 4×10^{-5}	1.1×10^{-3} to 5×10^{-3}	Experiments differ only in preirradiation and sodium loading techniques.
IP-4	1000	0.103	0.44	0.021	(a)	4×10^{-5}	2×10^{-4}	
OP-1	900 to 1400	(b)	0.053	(b)	(a)	$8.6 \times 10^{-4(c)}$	(a)	Diffusion and recoil only. No fuel melting.
Hart ⁽³⁾	900 to 1500	(b)	(b)	(b)	(b)	2.5×10^{-6}	(b)	Chemical injection of NaI. No uranium fuel involved.
Begley ⁽⁴⁾	500	(b)	(b)	(b)	(b)	2×10^{-4} to 1.5×10^{-2}	(b)	Simulate release of equilibrium condition of I ₂ and inert cover gas due to cladding failure. In the 1000°F case, an oxide layer may have affected the release fraction.
	1000	(b)	(b)	(b)	(b)	$<2 \times 10^{-4}$ to $<1.4 \times 10^{-2}$	(b)	

(a) Not observed.

(b) Not measured.

(c) This amount was recovered on the heated portion of the tubing. The amount actually mobile was 2×10^{-6} of the activity in the sodium.

Based on the normal radioactive decay for each isotope, the predicted value after approximately three months is shown in Table 3, along with actual sodium concentration measured at the later date.⁽⁵⁾

It was also observed shortly after the incident that the fission product activity appeared to be associated very markedly with the several pounds of carbonaceous material which had originally caused the melting of the fuel element. Table 4 shows the difference in specific activity between sodium samples and flecks of the carbon material obtained from the system.

A larger sample of the carbonaceous material was obtained from the tops of the moderator cans and was compared to the sodium specific activity shown

TABLE 2. INITIAL FISSION PRODUCT ANALYSIS OF SRE PRIMARY SODIUM

Isotope	Primary Coolant Activity ($\mu\text{Ci/g Na}$) ^(a)	Total Coolant Inventory (Ci) ^(a)	Total Reactor (Ci) ^(a)	Fraction of Inventory Released ^(b)
Cs-137	1.26	2.77×10^1	8.70×10^3	3.18×10^{-3}
Cs-134	0.02	4×10^{-1}	2×10^2	$2 \times 10^{-3(c)}$
Sr-89	20.0	4.44×10^2	1.60×10^5	2.78×10^{-3}
Sr-90	0.97	2.14×10^1	8.15×10^3	2.63×10^{-3}
I-131	0.74	1.63×10^1	1.68×10^4	0.97×10^{-3}
Ce-141	4.38	9.65×10^1	1.27×10^5	0.76×10^{-3}
Ce-144	5.18	1.41×10^2	1.69×10^5	0.67×10^{-3}
Ba(La)-140	1.65	3.63×10^1	5.61×10^4	0.65×10^{-3}
ZrNb-95	13.9	3.06×10^2	5.53×10^5	0.55×10^{-3}
Ru-103	0.95	2.09×10^1	7.52×10^4	0.28×10^{-3}

^(a) As of July 26, 1959.

^(b) Multiply values in this column by 3 to adjust fraction released to average values for those fuel elements which suffered cladding failures (14 of 43 elements failed).

^(c) From neutron capture in Cs-133; estimated.

TABLE 3. FISSION PRODUCT CONTAMINATION DECREASE IN PRIMARY SODIUM
(August 2 to October 31, 1959)

Isotope	Fission Product Contamination ($\mu\text{Ci/g Na}$)			Ratio: Oct 31 (actual)/ Oct 31 (predicted)
	Aug 2	Oct 31 (predicted) ^(a)	Oct 31 (actual)	
Cs-137	1.26	1.26	0.45	0.36
Cs-134	0.02	0.02	0.006	0.3
Sr-90	0.97	0.97	0.060	0.062
Sr-89	19.0	5.8	0.25	0.043
I-131	0.42	0.00019	0.00012	0.63
Ce-141	3.7	0.54	0.000088	0.00016
Ce-144	5.1	3.9	0.00031	0.00008
ZrNb-95	13.0	5.2	0.0067	0.0013
Ru-103	0.85	0.19	0.0045	0.024

^(a) On the basis of radioactive decay of August 2 sample.

TABLE 4. RADIOACTIVITY ANALYSIS OF TYPICAL SAMPLE OF CARBONACEOUS MATERIAL FROM PRIMARY SODIUM SAMPLE

Isotope	Activity of Carbonaceous Material ^(a) ($\mu\text{Ci/g}$ of particles) ^(b)	Activity of Sodium ($\mu\text{Ci/g Na}$) ^(b)
Cs-137	1.1×10^3	0.45
Cs-134	2.9×10^1	0.006
Ce-141	2.3×10^2	0.0007
Ce-144	2.9×10^2	0.00039
ZrNb-95	2.2×10^2	0.018

^(a) Obtained from reactor on 10/31/59.

^(b) As of 7/26/59.

TABLE 5. RADIOACTIVE ANALYSIS OF CARBONACEOUS MATERIAL TAKEN FROM TOP OF MODERATOR CANS

Isotope	Activity of Carbonaceous Material ($\mu\text{Ci/g}$) ^(a)	Activity of Sodium ($\mu\text{Ci/g}$)
ZrNb-95	6.8×10^3	0.018
Cs-137	5.0×10^3	0.45
Cs-134	1.3×10^2	0.006
Ce-144	3.6×10^3	0.00039

^(a) As of 7/29/59.

TABLE 6. IMPURITY LEVELS IN COLD TRAP AS COMPARED TO SODIUM COOLANT IN THE SRE

Impurity	Level in Cold Trap	Level in Sodium Coolant	Collection Ratio $\frac{A_{\text{trap}}}{A_{\text{coolant}}}$
Carbon	144-1550 ppm	18-60 ppm	77
Cs-137 ^(a)	$4.0 \times 10^2 \mu\text{Ci/g}$	$1.5 \times 10^{-2} \mu\text{Ci/g}$	2.7×10^4
Sb-125 ^(a)	4.3 $\mu\text{Ci/g}$	$0.63 \times 10^{-2} \mu\text{Ci/g}$	6.8×10^2
Fe ^(b)	200->500 ppm	50 ppm	>10
Si ^(b)	200->500 ppm	50 ppm	>10
Mn ^(b)	50-500 ppm	<5 ppm	>100
Pb ^(b)	5->500 ppm	10 ppm	>50
Cr ^(b)	5->500 ppm	5 ppm	>100
Ni ^(b)	10-300 ppm	5 ppm	60

^(a) Measured by gamma spectroscopy.

^(b) Measured by emission spectroscopy.

in Table 5. As far as the activity of the cover gas is concerned, only xenon and krypton isotopes were observed in the reactor cover gas following the fuel-element release.

Work by Hansen⁽⁶⁾ has shown that the system cold trap, which is designed to keep the oxide level to a lower limit of 10 ppm, is also a very effective device for coolant purification. These results are shown in Table 6.

The high collection ratio of the Cs-137 is particularly interesting. Hansen speculates that cold trap-

ping of the cesium by saturation is not likely since the cesium concentration by weight is 10^{-4} ppm and it is soluble in sodium to 50 w/o at 250°F. He further suggests that the high oxidation potential of cesium may result in the reduction of sodium oxide as the retention mechanism. The much higher collection ratio for cesium-137 indicates cesium-trapping mechanisms other than a trapping of carbon particles carrying cesium.

Earlier results also have been corroborated with more recent data which were obtained as the primary system piping of the SRE was dismantled for reactor modification. Figure 2 shows a photograph of a black deposit which was noted on the SRE spool piece (the piping section of the primary system adjacent to the reactor). For comparison the figure includes an autoradiograph of the same sector showing the marked correlation of the black carbonaceous

FIG. 2. Photo and Autoradiograph of SRE Spool L-103 after 26-hr HCl Etch.

FIG. 3. Relative Efficiency for Isotope Removal by Nitric Acid Rinsing.

TABLE 7. RELATIVE ISOTOPIC ACTIVITY INVENTORY OF A HIGH-BURNUP UC CAPSULE

Isotope	Cover Gas		Fuel Rod
	Measured μCi	Relative ^(a)	
Kr-85	1.0×10^{-5}	1.0	1.0
Xe-131m	1.2×10^{-5}	1.2	1.7
I-131	3.6×10^{-7}	0.036	15.3
Xe-133	1.1×10^{-4}	11.0	354.0

^(a) Normalized to Kr-85.

stain with deposition of radioactivity. Sectors of the same pipe spool piece were repetitively etched with acid to remove the deposited radioactivity. These data are typified by the graph shown in Fig. 3. It is of interest to note that this activity was not merely lying on the pipe surface, but, as indicated by the tenacity of the deposition removal, was rather firmly affixed to the pipe surface.

An important factor in assessing safety considerations when vented fuel elements are used is the extent of decay during transport time of the rare gases released into the cover gas. Very little experimental data are available regarding the transport delay times of xenon and krypton isotopes from formation in the fuel to the release into the sodium bond, and subsequent appearance in the cover gas. Some information applicable to this problem was recently obtained from post-irradiation examination of cover-gas and sodium-bond activity in a high-burnup UC test fuel rod irradiated to 20,000 Mwd/MTU. The fuel rod was sodium bonded to 10-mil stainless-steel cladding, and the sodium bond extended approximately $\frac{1}{2}$ in. above the active fuel region. The gas space above the sodium was filled initially with helium. The fuel was hypostoichiometric UC enriched to approximately 11% in U-235. The fuel rod was 18 in. long and 0.5 in. in diameter, and contained 14.6 g of sodium in the bond and 749.6 g of UC.

The radioactive inventory of a cover-gas sample obtained from this fuel rod is shown in Table 7. The actual amounts measured have no significance since some of the gas activity was apparently lost due to a leak in a thermocouple weld; thus, it was necessary to use a normalization procedure to evaluate the results.

The data from the cover-gas sample were extrapolated back to reactor out-time and were normalized to the longest-lived isotope present (Kr-85). These data were compared with the estimated inventory of fission products anticipated from fuel under these burnup conditions. The calculated activity ratios in the fuel-rod inventory would indicate

that there should be approximately 354 times as much Xe-133 in the cover gas as there would be Kr-85, whereas the measurements showed only 11 times. There was, therefore, an indicated depletion of Xe-133 by a factor of 32 relative to the krypton. If it is assumed that one may associate this depletion with half-life, it represents a decay while in transport equivalent to $2^5 = 32$, or five half-lives. Similarly, comparing the Xe-131m release, a 0.49 half-life is indicated. The half-life of Xe-133 is approximately 5.3 days; that of Xe-131m is 12 days. Therefore, an apparent delay of xenon release compared to krypton release could account for these anomalies if it is assumed that this delay is within the range of $5 \times 5.3 = 27$ days to $0.49 \times 12 = 6$ days.

The data presented in Table 7 also provided some insight into the transport behavior of I-131 under steady-state conditions of release. Apparently, there should have been 15.3 times as much I-131 in the gas space as krypton-85 if they were released in the same fraction. The data indicate that there was only 0.036 times as much I-131 as Kr-85. Therefore, the iodine must have been depleted by passage through the sodium bond, plating, or some other phenomena. These data, while admittedly sparse, indicate an apparent figure for iodine release from the sodium of 0.2%. These results compare with the other experimental release figures under controlled laboratory conditions of 0.02%.

C. Parameter Survey for Hypothetical Sodium Reactor System

An analytical study was recently conducted to determine the activity in the high-bay area and in the reactor cover gas during normal operation of a

FIG. 5. Concentration of Kr⁸⁶ in High-bay Area and Cover Gas vs Leak Rate. (Parameters Represent Flow Rates into Purification System.)

power reactor containing vented fuel elements. As pointed out above, it is probable that during the operation of such a reactor, only the noble gases (Xe and Kr) and their daughter products will reach the reactor cover gas and the high-bay area. In this study, a conservative delay time of 5 days was assumed. With these considerations in mind, only two isotopes become significant contributors to the activity: Kr-85 and Xe-133. A further, rather arbitrary, condition was imposed, namely, that 5% of all fission gases formed leave the fuel and migrate through the sodium pool into the cover gas region.

Figures 4 and 5 show the estimated activities in the high-bay and cover-gas areas based on various leak rates from the cover gas into the high-bay area, and for various purification rates of the cover gas. The following conditions were kept constant during the parameter study:

- 1) a high-bay-area ventilation of 2 changes/hr;
- 2) a cover gas volume of 2500 ft³;
- 3) a high-bay volume of 750,000 ft³;
- 4) thermal reactor power of 2500 MW.*

It becomes obvious that a system to purify the cover gas will be necessary. The size of the purification system is, of course, dependent on the leak rate into the high-bay area. For a 0.1%/day flow rate into the high-bay area from the cover gas, a purification rate of 100 cfm will be required to maintain MPC ($1 \times 10^{-5} \mu\text{Ci/cc}$) in the high-bay area. Of the two isotopes considered, Xe-133 and Kr-85, the former is by far the most significant.

The ultimate disposal of these radioactive gases (now contained within the purification system) must also be considered. A reasonable period of time, say

FIG. 4. Concentration of Xe¹³³ in High-bay Area and in Cover Gas vs Leak Rate. (Parameters Represent Flow Rate into Purification System.)

* Although the study was originally done for a thermal reactor, the results are applicable to the fast reactor system.

one to two months, can be allowed for the decay of Xe-133, since its half-life is only 5.2 days. Krypton-85, with a half-life of 10 yr, will not significantly decay within a reasonable period of time. Figure 6 indicates the dose rate at the surface of a shielded cylinder containing a one-year supply of Kr-85. From these data, it is apparent that just under 6 in. of steel will be required to maintain a surface dose rate of 200 mR/hr. The dose rate at 3 m from the surface will be less than 10 mR/hr.

D. Summary and Recommendations

An attempt has been made in this paper to correlate the results of limited information, obtained from several sources, which are applicable to some safety problems anticipated in designs of vented fuel elements. It is obvious that considerable information of a specific nature is required to substantiate the assumptions made. For example, the noble gases and their daughters probably constitute the primary radioactivity concern as far as routine operation and maintenance are concerned. There also appears to be considerable evidence to suggest that a suitably designed sodium-cleanup system employing a cold trap or some form of carbon, such as charcoal, may be an effective means of removing fission products from the primary coolant for vented-fuel-element concepts. The observed deposition and plating of long-lived isotopes on piping surfaces may otherwise constitute a maintenance and replacement problem.

It appears that the radiation problems associated with the vented-fuel-element concept can be solved by the use of inherent and engineering safeguard devices. A comprehensive and detailed experimental and analytical evaluation will be necessary to determine the adequacy of these safeguard mechanisms for assuring safe operation.

FIG. 6. Dose Rate at Surface of Cylinder Containing a One-year Accumulation of Kr⁸⁵.

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Discussion

Mr. Leitz (GE): I think the results reported are very interesting and a very useful contribution to the entire field of fast reactor technology, and to the vented-fuel concept in particular. It's comforting to know that there is a second line of defense provided by the sodium coolant relative to the escape of fission products to the cover-gas system and perhaps beyond. However, I think this in no way reduces the importance and desirability of having the first line of defense function properly, that is, to have the fission products insofar as possible retained within the fuel itself. There seems to have been a misconception developed in the course of this conference that those who are considering vented-to-coolant fuel are somehow in favor of contaminating the sodium-coolant system. This is far from being the case. Quite to the contrary, the objective of venting a ceramic fuel to coolant is specifically to release selectively the long-lived fission gases so that the pressure stress on the fuel pin can be reduced; hopefully this will lead to a less frequent failure of fuel pins, which could in turn lead to the contamination of the coolant through a leaching phenomena. We think it's altogether credible that the venting to coolant may, in fact, reduce the burden of fission product contamination in the coolant rather than increasing it. It was this mechanism of leaching of fission products out of fuel that had been irradiated that led finally to the shutting down of the Russian BR-5 reactor, if I understand things correctly, the leaching of cesium-137 to the coolant.

Mr. Kunkel: I would like to add that we'd be interested in some of the data from Dounreay on the actual activity levels that they have observed in their sodium coolant during their vented fuel operation.

Mr. Porges (ANL): Do I understand that you might exploit this carbonaceous deposit further and possibly use carbonaceous material of some sort as a means of removing fission products from the core in a purifying loop?

Mr. Kunkel: I would not like to say categorically

that the carbon traps the cesium. It hasn't been sufficiently demonstrated. I merely propose it as a rather interesting observation that perhaps coolant-cleanup devices of this sort might be a credible contribution to the remainder of the reactor system.

Mr. Porges: What specific thing are you doing now in this purifying loop?

Mr. Kunkel: We're using cold trap devices primarily. I don't know that there is any intent to use a carbon trap at the present time.

Fuel-failure Detection in Sodium-cooled Reactors

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A discussion of the present status and future possibilities of cladding-failure detection in connection with fast, sodium-cooled reactors may usefully start with a circumspective review of the rationale of fuel-failure monitoring of reactors of different types. For metal-fueled gas- and water-cooled reactors, the basic purposes of providing impervious cladding are the prevention of chemical attack of the coolant on the fuel and prevention of contamination of the coolant by fission products. The design of this cladding inevitably represents some compromise between extreme reliability on one hand, and efficiency as well as cost on the other; thus, occasional cladding failures must be expected to occur with any practical core, and provision should therefore be made to detect these at an early stage of development. Moreover, the high cost of shut-down time of a power reactor justifies the installation of very elaborate systems of failure location. These circumstances led to the development of ever larger cladding-failure monitoring complexes,⁽¹⁻³⁾ with hundreds of sampling tubes installed at the exit of the core, working into programmed multiple-port valves which direct coolant sample streams into batteries of detectors sensitive to the presence of fission products. In some of these installations,⁽³⁾ the detector outputs, as well as the reactor instrumentation, are processed by a special digital computer which calculates the probable size of the cladding failure and decides whether to continue operating while keeping a watchful eye on the leaking fuel element, or to scram in order to replace it. In all modern cladding-failure monitors, the occurrence of a leak in a fuel element is inferred from the presence of fission products in the coolant, since even bulky coolant-sampling manifolds are in practice more compatible with various design requirements than the alternative, direct attachment of sensors to each element or subassembly, provided in some early reactors.

The advent of sodium cooling puts an entirely different emphasis on the rationale of cladding-failure monitoring. Sodium does not attack the fuel, nor is the increase of the coolant activity which would result from fission product contamination (over and above the Na^{24} activity, possibly up to 10 mCi/cm^3 , already present) cause for concern. Thus, fast reac-

tors may well have vented fuel elements, as discussed earlier in this conference, while, in the same vein, satisfactory operation of a fast core with a large number of failed elements has been reported.⁽⁴⁾

In contrast, it may be pointed out that a number of meltdowns, whose exact cause could not in all cases be ascertained, have occurred in sodium-cooled reactor experiments. It is perhaps not unreasonable to suppose that a severe failure of a fuel element, resulting in emission of debris which might lodge in adjacent flow channels, thus blocking the coolant and leading to a rapid temperature rise, could provide the starting mechanism for a partial meltdown. This would indicate a need for monitoring cladding failure at least for the immediate future of fast reactor technology; moreover, the failure monitor must be very fast-responding and, if possible, provide at least qualitative information regarding the type of failure—assuming that the detector signal can be correlated with the degree of danger to the structure for a given core and fuel. More generally, location of a potentially dangerous failed element, which must be removed, at least within a group of subassemblies will be needed if this type of failure cannot be reduced to insignificance through advances in the art of fuel design.

Turning now to consider the technical means available for implementing these design goals, it is evident that much depends here on the specific design of the reactor, and particularly the freedom of designing the coolant-sampling system afforded by it. As concerns the choice of detection equipment, the most rapid detection system appears to be delayed-neutron monitoring; other fast-responding detection schemes are eliminated by the strong gamma activity of the coolant. Since the delayed-neutron signal varies strongly with both power level and coolant throughput, a confirming detector, based on a different scheme, is desirable. For this purpose, precipitation detectors, in connection with gas-processing equipment which will be discussed below, are most suitable. Pilot systems of these two types are, in fact, installed in all fast reactors currently in operation or under construction. By way of illustration, the system now in operation in EBR-II will be briefly described here, together

with some possible future additions; this system provides gross monitoring only, as well as a limited amount of qualitative information on the type of failure. As concerns the next generation of fast reactors, more ambitious cladding-detection schemes, which would have at least partial location capability, will be considered, in broad outline, further on.

The EBR-II cladding-failure monitor consists of a fast delayed-neutron detector, FERDL (Fuel Element Rupture Detection Loop), as well as a wire precipitator presently monitoring the gas blanket. The FERDL is installed on a loop which samples the coolant at the heat exchanger, with a maximum loop flow of 100 gpm out of a total of about 8000 gpm. At a site well shielded from core neutrons, a graphite stack surrounds a section of this loop, in order to moderate the fast neutrons emitted by the delayed-neutron emitters down to the region where cross sections useful for detection are large. The size of the graphite stack, the location and efficiency of embedded detectors, determine the number of counts per input neutron per detector. Cost of graphite and shielding must be considered versus cost of additional detection channels as well as, of course, available space for either, for a given efficiency. The unit shown in Fig. 1, a 20-cu ft stack with eight detectors, thus represents only one of many possible compromises between the above-mentioned factors. The loop pipe is surrounded by a vacuum jacket inside the stack, shown here before electric heaters were attached and heat insulation packed onto the whole length of exposed pipe. A lead radiation shield of 8-in. minimum thickness was then built around the whole system. As may be seen in the figure, the stack as well as the electromagnetic pump is designed to move several inches with the thermal expansion of the pipe. The neutron detectors and four preamplifiers are accessible through a small port in the shielding, covered in operation with a removable lid, through which cables as well as forced-air ducts emerge.

The signals from the neutron detectors are processed through electronic circuitry of 20 Mc bandwidth, designed to minimize pileup of the numerous small background pulses, and providing from two to four output channels. Some of this output information is temporarily stored in a buffer magnetic-tape memory and displayed by a fast oscillograph in the control room on command of a count-rate discriminator and alarm. The system discriminates very adequately against gamma background; detection efficiency per fast neutron emitted within the 2-liter loop section effectively surveyed approaches 1%. The more important criterion of the minimum extent of clearly distinguishable cladding failures depends, however, on two largely unknown additional factors: sampling efficiency and neutron background.

Considering, first, the sampling efficiency, there are indications that dilution in the coolant, as well as transit time to the detector of a "slug" of fission products, depends very considerably on its point of origin in the core; these uncertainties result in even larger variations in signal strength and duration through the short half-life of most delayed-neutron precursors. Even a precise knowledge of these factors would, moreover, apply only to a reactor with clean sodium, whereas various causes, *inter alia* cladding leaks, contribute in practice to a steady buildup of tramp uranium through which the signal-to-background ratio of the detector deteriorates. As a concomitant of this loss of discrimination, one must expect an increase of false alarms, or excursions in the count rate which are not clearly identifiable. In this situation, a confirming detector of integrating type, and thus an inherently better signal-to-background ratio as well as slower response time, effectively complements the delayed-neutron unit. Such a detector is now installed in EBR-II, monitoring the gas blanket for volatile fission products; the integrating feature consists of a trap viewed by a gamma detector in which the fission products, previously precipitated onto a traveling wire, are retained.⁽⁵⁾

At present, the signal from this detector is delayed by a time of the order of 20 min, largely due to the unusually large amount of coolant and extensive gas-blanket characteristic of EBR-II.

Considering now possible future improvements in this instrumentation, it is evident that a charge-collection fission product detector could yield a much faster return—of the order of a few minutes—if it were located on the sampling loop. The inefficiency of sampling would be more than compensated for by the high concentration of fission products in the coolant, provided that a highly efficient method of gas stripping can be developed. Gas-stripping devices of very good efficiency have been designed recently for this very purpose in connection with water-cooled reactors, and could probably be adapted to function with sodium.⁽⁶⁾ For continuous operation over long periods, it will further be necessary to strip the sparging-gas stream of remaining small droplets of sodium, perhaps through a system of mesh and baffle traps which must be designed so as to shed excess sodium, through trapping in low-vapor-pressure liquids, such as NaK or mercury, or else by sonic precipitation. Alternatively, it may be preferable to design a fast-acting valve which would take a sample of the very portion of the coolant which resulted in an excursion of the delayed-neutron monitor count rate. This sample may then be sparged very thoroughly and the sparging stream analyzed by a number of methods.

A second possible future addition to the cladding-

FIG. 1. FERDL Detector Station, EBR-II.

failure detection facilities of EBR-II would be a monitor installed on the charging machine for the purpose of identifying subassemblies containing failed elements during reactor-unloading operations. In its simplest form, such a monitor would pump gas in contact with the subassembly inside the coffin through a cold trap at -110°C so as to retain fission product xenon. The trap might then be viewed by a gamma detector such as a NaI scintillation spectrometer, adjusted to some strong line in the Xe fission product component, say the 250-keV line of 9-hr Xe-135 (a more sophisticated detector installation would employ a Li drifted PIN junction detector inside the trap, with correspondingly better resolution, hence discrimination against background). A further refinement would be a duplicate unit downstream from the first trap, reading background only, for purposes of subtraction.

Considering now what can be done to improve

cladding-failure detection in the future, certain possible improvements in detection efficiency may be briefly discussed, before turning to the more important question of improvements in sampling and other means of partial location of cladding failures. The signal strength of a delayed-neutron monitor can evidently be boosted by the simple means of sampling closer to the core, through a pipe of relatively small diameter, so as to shorten the transit time; by the same token, the section of the sampling loop passing through the detector may be enlarged, up to a limit determined by considerations of moderating efficiency. Discrimination against random background excursions may be enhanced through installation of a second monitor downstream and signal processing based on delayed coincidence. Finally, the statistics may be considerably improved through the insertion of fission plates or otherwise loading the graphite stacks, up to a multiplication of the order 10-20.⁽⁷⁾

Although all these stratagems would require considerable development work, even more elaborate tests would be required to develop a sampling system capable of locating the core region in which the cladding failure has occurred. The design of the reactor may allow a certain degree of this location by relatively simple means, from the time of arrival of fission products at a number of detectors sampling the whole core at different points.⁽⁸⁾ Improvement of the precision of this method through increasing the number of detectors is, however, limited by the large size of detector stations and requirement of proximity to the core.

A more practical system might be based on sampling manifold and valving station, which would go into action only after a cladding failure signal is established by means as described above, while the reactor runs at half power. The cost of designing such a sampling manifold, which may have to be removable for loading, is possibly beyond a justifiable limit. However, a single, telescopically retractable, partially articulated sampling tube might be easier to work out. Such a sampling tube could eventually be programmed to sweep across the core to seek a maximum signal and thus to locate the cladding failure.

In principle, the complete location of a faulty subassembly is therefore not necessarily impossible technically. As mentioned earlier, however, it is conceivable that reactors will be allowed to operate with a number of small cladding leaks; on the other hand, a partially debris-blocked channel may, after the initial burst signal, deliver an insufficient amount of coolant for clear identification by local sampling as suggested above. Thus, the practical problem is not necessarily solved by a single sampling system.

This circumstance lends some interest to another scheme of burst-slug location, which has the advantage of detecting largely fresh cladding leaks. In this scheme, fuel elements of EBR-II type would be coded with different gases or solids. Suitable gases (which must be chemically inert and, of course, absent elsewhere in the reactor) might be nitrogen, neon, krypton, and xenon; solids, which may be alloyed into the fuel or onto its surface, might include gold, arsenic, or antimony.⁽⁹⁾ Detection would be effected by taking a sample of the coolant on command of the delayed-neutron detector, and separating the code element from the sodium. In the case of solids, this would require a certain amount of chemistry, starting with aqueous solution, addition of carrier, etc.; the resultant sample would be gamma counted. For gases, sparging and various methods of detection might be tried; it may be pointed out that in comparison with the coding with solids, coding with gases is a less

reliable means of failure indication since relatively little gas may be emitted in a cladding failure near the bottom of a fuel element. Although the number of available codes is necessarily limited, it is clear that the combination of coding and delayed-neutron channel-sampling techniques provides a far more powerful means of establishing and locating a dangerous type of slug burst than either method alone.

At the present time, all these suggestions are necessarily highly conjectural. Whether the cost of implementing some of the measures considered here, or others of a similar nature, is justified by their possible usefulness depends to a large extent on the experience which will become available as a result of operating facilities such as EBR-II, Dounreay, Rhapsodie, BR-5, and others.

Meanwhile, however, some preliminary tests and calculations on a more modest scale might be undertaken, and other concepts developed, which these remarks are hoped to stimulate.

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Discussion

Mr. Hannum (LASL): It may be of some interest to say what we did in LAMPRE, the Los Alamos Molten Plutonium Reactor, and what we are planning to do in our next one. First, there is somewhat a different philosophy in that we do not seek to find any small leaks at all, but we are concerned about large leaks for the very reason they may propagate. They certainly will dump molten plutonium into the sodium stream. There were two major failures in LAMPRE-I. These were first detected by gross increases in the gamma activity of the cover gas, and this was correlated with a loss in reactivity due to the loss in the fuel from the reactor. These were located simply by checking to see which fuel element

didn't have enough reactivity. In the core test facility we added another complication in that we set as a criterion that the detection system must work with vented fuel. In this case, in contrast to Mr. Leitz, we release all our fission products when we vent. So we cannot use fission products at all. Again, we are going to rely on the gross loss of fuel. We use a module can (seven pins to a can) with thermocouples in the outlet of each. If we assume the fuel leaves the high-flux region, that will cause a loss in power in that module, and this will locate our failure.

Mr. Porges: That would be essentially a system in which each module or subassembly has permanent sensors attached, which brings one back to the old days. For instance, at the Brookhaven pile all the fuel elements were pressurized, and one detected element failure simply on a large board which had hundreds of Bourdon gauges on it.

Mr. Hannum: Quite true, except we use thermocouples.

Mr. Porges: Would this be practical in a large reactor?

Mr. O'Neill (GE): I'm confused by the remark relating to fragmented release associated with vented fuel. Do you mean with any fuel?

Mr. Porges: Yes, I mean really with any fuel. We want a sure indication that the type of failure is indeed something to worry about and not just a pinhole. We've thought of putting sieves or something into the sample tube, but so far this is really quite an engineering problem.

Mr. O'Neill: You failed to mention one thing that I consider a disadvantage of fuel-failure location systems, and that is the simple fact that you have to place it in the way of refueling. This system is another system in between the core and the refueler, and that is a disadvantage.

Mr. Porges: I had this in mind; that's why I would say that there is really no hope of having a permanent manifold. I would have a remote-control engineer make an articulated tube which can normally be folded out of the way so it does not get in the way of refueling; then you have a gross fuel-element failure monitor which tells you that you have to stop when you get to half power. Of course, this is something which doesn't exist. It's a kind of a Buck Rogers thing.

Mr. O'Neill: I hope he works for G.E.

Mr. Leitz (GE): I would just like to comment relative to the specifications which you suggested that a vented fuel should attempt to meet to permit the detection or at least no interference with the present planned detection. The delayed time for the inert gases (and I would assume the halides would be

held up at least as long) was observed to be about 5 days, so this would seem to be no problem. When you suggested the use of charged-wire methods of observing the escape of fission products, perhaps you were thinking of the detection of 9-hr xenon; may I just observe that 5 days are equivalent to a 10^4 decay period. The noise from that source should be fairly low. You'll have to look for fresh fission product gases as opposed to those that are old relative to 5 days. The discrimination between these two should be no great chore I would think. Is that right?

Mr. Porges: Yes, in sort of an amplification of this, you want to multiply the number of fuel elements in the reactor. This gives you an idea of the attenuation you require for the activity that is being released by the vented pin in order to be able to detect the same activity from a fresh break. If you have a thousand fuel elements, then you want to go 10 half-lives roughly so as to provide a one-to-one signal-to-background ratio. With a 60-sec half-life, this is very manageable.

Mr. Jackson (Risley): What confidence do you have in your fast delayed-neutron monitor? What kind of response time do you have, and what kind of contamination is in the circuit anyway?

Mr. Porges: For a fast neutron emitted in about 2 liters of the pipe that is being viewed by the detectors, one can get of the order of 0.1 to 1% detection. It depends really how large you want to go: the number of detectors, graphite, etc. One can improve this by having a thin pipe coming up from where the sampling takes place and then have the pipe widen out in the detection volume. One can further improve this by having many, many detectors. Finally, there is the possibility of loading graphite with fission plates and getting neutron multiplication. With all this means one can certainly always get a very good neutron signal. There are, however, two unknown factors in all these detection schemes. One is the mixing. You release a certain slug of contamination into the coolant. How well does this mix and what is the shape of the signal? The second problem is, of course, that the flow pattern of many reactors is clearly unknown. There is also some indication from model tests from Cadarache that if you have a fuel-element failure in certain parts of the reactor it may take considerable time to reach a given sampling tube. It doesn't, perhaps, do much good to improve the neutron-detection signal because as time goes on the neutron background will build up, so that you have to keep on increasing the discriminator level; where this can end nobody knows. It's very difficult at this stage to say what kind of fuel-element failures we can detect with

any certainty. I'm afraid that doesn't show a very high degree of confidence.

Mr. Jackson: You suggested one might be able to locate a defective fuel pin by enclosing a pin in some way. We've certainly looked at this in the U.K. and are using it in a limited way. I'm surprised that you think you can use solids. Are you suggesting that the whole of the inside of the fuel pin be coated with a solid? I don't see how else you make it comprehensive. Am I missing a point?

Mr. Porges: This is still a very open question. It would seem that gold at least can be quite nicely bonded on the surface of the fuel pin and then over that would go the cladding. This was suggested by Bob Larsen as an answer to the uncertainty of coating with gas and also because the chemistry is extremely easy and well understood. I'm afraid that's

about as far as this idea has gone. The rest of the ideas haven't even proceeded that far.

Mr. Kirn (ANL): Perhaps I could answer your question about the sensitivity of delayed neutrons. There will be a paper at the Washington ANS meeting with respect to a bare-pin experiment in which a bare EBR-II pin was put into the reactor and both the delayed-neutron monitor and the fission gas monitor response was investigated. The background from the sodium was very low. I think the background from what we would call tramp uranium in this system was something like half a count per megawatt, and the signal from a bare pin, which is equivalent to about 50 cm of exposed surface, was something of the order of 2 counts per megawatt with the delayed neutron monitor. The fission gas monitor had about a 4 to 1 signal-to-noise ratio, and delay time was something like 20 to 30 minutes.

Preliminary Evaluation of a Technique to Study Expulsion of Sodium into Air

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Introduction

A technique has been developed to study the expulsion of molten sodium into air. This technique features an impact mechanism presently using a 0.38-cal Teflon pellet fired from a revolver and is limited to sodium ejections of about 2 gm. The work was initiated as a laboratory effort to determine if small quantity ejections of sodium into air could produce data capable of being extrapolated to full-size systems. If the small-scale experiments are appropriate to define the physical nature of the sodium-air reaction, then experiments on a larger scale with their associated costs in time and effort may be avoided.

The need to conduct these specific sodium-air reaction tests is related to the FARET containment design (see Fig. 1). Specifically, the containment design should accommodate a maximum pressure associated with the sodium-air reaction following explosive ejection of sodium into the air-filled containment.

The FARET Preliminary Safety Analysis Report⁽¹⁾ indicates that argon will be maintained initially within the containment structure. It is believed that at certain times during the life of the plant it will be desirable to provide air above the FARET reactor vessel, both in the cell and the cell annex (see Fig. 1). The most likely time at which one would desire to use air above the reactor vessel is during the regular, normal maintenance of control-rod drives and instrumentation when the reactor is shut down. Since hold-down of the vessel cover is presently not contemplated, one must concede that for containment-design purposes all of the sodium in the reactor might react with all of the available oxygen in the cell and/or cell annex above the vessel. The vault and reactor cavity will normally contain argon, so those volumes need not be considered to contribute to a sodium-oxygen reaction.

Criteria for the Experiments

In order to obtain engineering data related to possible FARET cell pressure, the following criteria for a small experiment were developed:

(1) All of the sodium available in a model core would be ejected into a model air cell.

(2) The sodium available would be limited to gram-sized quantities.

(3) The sodium temperature should be variable as desired up to 1200°F.

(4) The sodium in its capsule should be blanketed with an argon atmosphere separate from the air-cell atmosphere to prevent oxidation of the sodium during preheating.

(5) The velocity of sodium ejection should be about 100 ft/sec, corresponding to estimates of a maximum possible rate of ejection of the entire core contents.

(6) Air-cell pressure should be monitored with fast-response instrumentation.

(7) The sequence of sodium-air reactions should be photographed at high speed in an effort to gain some insight into the physical phenomena which accompany the expulsion of sodium into air.

Equipment Developed to Conduct Experiments

The earlier developed criteria were met with the apparatus shown in Fig. 2.

(1) The volume of the sodium capsule and the volume of available air were chosen to provide an approximate theoretical stoichiometric reaction (Na/O_2 mole ratio of 4.0).

(2) 1-2-gm quantities of sodium were contained in a thimble or capsule which is an upright cylinder with an open top and closed bottom.

(3) An external heater consisting of a Hevi Duty Electric Nichrome heater wire in a ceramic circular form was used to heat the sodium sample to temperatures up to 1200°F.

(4) The sodium-sample capsule rested on a circular-sheet, spring-steel impact diaphragm gasketed to a retainer ring. This provided for an argon seal at the bottom of the Sodium Capsule Barrel. A thin aluminum foil formed its own gasket at the top of the Sodium Capsule Barrel. Small ($\frac{1}{8}$ -in.-OD) gas inlet and outlet lines were welded to the Sodium Capsule Barrel to provide for a positive purge of argon during heatup of the sodium sample. The Sodium Capsule Barrel was maintained near at-

FIG. 1. FARET Containment.

mospheric pressure during heatup, and just before "firing" the gas supply was turned off.

(5) A separate set of preliminary experiments indicated that a 0.38-cal shell with a charge of 3.2 grains of Hercules "Bullseye" powder was capable of firing a Teflon pellet against the spring-steel diaphragm so that the resultant velocity of the sodium sample was about 100 ft/sec. The velocity of the sodium capsule was measured by a Hewlett Packard Electronic Counter with readout on a 502 Tektronic Dual Beam Scope. Two light screens by Industrial Instruments provided the source of light to photocells which was broken by the flight of the Sodium Capsule.

During the preliminary measurements of velocity, the sodium-containing capsule was permitted to enter the air cell. In subsequent tests, a mechanical stop was used to prevent the sodium capsule from entering the air cell. The capsule remained in

the argon firing chamber but on impact against the stop, the molten sodium was expelled into the air chamber. The Sodium Capsule acted as a "cookie cutter" to break the aluminum foil separating the argon atmosphere from the air cell.

(6) Air-cell pressure was measured by a 150.0-psig Consolidated Strain Gauge pressure transducer with a frequency response of 40 kc. The pressure signal was recorded on a frequency-modulated Precision Instrument magnetic-tape recorder with a DC response to 10 kc.

(7) High-speed photography was employed with a Fastax WF-3 camera with Ektachrome Commercial color film shot at 6000 frames per second.

Experimental Approach

Initially a large (20 in.) sheet metal cube for the Air Chamber was constructed. The box contained two gasketed Lucite windows opposing each other in the

FIG 2 Experimental Apparatus.

vertical plane of the box. The front window allowed for the high-speed photography and the back window permitted back lighting of high intensity.

To form a comparative basis to observe the expulsion of liquid sodium into air, water was expelled into air so that water droplet size and the "cone" or "shape" of the ejected water particles might be compared with those of ejected sodium. Also, a sodium-argon expulsion was conducted to determine if sodium droplets, dispersed into argon, behave similarly to water droplets dispersed into air.

Next, sodium was expelled into the same 20-in. air chamber.

These preliminary experiments indicated that molten sodium could successfully be expelled into an air

cell and that high-speed photography was very useful in observing the nature of the sodium-air reactions.

Early attempts at pressure measurement indicated a rapidly occurring peak pressure sufficiently high to cause leakage at the gaskets of the cube. Therefore steps were taken to provide for a more positive seal of the air chamber in following studies. A typical pressure-time trace is shown in Fig. 3.

Finally, three Lucite cylinders were fabricated for use as air cells. These cylinders were all of the same volume but of different H/D ratios. The volume was chosen to provide an approximate stoichiometric ratio of oxygen to about 2.0 gm of sodium.

Still photographs taken from the film are shown in Figures 4 through 6.

Results and Preliminary Conclusions

Preliminary conclusions and observations are as follows:

(1) It was observed in the water-air test that the particle size of ejected water was smaller than the particle size of ejected sodium into argon. Also it was observed that the "cone" of the water-air

FIG. 3. Typical Pressure—Time Response.

FIG. 4. Water into Air.

FIG. 5 Sodium into Air (20 x 20-in. Cube).

expulsion was wider than the "cone" of the sodium-argon reaction. These observations may be due to the fact that the surface tension of sodium is greater than that of water.

(2) It was observed, in the first sodium-air reaction in the 20-in. cube, that all of the sodium did not react instantaneously with the available air supply. There appeared to be an initial sodium reaction with the air immediately adjacent to the junction of the argon firing chamber with the air chamber. There was a considerable fraction of sodium which did not react until this sodium contacted air near the top of the air chamber. There was also evidence that for a finite period of time a small quantity of sodium did not react with air until the sodium droplets returned by gravity to the bottom of the air cell.

(3) The preliminary pressure measurements indicated that for constant-volume air cells, the measured pressure was greater for cells of smaller diameter.

Future Work

We are now starting to perform a statistically designed experiment to show that the geometry of the air container has a significant effect on the peak pres-

FIG. 6 Sodium to Air (Small Cylinder)

sure attained. A two-factor analysis with three levels of each factor with replication is being performed. The two factors to be considered initially are sodium weight and H/D ratio, with the air volumes being held constant. The sequence of conducting these tests will be randomized so that statistical inference may be drawn from the results.

Acknowledgment

The authors wish to acknowledge the services of retired ANL Sgt. Roy Andrews who assisted with the use of the hand gun, Mr. John McBride of the Reactor Engineering Instrument group, Mr. Lester Bohne, Reactor Engineering, and Mr. Daniel Giroux of Information Services who provided the high-speed photography.

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Discussion

Mr. Swift (ANL): Don't you think that the larger size of the sodium breakup is due to its higher surface tension? I think sodium has a surface tension of about 200 dynes/cm, whereas water has about one-third of this value.

Mr. Smith: That may be very true; it's a good point.

Mr. Bernard (AEC): Do you have any intentions of varying the velocity of the injected sodium and the ratio of oxygen to sodium?

Mr. Smith: Yes. The velocity is very easy to vary just by changing the charge of our 38 shell. We have tried to standardize on accurate loading of shells to get some preliminary data. We're also interested in what happens if there is a depletion of oxygen, particularly with respect to the effect on the ultimate pressure buildup.

Mr. Liimatainen (ANL): Can your data be compared to the work in an opaque system of Ross Humphreys and if so, are the pressures that you observe in the same ball park?

Mr. Smith: Our work is so preliminary that I would not like to comment on the absolute accuracy of our pressure measurements as compared with Ross's work. The asset of our system is that we can study many variables rather quickly because of the small size.

Mr. Dietrich (Combustion Engineering): How do the measured pressures compare with what might be expected from approximately complete reaction?

Mr. Charak (ANL): All one can do, as far as I know, is to compute so-called theoretical maximum pressures. To my knowledge there is little if any data on the reaction kinetics between sodium and oxygen. The theoretical maximum pressure computed for a 4:1 mole ratio of sodium to oxygen yields pressures of the order of 100–150 lb, depending upon atmospheric pressure. However, peak pressures occur at different mole ratios than one might expect from the basic stoichiometry. The pressures that we're getting are of the order of 20 to 80 lb.

Session VIII-B
October 14, 1965

Mostly Sodium Boiling

Chairman: P. A. LOTTES
Argonne National Laboratory

Secretary: R. M. Singer
Argonne National Laboratory

Effect of Fission Gas Bubbles on the Response of Molten Fuel Reactors to Fast Transients*

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Molten plutonium as a fast reactor fuel has been under development at Los Alamos for a number of years. The low-melting-point (411°C) alloy, Pu-Fe, was tested in LAMPRE I (Los Alamos Molten Plutonium Reactor Experiment, No. I) to about 0.5% burnup.⁽¹⁾ More recently, the ternary alloy Pu-Co-Ce is being tested to higher burnups in an in-pile loop.⁽²⁾ The latter alloy has the advantage that the plutonium fraction may be varied over wide limits without significant change of the melting point (~450°C). A core utilizing Pu-Co-Ce is in an advanced design stage in preparation for a test in the Fast Reactor Core Test Facility (FRCTF).^(3, 4)

Interest in molten fuels has, in part, been stimulated by the safety advantages offered by these fuels in fast reactor applications. These advantages derive from the large thermal expansion coefficients available in liquid metals. Linear expansion coefficients for Pu-Fe and Pu-Co-Ce are compared in Table 1.

The low expansion coefficient of tantalum, included in Table 1, plays an important role in amplifying the expansion effects occurring in tantalum capsules containing molten fuel. The type of fuel element under consideration is illustrated in Fig. 1. As the fuel is heated, the liquid fuel expands into the space provided above the fuel for the collection of fission gases. Since the tantalum expansion coefficient is less by about a factor of four than that of the fuel, the radial expansion of the capsule is small, and nearly the full *volumetric* expansion of the fuel is effective in increasing the core height, thus reducing reactivity.[†] Hence, the fuel temperature coefficient of reactivity is expected to be about three times larger for the liquid fuel than it would be if the fuel were in the solid state. The actual

isothermal temperature coefficients measured in LAMPRE are given in Table 2. (The constituent coefficients for the fuel and sodium were determined by assuming the total coefficient is resolvable into three components: fuel, sodium, and "structure.")

The effectiveness of the expansion coefficients in controlling transients in clean unirradiated fuel is illustrated in Fig. 2. The parameters plotted are the peak temperature, peak pressure, and energy release in the first burst caused by each given reactivity insertion rate in a LAMPRE-type core. In the analysis, it was assumed that the fuel-capsule walls and bottoms are fixed and rigid, and heat transfer from the fuel was neglected. For sufficiently rapid heating of the fuel, its expansion is inertially retarded, so that a pressure transient is induced. The pressure rise is at a maximum at the lower fixed end of the capsules and decreases to zero at the upper free surface of the fuel. The mechanisms are well adapted to the analytic methods for treating transients in fuel elements developed by Daane and Jens.⁽⁵⁾

The aim in the analysis of the clean-fuel transients was to investigate what range of reactivity insertion rates might lead to damage of the fuel capsule. In the results shown in Fig. 2, it is seen that the temperature rises ($\leq 10^\circ\text{C}$) for reactivity insertion rates up to \$100/\text{sec}\$ should have no adverse effects. However, the threshold for capsule damage due to the pressure rise might be expected to lie in the vicinity of the 7500 psi associated with the \$100/\text{sec}\$ ramp.

It would be expected that the transients shown in Fig. 2 would be significantly altered in irradiated fuel containing fission product gas bubbles. Long-term reactivity effects due to these bubbles were observed in LAMPRE, and the presence of the bubbles in the fuel was verified directly in postmortem capsule examination.

The history of reactivity losses occurring during the initial runs of a LAMPRE core is plotted in Fig. 3. The record shows some perturbations due to power level changes and reactor startups, but the overall trend is caused by the accumulation of fission gas bubbles. At the end of the irradiation, the total reactivity loss was about \$10, corresponding to 4.7 v/o

* Work done under the auspices of the U. S. Atomic Energy Commission.

† The effective expansion coefficient is thus

$$\alpha_{\text{eff}} = \frac{1}{H} \frac{\partial H}{\partial T} = 3\alpha_{\text{Pu}} - 2\alpha_{\text{Ta}},$$

where H is an increment of fuel height, and α_{Pu} and α_{Ta} are the linear expansion coefficients of fuel and tantalum, respectively.

bubbles in the fuel. No short-term power instabilities due to the bubbles were observed.

Photographs taken of sectioned fuel samples after the irradiation are displayed in Fig. 4. The upper photograph shows bubbles collected along the wall of a capsule, and the lower photograph shows bubbles in the meniscus at the upper surface of the fuel. No bubbles are observed in the interior of the fuel. The maximum bubble size is about one millimeter in diameter, but the bubbles average about 0.1 mm in diameter.

TABLE 1. LINEAR EXPANSION COEFFICIENTS

Material	Temperature	State	Linear Expansion Coefficient ($\times 10^6$)
Pu-Fe	500°C	liquid	28.8
Pu-Co-Ce		liquid	28.7
Ta		solid	6.5

FIG. 1. Molten Plutonium Fuel Element.

TABLE 2. LAMPRE TEMPERATURE COEFFICIENTS

Temperature, °C	Fuel State	Sodium Coolant	Total Co-efficient ($-c/^\circ\text{C}$)	Fuel Co-efficient ($-c/^\circ\text{C}$)	Sodium Coefficient ($-c/^\circ\text{C}$)
80	solid	out	1.1	0.8	
160	solid	in	1.3	0.5	0.5
480	liquid	in	2.9	2.0	0.5

To evaluate the effects of the bubbles on fast transients, a first approximation to the equation of state of the medium consisting of a mixture of liquid fuel and bubbles is considered. For a small volume, V_0 , of the mixture, the change in volume is the sum of changes in the liquid volume V_{0l} and gas volume V_{0g} :

$$\Delta V = -\frac{V_{0g}}{\gamma_c P_0} p(x,t) + V_{0l} \alpha T(x,t) - V_{0l} K_l p(x,t). \quad (1)$$

The adiabatic case is considered only, i.e., it is assumed no heat is lost from the liquid, either to the bubbles or to the external environment of the capsule. This will tend to overestimate the effects of the bubbles, since heating of the gas in the bubbles is an effect opposing the compression of the bubbles. In Eq. (1), $p(x,t)$ and $T(x,t)$ are the pressure and temperature rises at a point x measured from the bottom of the capsule; α and K_l are the volume expansion coefficient and compressibility of the liquid; γ_c is the ratio of specific heats in the gas.

During the course of an irradiation of the fuel, the steady-state pressure P_0 increases continuously in the sealed capsules as the inventory of fission gases increases. A pressure of 500 psi (corresponding to the maximum pressure observed in LAMPRE capsules) was assumed in the calculations for purposes of illustration.

In terms of the fuel displacement $E(x,t)$, Eq. (1) becomes

$$p(x,t) = \frac{1}{K} \left[\alpha T(x,t) - \frac{\partial E(x,t)}{\partial x} \right], \quad (2)$$

where K , the total compressibility, is

$$K = \frac{\beta_{0g}}{\gamma_c P_0} + K_l. \quad (3)$$

The liquid compressibility is about 1 part in 10^7 per psi of applied pressure. For a volume fraction of gas in the fuel, β_{0g} , of 0.047, taken from LAMPRE experience, compressibility of about one part in 10^4 is found from the first term in Eq. (3). Thus, the bubbles, as expected, enormously increase the compressibility of the fuel.

The hydrodynamic wave equation for the fuel mixture is

FIG. 2. Transients in Unirradiated Capsules.

FIG. 3. LAMPRE I: Reactivity Loss vs Integrated Power.

Below Meniscus

Meniscus

FIG. 4. Photomicrographs of Fission Gas Bubbles.

FIG. 5. Transients in Irradiated Capsules.

$$\ddot{E}(x,t) = c^2 \left[\frac{\partial^2 E(x,t)}{\partial x^2} - \alpha \frac{\partial T(x,t)}{\partial x} \right], \quad (4)$$

where the acoustic speed is

$$c = \sqrt{\frac{1}{\rho_0 K}} = \sqrt{\frac{\gamma_e P_0}{\rho_0 \beta_{0g}}}. \quad (5)$$

The acoustic speed from Eq. (5) is 7100 cm/sec, slower by about a factor of 5 than the speed in air. This slow speed is characteristic of a medium possessing both a high liquid metal density, $\rho_0 = 16$ g/cc, and a high compressibility.

Simultaneous solutions of Eqs. (2) and (4) and the neutron kinetic equations are depicted in the transients of Fig. 5. Because of the retardation of the fuel expansion due to the bubble compression, transients are prolonged, and the energy releases and temperature rises are larger. However, in the range of reactivity insertion rates up to \$100/sec, no damage to capsules would be expected because of the temperature rise.

On the other hand, the pressure rise is strongly inhibited by the bubbles. The bubbles, in effect, provide a protective cushion or internal shock absorber along the walls of the capsules. Thus, it is expected that irradiated capsules will withstand larger reactivity insertion rates than unirradiated capsules. It will be noted in Fig. 5 that transients for the bubble case are

extended only up to a reactivity insertion rate of \$10/sec. For larger rates where the pressure rise $p(x,t)$ becomes significant compared to the initial pressure P_0 , Eq. (3) is no longer a valid description of the compressibility. Both the compressibility and the acoustic speed vary during the course of such larger transients, and Eqs. (2) and (4) accordingly become nonlinear and intractable to the analytic methods used.

It may also be noted that the flexing of the capsule walls during transients in unirradiated fuel will produce effects similar, but smaller in magnitude, to the bubble effects. Wall flexing will lead to an increase in the effective compressibility or a decrease in the effective acoustic speed. Thus, the pressure rises for clean fuel given in Fig. 2 are overestimated, since they are based upon the assumption of rigid walls. Similar to the bubble case, the decrease in pressure rises is accompanied by an increase in temperature rises and energy releases.

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Discussion

Mr. Sesonske (Purdue): I missed a point whether or not you concluded that the collapse of the bubble did not inhibit linear expansion of the fuel because of the compressibility effects.

Mr. Carmichael: The compressibility of the fuel mixture with bubbles changes in character because it's much easier to compress the bubbles. On the other

hand, the energy releases and temperature rises are much greater with bubbles present since the transient is prolonged and expansion is retarded.

Mr. Morelle (Belgo-Nucleaire): What type of model did you use for calculating the speed of sound in the mixture of liquid fuel containing gas bubbles?

Mr. Carmichael: In the model it was assumed that the bubbles were spread homogeneously.

The Development and Growth of In-core Voids Due to Boiling during Fast Reactor Transients*

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An improved understanding of some of the characteristics of transient boiling in fast reactors has been achieved as the result of recent studies. Particularly significant among these characteristics are the following:

- 1) Voiding in a reactor channel starts near the upper boundary of the core and grows axially along the fuel channel.
- 2) Vapor production is so rapid that transient self-pressurization of the fuel channel occurs.
- 3) The resulting two-phase flow is strongly unstable hydrodynamically.

These findings have been developed from experiments and from studies with two digital codes developed at Atomics International for this purpose. The actual experiments and codes are described in detail elsewhere;^(1, 2) selected results of some of the digital studies and experiments are presented here to support and explain our main conclusions about the nature of transient boiling in fast reactors. Other significant results of the studies are developed in the discussion.

A. Single-channel Boiling

Steady-state boiling under forced-convection conditions has been studied and characterized by many investigators. Figure 1 shows the general picture for both metals and nonmetals that has been developed to explain many of the observed phenomena. The vapor first appears as small bubbles attached to the heated surface in a region where the bulk of the fluid is subcooled. As heat is added downstream and the bulk coolant temperature reaches saturation, large vapor agglomerations appear; then, an annular-flow region develops; finally, most of the liquid is evaporated and the heated walls dry out. This type of two-phase flow is characterized by constant mass flow rate everywhere in the channel and, in downstream regions, by relatively large differentials between the phase velocities (slip). Slip has an important influence on two-phase void fraction and pressure drop.

* Work performed under Contract AT(11-1)GEN-8 for the U. S. Atomic Energy Commission.

Recent measurements of these quantities made in a vertical, annular test section at AI are shown in Figures 2, 3, and 4. Note that steady-state slip ratios for sodium can be quite high.

Much steady-state heat transfer data can be applied directly to transients with confidence; however, the application of steady-state hydrodynamic data must be approached with considerable caution. It is quite certain that the condition of constant mass flow rate with position does not hold for transients and that the interpretation of slip ratios and void fractions for transients must be considerably different than for steady-state analysis.

The general picture of void configuration during transients developed from our studies is shown in Fig. 5B. The void fraction in the vapor region is near 100%, and this region is confined by two liquid slugs which are subcooled except in a narrow region near the void. The void grows primarily by pushing the liquid phase out of the channel rather than by evaporating the liquid. Because axial acceleration is constant for the incompressible liquid, pressure in the liquid regions increases linearly from the channel ends to the boundary of the void. The temperature distribution in the coolant depends on the history of the transient; however, there is always some sort of peak in the void region. Because vapor pressure is related exponentially to temperature, the vapor pressure curves corresponding to the bulk coolant temperature drop more rapidly than the total pressure curves as one moves away from the void region. The relation between the vapor pressure and total pressure curves is such that the liquid becomes more subcooled the farther it is from the void.

Other pictures of the void pattern during these transients might be postulated, and it is worth discussing these since the model used for analysis depends to a certain extent on basic assumptions about the vapor structure. A series of quasi-steady-state conditions could be considered as a possible description of transient boiling (see Fig. 5A). This approach is grossly in error, even for slow transients, because of

FIG. 1. Stable Steady-state Boiling.

FIG. 2. Measurements of Slip Ratio for Sodium.

FIG. 3. Two-phase Pressure Drop for Vertical Upflow of Sodium.

FIG. 4. Liquid Fraction in Vertical Upflow of Sodium.

FIG. 5. Idealized Picture of Single-channel Boiling.

the strongly unstable nature of the hydrodynamic situation if any boiling occurs in these channels. This applies for all power levels except perhaps for decay heat levels; if boiling should occur due only to decay heat, it could be relatively stable in some fast reactor configurations. As another alternate, it might be supposed that, because of superheating near the walls, bubbles would grow outward from the walls over a significant length of the channel (see Fig. 5C); or,

because of liquid-vapor interfacial instability, vapor from the high-void region might tend to slip out through the liquid, illustrated in Fig. 5D, as it does in steady state. These alternate pictures are not supported by the results of calculations primarily because of the relatively high calculated inertial pressures generated by the expanding void and the consequent tendency for quenching of vapor bubbles in regions away from the primary void. Also, the high heat transfer coefficient of the liquid metal minimizes wall-to-bulk temperature differences and reduces the axial region in which subcooled boiling can occur to a narrow range. The self-pressurizing effect keeps most of the liquid below its saturation temperature and probably prevents vapor formation or intrusion in most of the liquid region. Therefore, two-phase flow in the ordinary manner (parallel flow of the phases) probably is not present to any significant degree during the important initial phase of the transient. Since such two-phase flow is not present, it is not essential to calculate void fraction or slip ratio accurately in order to determine transient hydrodynamic behavior.

The preceding is not meant to imply that the calculation of void fraction is not an important result of the analysis, since it obviously is. However, the important transient fluid motions are more a function of transient buildup of vapor pressure than of the potential void generation. The void develops as a consequence of the motion of the liquid phase away from the vapor region and not vice versa. The important analytical problem is the determination of pressures.

B. Heat-transfer-limited Void Growth

Our first approach to the problem of calculating these transients was to assume that vapor formation was primarily determined by the heat input to the coolant, but that it might be modified by pressure buildup that occurred as a result of acceleration. The code TRANSFUGUE-I was developed from this point of view as a method of solution of the classical hydrodynamic differential equations for one-dimensional, variable-density flow. The solution is programmed in a general form which in principle does not rely on any assumptions about the limiting factors in void growth.

However, problems with numerical instabilities have thus far prevented its application without some artificial restrictions on certain parameters which tend to contribute to these instabilities. These restrictions apply to the factors that modify vapor production as a result of inertial pressure buildup and therefore tend to cause overestimates of void growth, that is,

the calculated inertial pressures are effectively decoupled from the solution of the rest of the problem and, therefore, do not affect the calculated void volume. This is accomplished by holding vapor density and saturation temperature constant everywhere in the channel. The stable solutions that have been obtained with this approximation are valid for the limiting case in which inertial self-pressurization is not sufficient to alter the rate of growth of voids significantly. Because of this fact, studies with this code have provided much of the quantitative data which have formed the basis of the picture of void growth outlined above.

Figure 6 shows schematically the hydraulic and heat transfer characteristics of a sodium-cooled, fast reactor fuel channel that has been studied with the TRANSFUGUE-I code. The transient started from full-flow, full-power conditions, and loss of flow was simulated by suddenly reducing the inlet pressure to a value corresponding to the natural convection head of the system. Rapid flow decay occurred, leading to boiling in about 0.8 sec as power was held constant. Figure 7 shows sodium temperature profiles calculated during the transient using 85 axial nodes.

Note that boiling does not start at the center of the core but near the outlet interface between core and blanket. A number of power and loss-of-flow transients have been studied recently, and this type of voiding pattern has been found always to occur in transients that start from full-power conditions. There is little doubt about the validity of this result because it does not depend in any way on the boiling model used. Prediction of the initiation point for the void depends almost exclusively on the transient heat transfer and single-phase flow solution to the problem. Some uncertainty of the exact timing does result from uncertainty about the degree of superheat required to nucleate the first bubble, and some statistical uncertainty must be associated with the precise location of a favorable nucleation site. However, the sharp peaking of the vapor pressure curve guarantees that the void will start at a location near the core outlet.

Description of the subsequent growth pattern and growth rate of a void is more difficult and uncertain because of the complexity of the situation and the limited experimental data available. Certain characteristics are clear, however. One is the presence of strong hydrodynamic instabilities. These were predicted for boiling in the Enrico Fermi Reactor as far back as 1957.⁽³⁾

All studies of boiling in sodium-cooled fast reactors are in agreement with the conclusion that the two-phase hydrodynamics are strongly unstable. This natural instability has two important consequences: it

FIG. 6. Schematic of Fuel Channel.

FIG. 7. Sodium Temperatures during Loss-of-flow Transient.

means that void growth is (1) rapid and (2) difficult to analyze numerically. Problems with numerical instabilities in the TRANSFUGUE-I calculation method are closely related to this characteristic. Rapid void growth is a consequence of the unstable flow-choking effect of boiling, which is autocatalytic. Under these circumstances, power sufficient just to initiate boiling results in complete voiding of the channel without

further increase in power. There is a tendency for sodium to re-enter a channel after the initial voiding transient. The unstable nature of the hydrodynamics means that the re-entrant flow will be unstable and some sort of chugging behavior will occur. Such behavior may have important influences on the ultimate outcome of a given transient and will be the subject of significant research in the future.

FIG. 8. Results of Calculations of Sodium Pressure for Loss-of-flow Transient.

C. Pressure-limited Void Growth

The inertial pressures calculated by TRANSFUGUE-I for the loss-of-flow transient are plotted in Fig. 8 together with curves of sodium vapor pressure corresponding to the temperatures shown in Fig. 7. Study of plots of this type for a number of loss-of-flow and power transients has led to the general conclusion that self-pressurization occurs in most transients initiated from full power. As indicated previously, saturation temperature and vapor density were held constant in this calculation for reasons of numerical stability, thus yielding a heat-transfer-limited rate of growth of voids. The calculated inertial pressure is the pressure required to move the liquid out of the way to make room for the growing void under the assumed circumstances. The calculation clearly shows that more pressure is required for this type of void growth than is available from the only source of pressure, namely, the vapor pressure of the sodium. This means, simply, that the void will not grow at a heat-transfer-limited rate, but will grow at a slower rate controlled by the rate of increase of vapor pressure at the hottest spot. It also means that the maximum pressure will be no less than the maximum vapor pressure of the hottest liquid sodium in the channel. This is the self-pressurization effect to which we have referred.

Figure 8 also shows the typical relationship between the shapes of the vapor pressure and inertial pressure curves. The total pressure gradients in the liquid regions are constant because acceleration is uniform. The inertial pressure curve at 0.86 sec and the vapor pressure curve at 0.88 sec together are a fair represen-

tation of these quantities for a solution at 0.88 sec corrected for the effect of self-pressurization. In other words, the true inertial pressure curve for 0.88 sec probably should be about in the position of the 0.86-sec curve. Comparison of these curves then shows the rapid development of subcooling in regions away from the void. Both power and loss-of-flow transients show this type of behavior, and it is believed to be fairly general for fast reactors at substantial power levels.

In certain power transients initiated by relatively large reactivity insertions ($> \$1.00/\text{sec}$), the power level will rise so rapidly that the critical (burnout) heat flux will be exceeded before bulk boiling starts. Because of the relatively high heat transfer resistance introduced by film boiling under such circumstances, it is possible that heat transfer limitations then would be the controlling factor in determining void growth and that the self-pressurization effect would be secondary. Our studies of this aspect of the problem are not complete; however, they do indicate that film boiling can, under the proper circumstances, have an important influence on void growth and pressure buildup. A number of experimental measurements of critical heat flux for sodium in forced convection have recently been completed at our laboratory. Figure 9 shows critical heat flux trends in both the subcooled and saturated boiling regions. The details of this work are being published.⁽¹⁾

Void growth characteristics determined from interpretation of results of TRANSFUGUE-I calculations of power and loss-of-flow transients in typical fast reactors have been used as the theoretical basis of

FIG. 9. Critical Heat Flux for Sodium under Forced Convection.

FIG. 10. Geometry Assumed in BURP Model.

another calculational method which avoids some of the numerical problem areas of TRANSFUGUE-I. The fundamental assumptions that are used to construct this model are:

- 1) The void spreads axially along the channel from a single starting point, as shown in Fig. 10.
- 2) Pressure in the void region is equal to the highest vapor pressure of the bounding liquid.
- 3) The self-pressurization effect suppresses boiling away from the growing void region.
- 4) Void fraction within the vapor region is 100%.

The second assumption regarding pressure in the

void region is the key factor in the analysis and is also subject to the greatest uncertainty. In the digital code based on this model (BURP), temperatures in the liquid regions are calculated numerically by the usual finite-difference equations and searched for a maximum which usually occurs at the lower liquid-vapor interfacial node. This temperature is converted to pressure by use of the vapor pressure relationship for sodium, and this pressure is then assumed to exist in the vapor region. This ignores possible pressure sources in the vapor region and also ignores the condensing effect on the vapor pocket that would tend to occur when the upper boundary moves into relatively cool blanket and upper plenum regions. Determination of better assumptions to use in determining pressure in the void must await the completion of appropriate experiments.

In any event, the BURP method yields the best available estimates of void growth rate and pattern and pressure buildup for the class of fast reactor transients to which it applies. As examples of results of calculations based on this method, a loss-of-flow and a power transient have been selected. The loss-of-flow transient (see Fig. 11) is for the same system and conditions described previously for the study with TRANSFUGUE-I. The ramp power transient (see Fig. 12) is for a similar reactor which has a 4.3-ft-long core instead of the 3.0-ft core studied in the loss-of-flow situation.

Results confirm previous calculations indicating that the void starts near the outlet core-blanket interface and tends to work into the upper blanket before the core. This characteristic is most pronounced for the power transient in which the pump keeps running.

FIG. 11. Maximum Pressure and Location of Void Boundaries after Boiling in Flow Transient.

FIG. 12. Velocity, Maximum Pressure, and Void Boundaries after Boiling in Ramp Power Transient.

An approximately 0.15-sec delay occurs between the starting of the void into the upper blanket and starting of the void into the core. The reason for this delay is explained best by study of the velocity plot shown in Fig. 12. Coolant moving through the core must turn around, i.e., go from a positive to a negative velocity, before the void can grow downward into the core. If a relatively high upward velocity exists when boiling starts, a certain time will be required to stop the lower liquid column and get it moving downward, and the

core will remain flooded during this time. No such conditions apply to the upper blanket region, so the void will develop more rapidly there.

With the exception of the consideration of surface-to-bulk radial temperature variations, this discussion and analysis has been limited to the one-dimensional problem. It is believed that void-growth patterns and pressures calculated by this method can be applied with reasonable confidence to single fuel elements typical of most proposals for fast reactor design.

However, further investigation of the effects of pin-to-pin power variations and local flow variations certainly is required. In order to obtain void effects for the core as a whole, the multichannel problem must be solved together with nuclear feedback effects. Although complicated in application, the multichannel problem is relatively simple in principle once a satisfactory method for single-channel analysis is in hand. It is expected that continued development along the lines outlined here will yield the desired result.

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Discussion

Mr. Sanathanan (ANL): You mentioned that the slip ratios could be very different during transients. Could you explain how you derived the transient slip ratios?

Mr. Noyes: With the picture I postulated of transient boiling in a fast reactor with substantial power, the slip ratio almost has no meaning at all. The vapor bubble just grows as a unit, and there is really no parallel flow of liquid in the vapor that is significant in the problem until the initial bubble reaches the boundary of the core. Then with reentry and so forth you get a situation in which slip would be involved. Another factor is that when you start the transient, the slip ratios will tend to be lower because the starting condition is essentially a slip ratio of one. On generating the bubbles have the same velocity initially as the liquid, and the acceleration has to occur over some period of time. Somehow during the transient, the slip, if there is a slip, would tend to be lower. We haven't calculated the slip ratios or measured them during any transient. This is qualitative speculation.

Mr. Sanathanan: Would it not be better to go with the hydrodynamic equations themselves rather than introducing the slip ratio?

Mr. Noyes: Homogeneous model?

Mr. Sanathanan: No. I don't expect a rigorous solution but you have to have something to help specify what happens at the time boiling starts. This was your difficulty, was it not?

Mr. Noyes: No, not really. It's a homogeneous model, and I think that the arguments that I just mentioned tend to justify using a homogeneous model.

You essentially assume the slip ratio is one, and it makes the problem a lot more tenable mathematically.

Mr. Sanathanan: Yes, but mathematical difficulty should not prevent a physical explanation.

Mr. Noyes: Well, I didn't mean to imply that. I hope I didn't.

Mr. MacFarlane (ANL): I'm a little confused on your comments about the unstable nature of two-phase liquid metal boiling, in particular, your comments about numerical instability. Does this indicate anything physical about the unstable nature of boiling in the transient?

Mr. Noyes: Two things are involved: numerical instabilities and physical instabilities. I tried to distinguish between them. So far as the physical instabilities are concerned, under fast reactor conditions with substantial power, at anything above decay heat level the flow is as strongly unstable. This is related to the numerical instability. The numerical problem depends on the numerical approach used. We are using Euler integration. In sequential integrating procedures, very often the physical instabilities tend to be amplified by the numerical technique.

Mr. MacFarlane: People postulate that the instabilities in loops are concerned with things like liquid superheat, nucleation sites, and the like. Now, any model you could develop to do a transient two-phase calculation is by definition going to be somewhat limited and cannot include these effects. Therefore I think that any conclusions about unstable flow from these numerical results is somewhat subject to question.

Mr. Noyes: I disagree. First of all, you don't have to make transient calculations to predict instabilities. The calculations that were made in 1957 and for Fermi were steady-state calculations. They showed a very strong negative slope and check curves which have always been shown to be associated with hydrodynamic instabilities. I am not talking about nucleation instabilities or superheat instabilities; those are another type of instability.

Mr. MacFarlane: But that's a steady state analysis, isn't it?

Mr. Noyes: That's right.

Mr. MacFarlane: I think this is a very weak approach.

Mr. Noyes: It always works.

Mr. Häfele (Karlsruhe): I would like to point out that numerical instabilities can be avoided principally by a certain technique of integration of the Eulerian equations. For example, you are given a curve which contains some initial conditions, say of the velocity, density, or whatever. You have to integrate along the characteristics. On the inside of the area the flow pattern is determined by the initial conditions. What one

does usually in the numerical procedure is to work along a network. Then it can easily happen that the flow pattern is determined numerically although it cannot be determined physically from the starting conditions. Therefore, if one applies such a technique of integration, these numerical instabilities can be avoided. One has to have in hand the velocity of sound, because the slope of the characteristic is dictated by the velocity of sound, and therefore one has to make an evaluation of what the velocity of sound is in this two-phase flow. I agree that there are a number of physical reasons for instabilities and we feel partly that they are connected with the question of shock waves. If one investigates the velocity of sound in the presence of bubbles, it isn't too difficult to handle the question of shock waves at the same time.

Mr. Noyes: I generally agree and we have started on this sort of approach to the problem so far as detailed calculations are concerned. However, I think that for making calculations for safety studies, where you want to get an idea primarily of the voiding rate or the rate of ejection of sodium, the simple model that I presented probably will be good enough.

Mr. Fischer (Karlsruhe): Did you take into account the kinetic energy in the basic differential equations?

Mr. Noyes: Yes.

Mr. Fischer: Did you consider the difference between the entropy and the internal energy in these equations?

Mr. Noyes: Yes. I did a separate analysis of that problem which is really not contained in the writeup on TRANSFUGUE and tentatively came to the conclusion that it was not important in these problems to differentiate between enthalpy in that way.

Mr. Fischer: The difference is always the work of displacement, and I believe that as expansion progresses this difference is essential.

Mr. Cobb (Nuclear Power Group, England): In your description of the first model you talked about inertial pressure drop; I didn't hear you mention frictional pressure drop at all. Do you allow for that in the model?

Mr. Noyes: Yes, the friction is there. It isn't very important in these terms because the dominant term during the transient is the inertial pressure.

Mr. Cobb: I would have thought that you were talking about voiding in less than 0.1 sec; in that case the velocities would be quite high.

Mr. Noyes: Yes, and inertial pressures are quite high, too.

Mr. Cobb: I thought the friction might even be equal to the inertial pressure drop.

Mr. Weills (ANL): I would like to make one little comment after seeing the pressure pulse going up to 300 psi. This is enough in many designs to cause a ballooning of the hexagonal tube, which would make the modular type of reactor shut itself down due to radial expansion. This should be part of a complete analysis of the reactor kinetics coupled with the hydrodynamics.

Mr. Noyes: Yes, it could. The existence of that pressure is over a very short period of time, though.

Mr. Carmichael (LASL): I'd like to comment on Dr. Häfele's remark about the speed of sound. Isn't the problem essentially one that some fraction of the energy may be transmitted directly through the liquid without affecting the bubbles? It's sort of like a heavy spring that is impacted and you get a high frequency ping that's transmitted directly through the metal whereas probably the bulk of the energy goes into compressing the spring. In the calculations we did recognize this; and certainly neglecting this sort of energy overestimates the effect of the bubbles. In our application, also, there will be bubbles clinging to the wall and not homogeneous through the liquid.

Mr. Fauske (ANL): I would like to point and something of importance about this sonic velocity business. If you do perform a calculation of the sonic velocity in sodium at atmospheric pressure, assuming equilibrium, you find out that the sonic velocity is on the order of 5 feet a second, which is very low. Now, another way of calculating this is to assume complete metastable conditions where the sonic velocity does not see a phase change. In this case you get a sonic velocity of the order of 80 feet a second. In any case, or in both cases, I think this is of very much importance in the expulsion of sodium from the tube. As a matter of fact, in the instant you form vapor you probably have a choking type phenomena. This can cause another hydrodynamic instability. There is an experimental observation of the velocity of sound in a two-phase mixture which indicated these low velocities.

Mr. Lottes (ANL): Do you have a reference?

Mr. Fauske: There is a Russian paper. I don't remember the author. Kasmin or something like that. It concerns work on steam-water where they actually show that the velocity of sound propagates with a metastable behavior. They also have the newest measurements on the velocity of sound in a two-component system. Air-water mixtures with primarily bubble flow show an extremely low velocity of sound.

Coolant-ejection Studies with Analogy Experiments

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1. Introduction

An important part of the Karlsruhe Fast Breeder Program* deals with safety questions. The coolant behaviour during a reactor accident is of particular interest, especially the ejection of the coolant from the channel. At Ispira, the experimental program is concerned with liquid metals and with analogy experiments with other liquids. Preliminary experimental results are presented in this paper.

Among the potential causes of boiling excursion with consequent coolant ejection are the following:

- 1) an increase of heat production;
- 2) a decrease of coolant velocity;
- 3) a decrease of pressure.

The first series of experiments dealt with a special combination of 2) and 3): the ejection of primary stagnant coolant from a single vertical channel as a consequence of a sudden depressurization. Water was taken as test liquid. The process was observed by taking high-speed movies.

2. Experimental

The test section is a simple glass apparatus (see Fig. 1) consisting of a central test tube of 7.2-mm diameter, surrounded by a jacket for preheating with glycerine. The top of the test tube expands to a 20-mm-diameter tube which is surrounded by a water-cooled jacket. The whole assembly is closed at the top by a thin plastic diaphragm (the cooling is necessary to protect this diaphragm from the water vapor). An electromagnetic rupture device perforates the diaphragm at a fixed delay time after the camera starts. The liquid column can be pressurized up to 4 atm; the diaphragm withstands this pressure until it is perforated, and then ruptures immediately and completely.

The movies were taken with a Fairchild camera using photography by transmitted light. Illumination was provided by three 500-W lamps with a screen behind the heating jacket. On the film, the vertical channel has a horizontal orientation. This was done to obtain a larger image of the object. With the 35-mm objective, the camera-object distance was 1.2 m.

* This work was done within the framework of the Karlsruhe Fast Breeder Project (Association EURATOM/Gesellschaft für Kernforschung).

Two series of experiments were made, one with double-distilled, thoroughly degassed water and the other with normal non-degassed water. The starting temperatures T_1 in both cases were: 105/107.5/110/112.5/115/117.5/120/125°C. The temperature was measured with a thermometer at the top of the heating jacket. The temperature fall along the jacket was less than 0.3°C.

Before starting a test the water column was pressurized up to 3.5 atm, then heated up from 100°C to the desired temperature. Immediately after arriving at this temperature, the camera was started. After a fixed delay time of 1 sec, necessary for allowing the camera to arrive at the desired speed of ca. 3,000 frames/sec, the diaphragm was perforated by the rupture device.

The rupture results in a depressurization wave which arrives at the surface of the water column about 10^{-3} sec later. Taking into account that the pressure ratio from starting to atmospheric pressure is supercritical, about 3 reflections at the outlet (diaphragm position) and closed end (top of the water column) of the "pipe" are necessary to obtain atmospheric pressure at the surface of the water column. That is to say we can assume atmospheric pressure from 3 to $5 \cdot 10^{-2}$ sec after the diaphragm rupture. This is still a short time with respect to the duration of the ejection process itself, which is about 0.1 to 0.2 sec.

Thus we can state the following boundary conditions:

$$\left. \begin{array}{ll} t < 0; & p = 3.5 \text{ atm;} \\ t \geq 0 & \left\{ \begin{array}{l} p = 1.0 \text{ atm;} \\ T_1 = 105/107.5/110/112.5/ \\ 115/117.5/120/125^\circ\text{C} \end{array} \right. \end{array} \right\} 0 < x < 1,$$

that is to say, the whole column is equally superheated with respect to the atmospheric pressure.

3. Evaluation of the Film

The liquid phase appears on the image as a black shadow, whereas the vapor phase is white. Thus, the liquid-phase velocity and the movement of vapor bubbles can be distinguished. When the flow pattern has transformed into mist flow (see point 2), one can still observe the movement of water droplets but no

longer the vapor-phase velocity. The slip ratio cannot be measured in this region.

Observing the film, one can give a qualitative answer on the question of how the ejection process occurs:

1) Immediately after arrival of the depressurization wave a great number of bubbles is formed over the whole length of the water column. The bubble density diminishes with the distance from tube exit. The medium bubble density grows with the superheating temperature. The bubbles are more numerous in the case of non-degassed water than with degassed water.

2) The acceleration of the water-bubble mixture starts at the exit of the tube in form of an "acceleration front" which propagates along tube against flow direction. The depth of this front is several centimeters. When the acceleration front passes a certain position, the flow pattern changes from a liquid-phase flow (with enclosed small vapor bubbles) of low velocity to a mist flow of high velocity. The movement of the acceleration front is changed by statistically distributed disturbances, but the medium speed of the front is practically constant.

3) This phenomenon becomes more regular with higher superheating temperatures, i.e., with a greater number of previously formed bubbles. The gas content of the test liquid has the same effect.

4) In the case of double-distilled degassed water up to superheating of 20°C, a certain amount of superheated liquid remains within the tube (see Fig. 2) after the initial ejection. Depending on occasional disturbances, it is expelled some seconds later, similar to the retarded boiling phenomenon.

5) The downstream flow (behind the acceleration front) appears to be critical flow. This is consistent with the observation at the tube exit that the velocity increases in a region of expanding flow area.

The medium velocity of the acceleration front was measured by observing the time span τ over the tube length. In the case of degassed water, the distance between exit and surface of this rest column was taken and divided by the respective time span.

The results are presented in Fig. 3. There is an obvious difference between the behaviour of degassed and non-degassed water. At superheating temperatures less than 15°C, the velocity is markedly higher with non-degassed water than with degassed water. At higher superheatings, the influence of gas content diminishes and the experimental points join to one curve.

4. Theoretical Considerations

To simplify the calculations, we take the transfer from liquid-phase flow to mist flow to occur in a front of negligible depth (as in shock or detonation fronts).

With this assumption we can obtain a simple station-

FIG. 1. Test Section.

ary problem, taking a coordinate system travelling with the front and applying the energy, momentum, and mass balances. Figure 4 presents the basic equations. We have a system of 3 equations in 4 unknown quantities:

$$u_1, u_2, T_2, \text{ and } x.$$

The values of pressure and vapor density are given by temperature relations assuming thermodynamic equilibrium. For the density ρ_1 , the liquid density ρ_l was taken, i.e., we neglect the influence of the little bubbles on density. By reason of the fact that we have only 3 equations for 4 unknowns, one additional assumption on the downstream conditions is required.

Firstly, one can make the assumption—which is always made in subsonic flow—that the pressure at the tube exit is equal to atmospheric pressure. (The pressure drop along the test tube and the "pipe" between test tube exit and diaphragm shall be neglected.) The results of the calculations with this assumption are presented in Fig. 3 (lower curve).

However, the film evaluation suggests using the critical condition in the downstream region (behind the

FIG. 2. Remaining Column after Ejection.

FIG. 3. Medium Velocity V of the Acceleration Front.

front). Critical velocity is defined by

$$C_c = \sqrt{\frac{dp}{d\rho_{\text{adiab}}}}$$

The results of the calculations for a vapor-water-droplet mixture are presented in Fig. 5. The critical-flow relation is based on the assumption of unit slip ratio (vapor and liquid phases have the same velocity). The critical velocity of a mixture depends on two parameters: temperature and steam quality x or an-

ENERGY: $\frac{1}{2}(u_1^2 - u_2^2) + \frac{1}{\rho_1}(P_1 - P_2) + C(T_1 - T_2) = X \cdot L$

MOMENTUM: $P_1 + \rho_1 u_1^2 = P_2 + \rho_2 u_2^2$

CONTINUITY: $\rho_1 u_1 = \rho_2 u_2$

$P = p(T) \quad \rho_1 \approx \rho_l \quad \rho_2 = \rho_l / (1 + X \cdot \rho_l / \rho_v)$

$\rho_v = \rho_v(T) \quad C_c = \sqrt{(dp/d\rho)_{\text{adiab}}}$

$\rho_v = \text{VAPOUR DENSITY} \quad X = \text{STEAM QUALITY}$

FIG. 4. Acceleration-front Velocity V ; Basic Equations.

other quantity related to x . In Fig. 5 the specific volume of the mixture was taken for abscissa.

A graphical method was used to solve the system of equations. The critical velocity and absolute downstream velocity were plotted against the downstream temperature T_2 . The intersection point gives the desired values of T_2 . The result is presented in Fig. 3 (upper curve).

Comparing now the experimental points with the curves, we must prefer the critical velocity assumption, especially with higher superheatings, where the deviation is about 20%. Taking into account the simplicity of the model, the agreement is satisfactory.

Figure 6 shows the results of another very simple calculation with a model which can be called the "column ejection model." In this model we assumed that the pressure p_1 related to the starting temperature T_1 expels the whole liquid column with only one expanding bubble at the bottom of the tube. It is to be seen that, at least from the theoretical standpoint, the ejection in form of big slugs should occur quicker than in form of a propagating acceleration front.

On the other hand, we must take into consideration that the assumption of thermodynamic equilibrium may not be valid. The nucleation and evaporation rates must play a significant role, especially with lower superheating and with a small number of bubbles. The discrepancy between the results obtained with degassed and non-degassed water confirms this.

FIG. 5. Critical Velocity of a Water-Vapour Mixture.

FIG. 6. Medium Velocity V of the Acceleration Front.

5. View on Future Program

In the future, the experiments will be extended in the following directions:

- 1) variation of geometrical dimensions and test liquid;
- 2) extending the superheating range;
- 3) partial superheating of the column.

These experiments are directed toward extending the range of variables so that the formulation of mechanisms and analytical models might be adequately established, and subsequently applied to liquid metals.

6. Acknowledgment

The author wants to express his thanks to Prof. Häfele and Dr. Grass for sponsoring this work and helpful discussion.

Discussion

Mr. Noyes (AI): Have you done any experiments, or do you intend to do any experiments, in which there is an axial temperature gradient along the channel?

Mr. Friz: Not up to now. We plan to use a longitudinal temperature gradient in our next experiment, that

is, a part superheated and another part not. In this case we start with *stagnant* liquid. We cannot maintain a radial temperature gradient; otherwise we should have a loop and this is a much more complicated device. For the next year we shall probably continue to work with stagnant liquids.

Mr. Cobb (Nuclear Power Group, England): Two very simple points I seem to have missed. First I take it the image on the film was reversed. Were the voids the dark areas?

Mr. Friz: The dark area was the water and the clear area was the vapor.

Mr. Cobb: And the other thing was, you started with a column of superheated liquid. How did the boiling start? What initiated it?

Mr. Friz: It started by the rupture of the diaphragm. The column was first pressurized to 3.5 atm. The diaphragm was then ruptured, producing depressurization down to atmospheric pressure and instantaneous superheating.

Mr. Cobb: That superheat referred to the pressure after you passed the diaphragm.

Mr. Friz: Yes, with respect to the external pressure.

Shock Front Conditions in Two-phase Flow Including the Case of Desuperheat*

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1. Introduction

The investigation of the safety of large fast sodium-cooled reactors leads to the problem of coolant behaviour in the cooling channels during a power excursion.⁽¹⁾ This is primarily a hydrodynamic problem. But it is linked to the neutron behaviour of the core via the coolant void effect. The time characteristic for the expulsion of coolant together with the reactivity value of voiding the coolant channels result in a ramp rate of a positive reactivity insertion. The reactivity value of voiding the coolant channels was calculated in a recent study. The first value calculated was \$3;⁽²⁾ more recent results indicate a value of nearly \$7.^(2, 3) But the duration of the expulsion of the coolant is probably even more uncertain. Its calculation is a problem of two-phase sodium hydrodynamics. Attacking this problem, one realizes soon the crucial importance of shock-front phenomena. The appearance of shocks tends to bring the integral process of expulsion to a time scale which is dictated by the velocity of sound, because after having passed a shock front, the velocity of the medium is subsonic. Therefore, both the investigation of the shock-front conditions and the velocity of sound are necessary as first steps for a more complete theoretical analysis of the whole process of the expulsion of the coolant.

A recent analysis of Smidt⁽⁴⁾ indicated that superheating and desuperheating may play an important role in the safety analysis. In particular, the plugging and superheating accident of a subassembly located near the center might lead to a severe accident even if the safety system is operative.⁽¹⁾ Therefore, it was important to include in this analysis the phenomena of superheat and desuperheat.

It should be mentioned that parallel to the described theoretical effort of the Karlsruhe group, experimental studies have been started and will be increasingly pursued. In particular, the work of the Ispra group should

be mentioned. Friz⁽⁵⁾ reported the first such experimental results.

2. The Euler Equation of Two-phase Flow

The conservation of mass, momentum, and energy of a homogeneous two-phase flow is expressed in differential, one-dimensional form as follows:

Continuity:

$$(2.1) \quad \frac{\partial \rho}{\partial t} + \frac{\partial(\rho u)}{\partial z} = 0;$$

Momentum:

$$(2.2) \quad \frac{\partial(\rho u)}{\partial t} + \frac{\partial(\rho u \cdot u)}{\partial z} = -\frac{\partial p}{\partial z};$$

Energy:

$$(2.3) \quad \frac{\partial}{\partial t} \left[\rho \left(e + \frac{u^2}{2} \right) \right] + \frac{\partial}{\partial z} \left[\rho u \left(h + \frac{u^2}{2} \right) \right] = \frac{q}{d},$$

where ρ is the density, u the velocity along the z axis, p the pressure, e the internal energy, h the enthalpy, q the heat flux per cm² rod surface, d a characteristic width of the cooling channel, t the time, and z the coordinate along the cooling channel.

Some remarks on these equations should be made:

1. The only independent variable introduced apart from the time t is the axial coordinate z along the cooling channel. The radial gradients of the densities, velocities, and pressures are not taken into account here.
2. In the transient expulsion process which is of interest here the gravity forces are neglected in the equation of momentum.
3. In the energy equation the contributions from the internal energy, kinetic energy, the heat input at the wall, and the work of displacement are taken into account. The enthalpy appears only as an abbreviation of the term $u + (p/\rho)$, as usual.

Lurie and Noyes⁽⁶⁾ neglect the difference between internal energy and enthalpy. Both Noyes and Becker *et al.*⁽⁷⁾ also neglect the term involving kinetic energy.

* This paper has been prepared within the framework of the association EURATOM—Gesellschaft für Kernforschung mbH. in the field of fast breeder development.

But the contribution of the kinetic energy to eq. (2.3) is essential for the expulsion mechanism which is considered here.

In concurrence with Lurie and Noyes and Becker *et al.* the contributions of forces of dissipation and gravity to the energy balance (2.3) are neglected.

The energy equation (2.3) may now be brought into a simpler form by the introduction of the entropy. This offers advantages for the solution of the set of eq. (2.1)–(2.3). One obtains

$$(2.4) \quad \frac{\partial(\rho s)}{\partial t} + \frac{\partial(\rho u s)}{\partial z} = \frac{q}{dT},$$

where s is the entropy and T the temperature.

Consistent with this set of differential equations are the integral conditions of the next section. They can be obtained for the stationary case, $\partial/\partial t = 0$, by integrating once along the z axis.

3. Shock Fronts in Two-phase Flow

The motion of the shock fronts considered here shall be quasi-stationary. By transformation of the reference system to the shock front, stationary conditions are the result which are described by the following set of shock equations:

Continuity:

$$(3.1) \quad u_1 \rho_1 = u_2 \rho_2;$$

Momentum:

$$(3.2) \quad u_1^2 \rho_1 + p_1 = u_2^2 \rho_2 + p_2;$$

Energy:

$$(3.3) \quad \frac{u_1^2}{2} + h_1 = \frac{u_2^2}{2} + h_2.$$

Equations (3.1)–(3.3) are valid close to the shock front from the left side (1) and the right side (2). Within the shock front, that is, over some mean free paths only, friction also has to be taken into account. But we idealize the shock front as of zero thickness. Therefore the effect of friction is taken into account only indirectly; if eqs. (3.1)–(3.3) have a solution, the state 2 comes out to be different from 1.

3.1 Analytical Treatment of the Shock Conditions in Two-phase Flow

In this paper homogeneous two-phase mixtures shall be considered. Starting from eqs. (3.1)–(3.3), it is possible to write the initial conditions as

$$(3.4) \quad u_1 \frac{\rho_1''}{\rho_1' + x_1(\rho_1' - \rho_1'')} = u_2 \frac{\rho_2''}{\rho_2' + x_2(\rho_2' - \rho_2'')};$$

$$(3.5) \quad \begin{aligned} u_1^2 \frac{\rho_1''}{\rho_1' + x_1(\rho_1' - \rho_1'')} + p_1 \\ = u_2^2 \frac{\rho_2''}{\rho_2' + x_2(\rho_2' - \rho_2'')} + p_2; \end{aligned}$$

$$(3.6) \quad \begin{aligned} \frac{u_1^2}{2} + h_1' + x_1(h_1'' - h_1') \\ = \frac{u_2^2}{2} + h_2' + x_2(h_2'' - h_2'). \end{aligned}$$

Here ρ_1' and ρ_1'' are the densities of the saturated liquid and vapor before the front, respectively, ρ_2' and ρ_2'' those behind the front; x_1 and x_2 are the qualities of the mixture before and behind the front, respectively; corresponding definitions are valid for the enthalpies h_1' , h_1'' and h_2' , h_2'' , respectively.

We further assume thermal equilibrium of the two phases of the homogeneous mixture, but that implies that the Clausius-Clapeyron equation relates the pressure to the temperature or vice versa:

$$\left(\frac{dT}{dp}\right)_c = \frac{T}{h'' - h'} \left(\frac{1}{\rho''} - \frac{1}{\rho'}\right),$$

where ρ' , ρ'' , h' , and h'' are now functions of the pressure p only. A homogeneous two-phase flow, therefore, is completely defined by u , p , and x . Hence, the three eqs. (3.4)–(3.6) are available to determine the three unknown quantities u_2 , p_2 , and x_2 .

We look for conditions which give a nontrivial solution for u_2 , p_2 , and x_2 , apart from the trivial solution $u_2 = u_1$, $p_2 = p_1$, and $x_2 = x_1$. From eqs. (3.4) and (3.5) it follows that

$$(3.7) \quad x_2 = \frac{1}{\rho_1} \frac{\rho_2''}{\rho_2' - \rho_2''} - \frac{p_2 - p_1}{\rho_1^2 u_1^2} \frac{\rho_2''}{\rho_2' - \rho_2''} - \frac{\rho_2''}{\rho_2' - \rho_2''},$$

and with eq. (3.6)

$$(3.8) \quad \begin{aligned} \frac{(p_2 - p_1)^2}{2\rho_1^2 u_1^2} - \frac{p_2 - p_1}{\rho_1} \\ + \left[\left(\frac{1}{\rho_1} - \frac{p_2 - p_1}{\rho_1^2 u_1^2} \right) \rho_2' - 1 \right] \\ - \frac{\rho_2''}{\rho_2' - \rho_2''} (h_2'' - h_2') + h_2' - h_1 = 0. \end{aligned}$$

Equation (3.8) is a transcendental equation for the variable p_2 which depends on u_1 , p_1 , and x_1 . In addition to the trivial solution $p_2 = p_1$, whose existence is necessary in all cases, eq. (3.8) has a second root characterizing a shock, i.e., $p_2 = p_2(u_1, p_1, x_1) \neq p_1$. Thus, eq. (3.8) represents the exact shock condition for homogeneous two-phase mixtures, which is hardly very illuminating in this form. It is useful in a direct

form only for a numerical evaluation of the shock strength p_2/p_1 .

But it is not too difficult to arrive rather quickly at physically meaningful results in the cases

- a) of differentially small shock strengths considered leading to the velocity of sound;
- b) of shock strengths which are small enough to allow for a finite but linear approximation of p_2/p_1 .

3.2 The Velocity of Sound

In order to arrive at the velocity of sound, the following simplifications in eq. (3.8) must be made:

$$h_2'' - h_2' = h_1'' - h_1' = \Delta h_1;$$

$$c_{pl2} = c_{pl1}; \quad \rho_2' = \rho_1',$$

where c_{pl} is the specific heat of the liquid. Moreover, we have $\rho' \gg \rho''$.

These simplifications are the more valid the smaller is the shock strength.

For getting the velocity of sound only differentially small changes have to be considered. Therefore, one obtains from eq. (3.8),

$$(3.9) \quad \frac{(p_2 - p_1)^2}{2\rho_1^2 u_1^2} - \frac{p_2 - p_1}{\rho_1} + c_{pl1}(T_2 - T_1) + \left(\frac{x_1}{\rho_1} - \frac{p_2 - p_1}{\rho_1^2 u_1^2} \right) \rho_2'' \Delta h_1 - x_1 \Delta h_1 = 0.$$

The temperature difference $T_2 - T_1$ is expressed as a pressure difference by means of the Clausius-Clapeyron equation:

$$(3.10) \quad T_2 - T_1 = \left(\frac{dT}{dp} \right)_1 (p_2 - p_1).$$

One should realize that for getting the velocity of sound it is sufficient to have a Taylor expansion of first order only. For the case of weak but finite shocks, the Taylor development must be at least of second order.

For the vapor density ρ_2'' in eq. (3.9),

$$\rho_2'' = \rho_1'' + \delta \rho'',$$

with

$$\delta \rho'' = \left(\frac{\delta \rho''}{\delta p} \right)_x \delta p + \left(\frac{\delta \rho''}{\delta p} \right)_p \delta T.$$

With a modified gas equation $\rho'' = p/z'RT$; therefore, the following relation results:

$$\delta \rho'' = \rho_1'' \frac{p_2 - p_1}{p_1} \left(1 - \frac{p_1}{T_1} \frac{\delta T}{\delta p} \right).$$

Here $\delta T/\delta p$ is taken from the Clausius-Clapeyron

relation. This gives the relation

$$(3.11) \quad \rho_2'' = \frac{p_1}{z'RT_1} \left[1 + \frac{p_2 - p_1}{p_1} \left\{ 1 - \left(\frac{dT}{dp} \right)_1 \frac{p_1}{T_1} \right\} \right].$$

By means of eqs. (3.10) and (3.11) it follows from eq. (3.9) that

$$(3.12) \quad \frac{(p_2 - p_1)^2}{2\rho_1^2 u_1^2} - \frac{p_2 - p_1}{\rho_1} + c_{pl1} \left(\frac{dT}{dp} \right)_1 (p_2 - p_1) + \left\{ \left(\frac{x_1}{\rho_1} - \frac{p_2 - p_1}{\rho_1^2 u_1^2} \right) \frac{p_1}{z'RT_1} \left[1 + \frac{p_2 - p_1}{p_1} \cdot \left(1 - \left(\frac{dT}{dp} \right)_1 \frac{p_1}{T_1} \right) \right] - x_1 \right\} \Delta h_1 = 0.$$

After some transformations this gives

$$(3.13) \quad (p_2 - p_1)^2 \left[\frac{z'RT_1 - 2 \left\{ 1 - \left(\frac{dT}{dp} \right)_1 \frac{p_1}{T_1} \right\} \Delta h_1}{2\rho_1^2 u_1^2 z'RT_1} \right] - (p_2 - p_1) \left[\frac{1}{\rho_1} - c_{pl1} \left(\frac{dT}{dp} \right)_1 + \frac{p_1 \Delta h_1}{\rho_1^2 u_1^2 z'RT_1} - \frac{x_1 \Delta h_1}{p_1} \left\{ 1 - \left(\frac{dT}{dp} \right)_1 \frac{p_1}{T_1} \right\} \right] = 0.$$

Now it is possible to separate the trivial solution $p_2 = p_1$. If the difference between p_2 and p_1 becomes infinitely small for the remaining nontrivial root $(p_2 - p_1)$, this results in the velocity of sound a in homogeneous two-phase mixtures according to eq. (3.13) of

$$(3.14) \quad u_1^2 = a_1^2 = \frac{\frac{\rho_1''}{\rho_1^2} \Delta h_1 \left(\frac{dp}{dT} \right)_1}{c_{pl1} + x_1 \left[\frac{\Delta h_1}{p_1} \left(\frac{dp}{dT} \right)_1 - \frac{\Delta h_1}{T_1} - \frac{1}{x_1 \rho_1} \left(\frac{dp}{dT} \right)_1 \right]}.$$

The propagation of weak disturbances is nearly isentropic also in two-phase mixtures, so that the derivation of the velocity of sound may just as well be made from the general expression

$$(3.15) \quad a^2 = \left(\frac{\partial p}{\partial \rho} \right)_s = - \left(\frac{\partial p}{\partial v} \right)_s v^2,$$

where v is the specific volume. In order to evaluate this expression a reduction of eq. (3.15) to known functions in the two-phase region is necessary. From the total differentials of the entropy s and the specific volume v :

$$ds(p, x) = \left(\frac{\partial s}{\partial p} \right)_x dp + \left(\frac{\partial s}{\partial x} \right)_p dx;$$

$$dv(p, x) = \left(\frac{\partial v}{\partial p} \right)_x dp + \left(\frac{\partial v}{\partial x} \right)_p dx;$$

after brief calculation the differential quotient in question is the following:

$$(3.16) \quad \left(\frac{\partial p}{\partial v}\right)_s = \frac{\left(\frac{\partial s}{\partial x}\right)_p}{\left(\frac{\partial v}{\partial p}\right)_x \left(\frac{\partial s}{\partial x}\right)_p - \left(\frac{\partial v}{\partial x}\right)_p \left(\frac{\partial s}{\partial p}\right)_x}.$$

If the functions for two-phase mixtures

$$s(p, x) = s_l + x(s_v - s_l);$$

$$v(p, x) = v_l + x(v_v - v_l),$$

where the subscripts l and v denote liquid and vapor, are known, the differential quotients in eq. (3.16) are also known, as was also shown by Levy.⁽⁸⁾ Thus the velocity of sound in homogeneous two-phase mixtures can also be written as

$$(3.17) \quad a^2 = - \left(\frac{\partial p}{\partial v}\right)_s v^2 = - \frac{v^2}{(1-x) \frac{dv_l}{dp} + x \frac{dv_v}{dp} - \frac{v_v - v_l}{s_v - s_l} \left[(1-x) \frac{ds_l}{dp} + x \frac{ds_v}{dp} \right]}.$$

By using the entropy differential

$$ds = \frac{dh}{T} - v \frac{dp}{T}$$

and the conditions which resulted in eq. (3.14), it is possible to transform eq. (3.17) into eq. (3.14).

In eq. (3.17) it becomes apparent that in the plane of thermal states it is not possible to differentiate across the curves bounding the two-phase region but only along the boundaries $x = 0$ and $x = 1$, respectively, as these lines form discontinuities in the plane of thermal states. Thus, the velocity of sound in homogeneous two-phase mixtures and also in saturated and superheated liquids (i.e., $x \rightarrow 0$, but different from zero) is obtained.

The derivation of the velocity of sound was presented in considerable detail since it is of general importance for the understanding of shock conditions.

In the next section it will be shown that for velocities $u_l > a_l$ the existence of shocks is possible.

One should be aware of the fact that the velocity of sound obtained here implies differentially small condensations and vaporisations, $x_2 - x_1 \neq 0$. This requires an adequately quick heat transfer and, therefore, an adequately high degree of homogeneity. If this condition is not fulfilled it may happen that the process of condensation and vaporization cannot follow fast enough because of delays by heat transfer. The velocity

of sound in that case finds a limit in the case in which there are no phase changes ($x_2 = x_1$). For that case one can evaluate a different velocity of sound. This will be done in section 3.5.2.

3.3 Conditions for Weak but Finite Shocks

The exact shock equation (3.8) is transcendental, and this prevents the insight into the conditions and mechanisms of shock fronts in flowing two-phase mixtures. Therefore, it is first transformed:

$$(3.18) \quad \frac{(p_2 - p_1)^2}{2\rho_1^2 u_1^2} - \frac{p_2 - p_1}{\rho_1} + \left(\frac{x_1}{\rho_1''} - \frac{p_2 - p_1}{\rho_1^2 u_1^2} \right) \cdot \frac{\Delta h_1}{z'R} \frac{p_2}{T_2} + c_{pl} T_2 - c_{pl} T_1 - x_1 \Delta h_1 = 0,$$

where the modified gas equation was used for the steam density:

$$\rho_2'' = \frac{p_2}{z'R T_2}.$$

Further, $\rho_2' \gg \rho_2''$ as well as $\rho_2' \approx \rho_1'$ were taken into account.

In eq. (3.18) we now expand the temperature T_2 and the function $1/T_2$ into a Taylor series as a function of pressure around state 1:

$$(3.19) \quad T_2 = T_1 + \left(\frac{dT}{dp}\right)_1 (p_2 - p_1) + \frac{1}{2} \left(\frac{d^2 T}{dp^2}\right)_1 (p_2 - p_1)^2 + \dots;$$

$$(3.20) \quad \frac{1}{T_2} = \frac{1}{T_1} - \frac{1}{T_1^2} \left(\frac{dT}{dp}\right)_1 (p_2 - p_1) - \left[\frac{1}{2T_1^2} \left(\frac{d^2 T}{dp^2}\right)_1 - \frac{1}{T_1^3} \left(\frac{dT}{dp}\right)_1^2 \right] (p_2 - p_1)^2 + \dots$$

The series are truncated after the second order, one order more than necessary in the case of flow near sound conditions. From eqs. (3.18), (3.19), and (3.20) an algebraic equation of the fourth order in p_2/p_1 is formed:

$$(3.21) \quad (p_2 - p_1)^3 \left[\frac{\Delta h_1 z' R \rho_1''}{\rho_1^2 u_1^2} \left(\frac{d^2 T}{dp^2}\right)_1 - \frac{\Delta h_1 z' R \rho_1''^2}{\rho_1^2 u_1^2 T_1} \left(\frac{dT}{dp}\right)_1^2 \right] + (p_2 - p_1)^2 \cdot \frac{p_2}{p_1} \left[- \frac{x_1 \Delta h_1}{2T_1} \left(\frac{d^2 T}{dp^2}\right)_1 + \frac{x_1 \Delta h_1}{T_1^2} \left(\frac{dT}{dp}\right)_1^2 \right] + \frac{\Delta h_1 \rho_1''}{\rho_1^2 u_1^2 T_1} \left(\frac{dT}{dp}\right)_1 + (p_2 - p_1)^2 \cdot \left[\frac{1}{2\rho_1^2 u_1^2} + \frac{c_{pl}}{2} \left(\frac{d^2 T}{dp^2}\right)_1 \right]$$

$$\begin{aligned}
& + (p_2 - p_1) \frac{p_2}{p_1} \left[-\frac{x_1 \Delta h_1}{T_1} \left(\frac{dT'}{dp} \right)_1 - \frac{\rho_1'' \Delta h_1}{\rho_1^2 u_1^2} \right] \\
& + (p_2 - p_1) \left[-\frac{1}{\rho_1} + c_{pl} \left(\frac{dT'}{dp} \right)_1 + \frac{x_1 \Delta h_1}{p_1} \right] = 0,
\end{aligned}$$

or, in an abbreviated form (where A , B , C , D , and E replace the square brackets in eq. (3.21),

$$\begin{aligned}
(3.21a) \quad & (p_2 - p_1)^3 \frac{p_2}{p_1^2} A + (p_2 - p_1)^2 \frac{p_2}{p_1} B \\
& + (p_2 - p_1)^2 C + (p_2 - p_1) \\
& \cdot \frac{p_2}{p_1} D + (p_2 - p_1) E = 0.
\end{aligned}$$

After separation of the trivial solution $p_2 = p_1$ and multiplication by $1/p_1$ there is a cubic equation for the nontrivial second solution of the basic shock equation:

$$\begin{aligned}
(3.22) \quad & \left(\frac{p_2}{p_1} \right)^3 A + \left(\frac{p_2}{p_1} \right)^2 (-2A + B) \\
& + \frac{p_2}{p_1} \left(A - B + C - \frac{D}{p_1} \right) - C + \frac{E}{p_1} = 0.
\end{aligned}$$

In full detail,

$$\begin{aligned}
& \left(\frac{p_2}{p_1} \right)^3 \left[\frac{\Delta h_1 z' R \rho_1''^2}{2 \rho_1^2 u_1^2} \left(\frac{d^2 T'}{dp^2} \right)_1 - \frac{\Delta h_1 z' R \rho_1''^2}{\rho_1^2 u_1^2 T_1} \left(\frac{dT'}{dp} \right)_1 \right] \\
& + \left(\frac{p_2}{p_1} \right)^2 \left[-\frac{\Delta h_1 z' R \rho_1''^2}{\rho_1^2 u_1^2} \left(\frac{d^2 T'}{dp^2} \right)_1 + \frac{2 \Delta h_1 z' R \rho_1''^2}{\rho_1^2 u_1^2 T_1} \right. \\
& \cdot \left(\frac{dT'}{dp} \right)_1 - \frac{x_1 \Delta h_1}{2 T_1} \left(\frac{d^2 T'}{dp^2} \right)_1 + \frac{x_1 \Delta h_1}{T_1^2} \left(\frac{dT'}{dp} \right)_1 \\
& + \left. \frac{\Delta h_1 \rho_1''}{\rho_1^2 u_1^2 T_1} \left(\frac{dT'}{dp} \right)_1 \right] + \frac{p_2}{p_1} \left[\frac{\Delta h_1 z' R \rho_1''^2}{2 \rho_1^2 u_1^2} \left(\frac{d^2 T'}{dp^2} \right)_1 \right. \\
& - \frac{\Delta h_1 z' R \rho_1''^2}{\rho_1^2 u_1^2 T_1} \left(\frac{dT'}{dp} \right)_1 + \frac{x_1 \Delta h_1}{2 T_1} \left(\frac{d^2 T'}{dp^2} \right)_1 - \frac{x_1 \Delta h_1}{T_1^2} \\
& \cdot \left(\frac{dT'}{dp} \right)_1 - \frac{\Delta h_1 \rho_1''}{\rho_1^2 u_1^2 T_1} \left(\frac{dT'}{dp} \right)_1 + \frac{1}{2 \rho_1^2 u_1^2} + \frac{c_{pl}}{2} \left(\frac{d^2 T'}{dp^2} \right)_1 \\
& - \frac{x_1 \Delta h_1}{p_1 T_1} \left(\frac{dT'}{dp} \right)_1 - \frac{\Delta h_1 \rho_1''}{p_1 \rho_1^2 u_1^2} \left. \right] + \left[-\frac{1}{2 \rho_1^2 u_1^2} \right. \\
& - \frac{c_{pl}}{2} \left(\frac{d^2 T'}{dp^2} \right)_1 - \frac{1}{\rho_1 p_1} + \frac{c_{pl}}{p_1} \left(\frac{dT'}{dp} \right)_1 + \left. \frac{x_1 \Delta h_1}{p_1^2} \right] = 0.
\end{aligned}$$

The following points have to be observed in eq. (3.22)

1. If $p_2/p_1 \rightarrow 1$, this again results in a nontrivial solution of the velocity of sound in the form of eq. (3.14).
2. Truncating the Taylor expansion gives additional roots, which are without any physical meaning, however. Only that root which is contained in the original transcendental eq. (3.8) is of interest.

In order to obtain this root, p_2/p_1 is set up as a linear

function in eq. (3.22):

$$\frac{p_2}{p_1} = 1 + \alpha; \quad \left(\frac{p_2}{p_1} \right)^2 = 1 + 2\alpha; \quad \left(\frac{p_2}{p_1} \right)^3 = 1 + 3\alpha.$$

Using the abbreviations of eq. (3.21a) we obtain

$$(3.23) \quad \alpha = \frac{-\frac{D}{p_1} - \frac{E}{p_1}}{B + C + \frac{D}{p_1}}.$$

In spite of its approximate nature this solution gives the desired insight into the physics of the original shock conditions.

Now it is evident that the flow velocity u_1 (apart from the thermal quantities p_1 and x_1) plays an important role also in two-phase flow when conditions for shock fronts are concerned. The characteristic quantity of every consideration of shock fronts in the sense of general gas dynamics is the Mach number $M = u/a$.

After multiplication of numerator and denominator in eq. (3.23) by $p_1 (dp/dT)_1$ and by using the relations

$$\left(\frac{dT'}{dp} \right)_1 = \frac{T_1}{\Delta h_1 \rho_1''}$$

and

$$\frac{p_1}{\rho_1''} = z' R T_1,$$

we obtain the desired shock condition:

$$\begin{aligned}
(3.24) \quad \alpha = & \frac{\frac{\rho_1'' \Delta h_1}{\rho_1^2 a_1^2} \left(\frac{dp}{dT} \right)_1 \left(1 - \frac{1}{M_1^2} \right)}{\frac{1}{M_1^2} \frac{\rho_1''}{\rho_1^2 a_1^2} \left(h_1 - \frac{3}{2} \frac{p_1}{\rho_1''} \right) \left(\frac{dp}{dT} \right)_1 + \frac{c_{pl}}{2 \Delta h_1}} \\
& \cdot \left(\Delta h_1 - 2 \frac{p_1}{\rho_1''} \right) - \frac{x_1}{2 \rho_1''} \left(\frac{dp}{dT} \right)_1 + \frac{x_1 \Delta h_1}{T_1}
\end{aligned}$$

Equation (3.24) shows that in addition to the trivial solution $p_2 = p_1$ there is indeed a second solution if $M_1 \neq 1$. In the following section it is shown that the entropy criterion excludes dilution shocks also in two-phase flow. However, this means that only in cases of $M_1 > 1$ discontinuities, i.e., shocks, may be expected.

Now it is important that in the homogeneous two-phase flow of low quality this limiting value may be reached and exceeded very easily. In section 3.6 this is discussed in great detail both phenomenologically and numerically.

The conditions for shock fronts in flowing homogeneous two-phase mixtures and for $x_1 \rightarrow 0$ (but different from zero, saturated fluids) and $x_1 \rightarrow 1$ (but different from one, vapors) are, therefore, as follows:

$$\begin{aligned}
(3.25) \quad & \frac{p_2}{p_1} = 1 + \alpha = 1 \\
& + \frac{\frac{\rho_2'' \Delta h_1}{\rho_1^2 a_1^2} \left(\frac{dp}{dT} \right)_1 \left(1 - \frac{1}{M_1^2} \right)}{\frac{1}{M_1^2} \frac{\rho_1''}{\rho_1^2 a_1^2} \left(\Delta h_1 - \frac{3p_1}{2\rho_1''} \right) \left(\frac{dp}{dT} \right)_1 + \frac{c_{p1}}{2\Delta h_1}}; \\
(3.26) \quad & \frac{x_2}{x_1} = 1 + \alpha \left[1 - \frac{p_1}{T_1} \left(\frac{dT}{dp} \right)_1 - \frac{\rho_1'' p_1}{\rho_1^2 a_1^2} \frac{1}{M_1^2} \frac{1}{x_1} \right]; \\
(3.27) \quad & \frac{u_2}{u_1} = 1 - \frac{p_1}{\rho_1 u_1^2} \left(\frac{p_2}{p_1} - 1 \right) = 1 - \frac{p_1}{\rho_1^2 a_1^2} \frac{1}{M_1^2} \alpha.
\end{aligned}$$

Equations (3.25)–(3.27) are the complete shock conditions in a linear approximation.

The conditions for density and enthalpy, ρ_2/ρ_1 and h_2/h_1 , are functions of

$$\frac{p_2}{p_1}, \quad \frac{u_2}{u_1}, \quad \text{and} \quad \frac{x_2}{x_1}.$$

For the density ratio before and after the front one obtains from eqs. (3.1) and (3.27),

$$(3.28) \quad \frac{\rho_2}{\rho_1} = \frac{1}{1 - \frac{p_1}{\rho_1 a_1^2} \frac{1}{M_1^2} \alpha},$$

and for the enthalpy condition

$$(3.29) \quad \frac{h_2}{h_1} = 1 + \frac{p_1}{h_1 \rho_1} \alpha.$$

Consistent with the linear approximation for p_2/p_1 higher powers of p_2/p_1 appearing in eqs. (3.28) and (3.29) are not taken into account.

3.4 The Entropy Criterion for Shocks

The criterion for the existence of shocks is the increase of entropy in the shock.

For all homogeneous media and also for homogeneous two-phase mixtures, the following relation holds:⁽⁹⁾

$$(3.30) \quad T_1(s_2 - s_1) = \frac{1}{12} \left(\frac{\partial^2}{\partial p^2} \frac{1}{\rho} \right)_{s,1} (p_2 - p_1)^3 + \dots$$

This change of entropy was derived for states close to state 1. With a slight pressure increase, the entropy grows with the third power of $p_2 - p_1$ if the second derivative of $1/\rho$ with respect to p is positive at constant entropy.

A more elaborate consideration shows that the second derivative is positive, not only for gases or liquids, but also for two-phase mixtures. Therefore, also in the case considered here, only shock fronts are possible which are associated with pressure increases.

However, this implies that the shock conditions eqs. (3.25)–(3.27) have a physical meaning only for flow rates $M_1 > 1$; that means there are no dilution shocks in the case of two-phase flow either.

3.5 Changes of Phase in Shocks

Equation (3.26) shows directly that, depending upon the quantities of state u_1 , p_1 , and x_1 , the quality x_2 after the shock is larger, equal, or smaller than x_1 . According to eq. (3.29), however, the enthalpy after the shock is always higher than before the shock:

$$(3.31) \quad h_2 = h_1 + \frac{p_1}{\rho_1} \alpha.$$

Associated with the increase with enthalpy is an increase of pressure and temperature. The Clausius-Clapeyron equation now shows that pressure increase favors condensation and temperature increase favors vaporization. Therefore, pressure and temperature increase, both dictated by the increase of enthalpy, are competing influences on the question of phase change in the shock front. If the quality is low (x_1 small), the specific heat of the mixture is high; the temperature increase for a given increase of enthalpy is then low and leads therefore to condensation.

If on the other hand the quality is high (x_1 large), the specific heat of the mixture is low; the temperature increase for the given increase of enthalpy prevails and leads to vaporization. For certain conditions pressure increase and temperature increase counterbalance each other and no change of phase takes place.

Therefore, all three cases: condensation, no change of phase, and vaporization, can be associated with shocks.

This turns out to be a main feature of the dynamics of two-phase flow.

3.5.1 The Limit between Shocks with Condensation and Shocks with Vaporization. Again it is useful to apply the linear approximation. From eq. (3.26) for $x_1 = x_2$ it follows that

$$(3.32) \quad \alpha \left[1 - \frac{p_1}{T_1} \left(\frac{dT}{dp} \right)_1 - \frac{\rho_1'' p_1}{\rho_1^2 a_1^2} \frac{1}{M_1^2} \frac{1}{x_1} \right] = 0.$$

For $\alpha \neq 0$ ($M_1 > 1$) this equation requires

$$(3.33) \quad 1 - \frac{p_1}{T_1} \left(\frac{dT}{dp} \right)_1 - \frac{\rho_1'' p_1}{\rho_1^2 a_1^2} \frac{1}{M_1^2} \frac{1}{x_1} = 0.$$

Using (3.14), one obtains:

$$\begin{aligned}
(3.34) \quad x_1 &= \frac{\left[\frac{1}{\rho_1} - c_{p1} \left(\frac{dT}{dp} \right)_1 \right] \frac{p_1}{M_1^2 \Delta h_1}}{\left[\frac{1}{M_1^2} - 1 \right] \left[1 - \frac{p_1}{T_1} \left(\frac{dT}{dp} \right)_1 \right] - \frac{p_1}{M_1^2 \Delta h_1} \frac{1}{\rho_1''}} \\
&= x_1(p_1, M_1).
\end{aligned}$$

Shock fronts in two-phase mixtures of this special composition result in a higher level of pressure and enthalpy, but not in a change of quality. Equation (3.34) is subject to the following limitations:

- The relation $\rho' \gg \rho''$ must hold; the vicinity of the critical point has to be avoided.
- $M_1 > 1$ must be in the range of the linear approximation.

However, this equation holds exactly for a flow near sound conditions, i.e., $M_1 \rightarrow 1$. There it is possible to define a limiting curve depending only on p_1 .

For $M_1 \rightarrow 1$ it follows from eq. (3.34) that

$$(3.35) \quad x_1 = c_{pl} \rho_1'' \left(\frac{dT}{dp} \right)_1 - \frac{\rho_1''}{\rho_1} = x_1(p_1).$$

Equation (3.35) therefore is the limit between shocks with condensation and those with vaporization in the flow near sound conditions. It can be interpreted as a two-phase flow without heat transfer between the two involved phases.

3.5.2 The Velocity of Sound in Case of Phase Constancy. Now it will be assumed that there is no heat transfer between the two involved phases of a two-phase flow. This implies that no longer a Clausius-Clapeyron coupling of temperature and pressure exists.

We now derive the velocity of sound for that case. Assuming $x_2 = x_1$ in the three basic equations (3.4)–(3.6), we may determine the quantities u_2 , p_2 , and T_2 without referring to the above-mentioned direct coupling between p and T .

After separation of the trivial root $p_2 = p_1$, it follows that

$$(3.36) \quad \frac{p_2}{p_1} = \frac{\frac{1}{2\rho_1^2 u_1^2} + \frac{1}{\rho_1 p_1} - \frac{c_{pl}}{z'R\rho_1'' p_1}}{\frac{1}{2\rho_1^2 u_1^2} - \frac{c_{pl}}{x_1 z'R\rho_1'' u_1^2}}.$$

In this case the quantities Δh , c_{pl} , and ρ' are again treated as constants. Again allowing $p_2/p_1 \rightarrow 1$ now gives the velocity of sound in the two-phase mixtures; we have used a compressibility mechanism only and not considered condensation and vaporization:

$$(3.37) \quad u_1^2 = a_1^2 = \frac{1}{\frac{x_1 \rho_1}{p_1} \left(\frac{\rho_1}{\rho_1''} - \frac{z'R}{c_{pl}} \right)}.$$

According to eq. (3.37), which has been evaluated numerically in Fig. 1, the velocity of sound of the two-phase mixtures at phase constancy converges rapidly but steadily to the corresponding velocities of sound of the one-phase conditions for $x_1 \rightarrow 0$ (including zero) as well as $x_1 \rightarrow 1$ (including one).

Formally, according to eq. (3.37), the velocity of sound tends towards infinity for $x_1 \rightarrow 0$. In that case,

FIG. 1. Velocity of Sound in a Sodium Two-phase Mixture at 1000°C as Function of Quality.

of course, the equation of state of the liquid has to be introduced into the basic equations (3.4)–(3.6).

3.6 Shock Probability in the Two-phase Flow

According to section 3.3, shock fronts are possible in two-phase flow by exceeding the critical velocity, i.e., for $u_1 > a_1$ or $M_1 > 1$. The critical velocity is represented by the velocity of sound, which is not only dependent on the thermal state, but also on the degree of homogeneity of the mixtures.

There is a lower and an upper limit to the velocity of sound in two-phase mixtures as function of the homogeneity. The lower limit implies differentially small condensations and vaporizations in the sound wave and is given by eq. (3.14). These phase changes require an adequately quick heat transfer and therefore an adequately high degree of homogeneity. The upper limit is reached when there are no phase changes in the sound wave and the process of sound propagation is only determined by the compressibility of the mixture. In this case the velocity of sound is given by eq. (3.37).

However, in two-phase mixtures of the special composition according to eq. (3.35), no changes of phase are possible in a sound wave. Therefore in mixtures of such a kind only one velocity of sound is possible,

FIG. 2. Velocity of Sound in a Sodium Two-phase Mixture at 1000°C as Function of Void Fraction.

i.e., the above-defined lower and upper limits must agree exactly.

Semenov and Kosterin⁽¹⁰⁾ paid no attention in their work to this fact.

In Figures 1 and 2, respectively, these limits of the critical velocity of two-phase mixtures, given by eq. (3.14) and eq. (3.37), are plotted for sodium versus quality and void fraction, respectively.

Most important for the reactivity ramp rate due to voiding the coolant channels are low qualities, because for sodium at a quality of 1% the vapor volume fraction already reaches nearly 91%. However, in homogeneous mixtures of this low quality, the lower limit of the critical velocity may be reached very easily (see Fig. 1), whereas the upper limit tends very rapidly towards the high value of the liquid.

Associated with the large difference between the two possible critical velocities at low qualities is the effect of dispersion, i.e., the dependence of the propagation of sound waves in two-phase mixtures on the wave frequency.

Let τ be a characteristic time for heat transfer:

$$\tau = \frac{\rho c r^2}{\lambda},$$

where ρc is the heat capacity per cm^3 , r is a characteristic length describing the degree of homogeneity, and λ is the thermal conductivity. Then the quantity τf (where f is the frequency of the sound wave) governs the dispersion. If

$$\tau f \gg 1,$$

then the mixture appears to be homogeneous, corresponding to the low velocity of sound [see eq. (3.14)]. If

$$\tau f \ll 1,$$

then the mixture does not appear to be homogeneous and the high velocity of sound applies [see eq. (3.37)].

The dispersion phenomenon increases the complexity of the process of coolant ejection.

4. Shock Fronts in the Case of Superheated Fluids

The consideration of superheat in connection with shock fronts only makes sense if $x_1 \rightarrow 0$, because $x_1 \neq 0$ would immediately lead to desuperheating. But this automatically implies that this consideration is important because in this region ($x_1 \rightarrow 0$) the velocity of sound is low and the shock probability is high (see section 3.6). Therefore, this case shall be considered now, always putting $x_1 = 0$.

4.1 Desuperheating with a Simultaneous Condensation Shock

The superheat energy is now introduced in the energy equation (3.6):

$$(4.1) \quad \frac{u_1^2}{2} + c_{p1} T_{lb} + c_{p1} \Delta T' = \frac{u_2^2}{2} + h_2' + x_2(h_2'' - h_2'),$$

where T_{lb} is the boiling temperature and $\Delta T'$ is the fluid superheat.

With eq. (4.1) and the functions and abbreviations of sections 3.1 and 3.3, the following polynomial for the pressure ratio p_2/p_1 is obtained:

$$(4.2) \quad (p_2 - p_1)^3 \frac{p_2}{p_1^4} A + (p_2 - p_1)^2 \frac{p_2}{p_1^3} B + (p_2 - p_1)^2 \frac{1}{p_1^2} C + (p_2 - p_1) \frac{p_2}{p_1^2} \frac{D}{p_1} + (p_2 - p_1) \frac{1}{p_1} \frac{E}{p_1} = \frac{c_{p1} \Delta T'}{p_1^2}.$$

For eq. (4.2) again the temperature T_2 was expanded as a function of pressure (see section 3.3).

A trivial solution $p_2 = p_1$ no longer exists in the case of superheating according to eq. (4.2). There are always two solutions which now are nontrivial:

The first root is the so-called quasitrivial solution $(p_2/p_1)_{qt}$. It describes desuperheating without a

condensation shock. The second root characterises desuperheating with a simultaneous condensation shock.

In order to obtain the second root of eq. (4.2), the quasitrivial root $(p_2/p_1)_{qt}$ also has to be separated. This is done by replacing in eq. (4.2) the sum of coefficients

$$C - \frac{E}{p_1} - \frac{c_{pi}\Delta T}{p_1^2}$$

by the quasitrivial solution:

$$\begin{aligned} C - \frac{E}{p_1} - \frac{c_{pi}\Delta T}{p_1^2} = & -\left(\frac{p_2}{p_1}\right)_{qt}^4 A - \left(\frac{p_2}{p_1}\right)_{qt}^3 (-3A + B) \\ & - \left(\frac{p_2}{p_1}\right)_{qt}^2 \left(3A - 2B + C + \frac{D}{p_1}\right) \\ & - \left(\frac{p_2}{p_1}\right)_{qt} \left(-A + B - 2C - \frac{D}{p_1} + \frac{E}{p_1}\right). \end{aligned}$$

In this way it is possible to form differences of the type

$$\left(\frac{p_2}{p_1}\right)^n - \left(\frac{p_2}{p_1}\right)_{qt}^n,$$

which permits separation. After some calculations the result is

$$\begin{aligned} & \left[\left(\frac{p_2}{p_1}\right) - \left(\frac{p_2}{p_1}\right)_{qt} \right] \left\{ \left(\frac{p_2}{p_1}\right)^3 A + \left(\frac{p_2}{p_1}\right)^2 \right. \\ & \quad \cdot \left[\left(\frac{p_2}{p_1}\right)_{qt} A - 3A + B \right] \\ & \quad + \frac{p_2}{p_1} \left[\left(\frac{p_2}{p_1}\right)_{qt}^2 A + \left(\frac{p_2}{p_1}\right)_{qt} \right. \\ (4.3) \quad & \quad \cdot (-3A + B) + 3A - 2B + C + \frac{D}{p_1}] \\ & \quad + \left[\left(\frac{p_2}{p_1}\right)_{qt}^3 A + \left(\frac{p_2}{p_1}\right)_{qt} \right. \\ & \quad \cdot \left(3A - 2B + C + \frac{D}{p_1} \right) \\ & \quad \left. \left. + \left(-A + B - 2C - \frac{D}{p_1} + \frac{E}{p_1} \right) \right] \right\} = 0. \end{aligned}$$

This separates the first root, desuperheating without shock:

$$\frac{p_2}{p_1} = \left(\frac{p_2}{p_1}\right)_{qt}.$$

What remains is to find the second root of eq. (4.3).

$$(4.4) \quad \alpha^* = \frac{A \left[\left(\frac{p_2}{p_1}\right)_{qt}^3 - 2 \left(\frac{p_2}{p_1}\right)_{qt}^2 + \left(\frac{p_2}{p_1}\right)_{qt} \right] + B \left[\left(\frac{p_2}{p_1}\right)_{qt}^2 - \left(\frac{p_2}{p_1}\right)_{qt} \right] + C \left[\left(\frac{p_2}{p_1}\right)_{qt} - 1 \right] + \left(\frac{p_2}{p_1}\right)_{qt} \frac{D}{p_1} + \frac{E}{p_1}}{- \left\{ A \left[\left(\frac{p_2}{p_1}\right)_{qt}^2 - \left(\frac{p_2}{p_1}\right)_{qt} \right] + \left(\frac{p_2}{p_1}\right)_{qt} B + C + \frac{D}{p_1} \right\}}.$$

If there is no superheat, i.e., if $(p_2/p_1)_{qt} = 1$, eq. (4.3) converges into eq. (3.23).

In eq. (4.3) p_2/p_1 is again set up as a linear function:

$$\frac{p_2}{p_1} = 1 + \alpha^*; \quad \left(\frac{p_2}{p_1}\right)^2 = 1 + 2\alpha^*; \quad \left(\frac{p_2}{p_1}\right)^3 = 1 + 3\alpha^*.$$

For desuperheating with a simultaneous condensation shock, it then follows for the intensity of the shock in a linear approximation and with the abbreviations used in section 3.3 that [see eq. (4.4) at bottom of page].

According to the entropy criterion, shock fronts are again always connected with an increase in pressure. Hence the possibility of condensation shocks with a simultaneous desuperheating in superheated fluids is directly described by eq. (4.4). It will be always a condensation shock.

The condensation in the shock front is weakened by vaporization due to desuperheating. Therefore, shock fronts with simultaneous desuperheating according to eq. (4.4) are restricted to regions with superheated fluids in which the effects of condensation and vaporization balance out partially or totally.

The condensation effect is determined by the flow velocity, the associated vaporization effect by the degree of superheat.

We now consider a second kind of critical velocity leading to $\alpha^* = 0$; that means condensation (due to the shock) and vaporization (due to desuperheating) compensate totally. This critical velocity α^* is a function of the degree of superheat and the Mach number (involving the velocity of sound with phase changes).

Desuperheating with a simultaneous condensation shock can happen only for flow velocities larger than α^* , because only then condensation prevails.

In Fig. 3 this second critical velocity α^* is plotted for sodium versus the degree of superheat.

4.2 Desuperheating without a Simultaneous Condensation Shock

The first root of eq. (4.2) is the so-called quasitrivial solution $(p_2/p_1)_{qt}$. It is generated in the case of superheating evolving from the original trivial solution $p_2 = p_1$. It describes the desuperheating without a condensation shock.

The quasitrivial solution is now given as a linear expansion with respect to the superheating term of eq. (4.2):

FIG. 3. Critical Velocity in Superheated Sodium at a Pressure of 2.71 atm (i.e., a Boiling Temperature of 1000°C).

$$(4.5) \quad \left(\frac{p_2}{p_1}\right)_{qt} = 1 + \frac{c_{ph}\Delta T}{\frac{\Delta h_1 \rho_1''}{\rho_1^2 a_1^2} \left(1 - \frac{1}{M_1^2}\right)}.$$

Also, this shock condition is physically meaningful for $M_1 > 1$ only. $(p_2/p_1)_{qt} - 1$ is inversely proportional to $1 - (1/M_1^2)$, a term which appeared up to now only as a numerator [see eq. (3.24)]. It refers to the velocity of sound with phase changes [see eq. (3.14)]. This critical velocity is the proper measure for the flow velocity also in this case. If $M_1 \rightarrow 1$ the quantity $(p_2/p_1)_{qt} - 1$ tends towards infinity. The reason is that the pressure increase associated with the process of desuperheating cannot dissipate, because such dissipation would have to use the velocity of sound and the flow of the fluid just counteracts such a dissipation.

At flows for which $M_1 > 1$, the energy of the front is indeed dissipated over larger flow areas in the direction of flow, which results in a removal of the otherwise infinitely high pressure at the original place of the shock.

In Fig. 4 the two possible shock fronts are plotted for superheated sodium:

1. desuperheating without a condensation shock and according to section 4.1;
2. desuperheating with a simultaneous condensation shock.

Figure 3 also evaluates the limits for desuperheating with and without simultaneous shocks.

It is useful to apply the following arguments:

Again the mechanism of state 1 involved here is that $x_1 \rightarrow 0$ but is different from zero. This can be seen from the nature of a [see eq. (3.14)], which appears in the definition of M in eq. (4.5). It is implied also by the fact that, according to the shock conditions, the increase of pressure is accompanied by an increase of density as it is in the front phenomena without superheat. This is true in spite of the occurrence of boiling.

But one does expect a dilution if boiling occurs. This cannot take place in form of a shock phenomenon; it spreads out over a finite interval and, therefore, becomes a boiling wave and not a boiling front. The experiments of Friz⁽⁵⁾ also seem to indicate this.

5. Conclusions

The state of liquid is described by $x_1 = 0$; if boiling starts it therefore evolves from $x_1 = 0$. According to the results of this paper one has to face a somewhat complex situation. On the one hand, there exists the dispersion phenomenon. But if the quality x increases somewhat,

FIG. 4. The Two Possible Shock Fronts in the Case of Superheated Sodium at a Pressure of 2.71 atm (i.e., a Boiling Temperature of 1000°C): A. Desuperheating with a Simultaneous Condensation Shock: p_2/p_1 ; B. Desuperheating without a Simultaneous Condensation Shock, i.e., the Quasitrivial Solution $(p_2/p_1)_{qt}$.

the two critical velocities which are possible come close to each other again. Then in the general case there is practically only one value for the velocity of sound, i.e., the critical velocity. The possibility for shock waves, on the other hand, increases as the velocity of sound decreases. Therefore, in general the low velocities of sound obtained in this study indicate that there is a rather large probability for shocks.

The occurrence of shocks in turn reduces the flow velocity to subcritical values again. Therefore, the velocity of sound is a rather general indication for the voiding of a coolant channel.

A typical situation could be the following:

Saturated sodium at 1000°C (2.71 atm)

quality, x_1	0.0001	0.001	0.01
void fraction, β_1	0.087	0.49	0.91
lower limit of the critical velocity, a_1 (m/sec)	3.5	6.1	28.8
upper limit of the critical velocity, a_2 (m/sec)	69.0	39.0	66.0

We conclude that in the sense of the arguments given above and with regard to the already large void fraction at still very small qualities, an overall effective expulsion velocity could be of the order of at least 4 m/sec and at most 60 m/sec.

Together with the more recent value of a maximum sodium void effect of the Karlsruhe 1000-MWe study of \$7, this leads to ramp rates of at least \$56/sec to several hundred \$/sec, with a minimum expulsion time of 125 msec.

The ramp rate due to voiding the coolant channels is, therefore, indeed an open problem. It must be realized that with the present study only the lower and upper limits of the ramp rate were investigated. A detailed study of the ejection taking into account all kinds of phenomena described here will follow as mentioned earlier.

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Discussion

Mr. MacFarlane (ANL): What type of flow do you have? Is it a homogeneous model? Secondly, if this is an expediency for calculation, what effect would a slip type of model have on the results you have calculated?

Mr. Fischer: I think in the expulsion process the slip ratio is not essential. Therefore, we are limited only by the upper and lower limits of the two derivatives; this implies that the homogeneity of the mixture is only essential for the critical velocity.

Mr. Fauske (ANL): As far as the upper and lower limits of the critical sonic velocity are concerned, measurements have been made recently of a critical flow in air-water systems compared to steam-water systems which give an excellent comparison between metastability and equilibrium. From these results the metastable sonic velocity apparently is the one controlling. This work was also presented at the recent Oxford meeting on two-phase flow.

Mr. Fischer: Yes. This work was done by the Russians.

Mr. Häfele (Karlsruhe): In the Russian work it is indicated that the velocity of sound follows the scheme in which no change of phases appear. However, one should be careful because it depends on the degree of homogeneity. The characteristic time is defined as $\rho c r^2 / \lambda$ as in the paper just given. For a given heterogeneity one can dream of sufficiently low frequencies that heat transfer is possible, and one can think of frequencies which are high enough to avoid heat transfer. Therefore, it should always be made clear what frequencies are being considered. This dispersion phenomenon really adds to the complexity.

Mr. Cohen (GE): What pressures were involved in your calculations?

Mr. Häfele: In our investigations the amount of pressure built up was not yet explicit. It will be investigated in a second step.

Mr. Fell (Winfrith): If I regarded this as a problem

of transmission of sound through a mixture of materials with two sound velocities, I'd recognize there were two problems: the question of the differing velocity of sound in the two media, and the question of impedance matching between the two media. I'd expect to find, in fact, that you just can't transmit a sound through this material, just as, for example, a bottle of glass beads can't be seen through. Why is it that you can ignore the actual physical heterogeneity and the matching between the phases when you do a model of this kind? You certainly can't if you tried to transmit light through the mixture of material of different dielectric constants.

Mr. Fischer: I would say if you take into account the inhomogeneity, the upper limit is defined by the inhomogeneity.

Mr. Fell: Surely in that case you would expect to see an attenuation effect. If you have a mixture of materials, the two associated velocities have different impedances. You get strong attenuation in a medium of this kind. I just wonder how valid calculations of this kind are, which say that there is a different velocity, and you have a statement of the velocity as a function of frequency, but you have no statement of attenuation. I thought that the actual physical heterogeneity itself did produce a strong attenuation of sounds, of velocities of this kind.

Mr. Fischer: The degree of homogeneity of the mixture is described by the factor τf , where τ is the characteristic time for heat transfer and f is the frequency of the sound wave. Therefore, we take both the inhomogeneity and the frequency into account.

Mr. Zivi (TRW): Some years ago, at what was then Armour Research Foundation, Karplus developed a theory for the propagation of sound through two-phase mixtures and shock fronts. Can you tell me how your theory differs from his? Are you familiar with his work?

Mr. Fischer: No.

Mr. Lottes: The report which was published as an Armour Research Bulletin, I believe, wasn't widely publicized. If I might, I would like to add one comment about Karplus' work that amplifies some of the things that have been said here. His initial experiments were with gas-water systems and the gas bubbles were of very small diameters, of the order of a fraction of a millimeter. He calculated a bulk modulus of the mixture in two ways: Case A was for an isothermal process, and Case B for an adiabatic one. The results of the two calculations differed by 20 %. He couldn't force his data to fit the adiabatic relation; his case was isothermal. This raised the question of what do you do in a steam-water mixture—should you allow heat transfer, condensation, vaporization to take place? He proposed certain experiments along this line but lost support for his project. We never saw any final results.

Mr. Zivi: He did make some measurements and in fact has data on the propagation of very strong shocks through two-phase mixtures. Shocks that are so strong that the pressure completely collapses the voids. I know he did get extremely low acoustic velocities, much lower for the steam-water than for the air-water mixtures.

Mr. Wolfe (GE): I'd like to ask a question of the previous speaker. Is it possible to get pressures in a channel which are higher than the equivalent saturation pressure when you have superheated liquid? If so, what is the physical cause of these higher pressures?

Mr. Zivi: I think the answer is it's possible. The physical mechanism is the water-hammer type of impact. You have a relatively long time to put impulse into a slug of liquid. It can hit a rigid boundary or other liquid, and the impulse is converted into pressure in a short time.

Mr. Fauske: The question was raised earlier in regard to what effect slip might have on the sonic velocity. If you look, not at bubble flow, but at droplet flow, just the complete opposite, in the gas phase, the continuous phase, it turns out you have to take into account slip in order to explain the sonic velocities. Furthermore, in the limiting case the liquid droplet is not seen at all by the sound wave. In other words, you have a constant void fraction there as the sound is propagated through the two-phase system. In calculation you find that the sonic velocity is independent of quality in a high-quality range. Data have been taken in this region that satisfy the calculations.

Mr. Fischer: No, I disagree. The sound velocity in the case of constant phase is dependent upon the compressibility, and the compressibility is a function of the void fraction or quality.

Mr. Fauske: I'm talking about droplet flow primarily at low pressure, where the void fraction is practically 100 %.

Mr. Fischer: But there is a large dependence upon the quality.

Mr. Fauske: Not in this region.

Mr. Fischer: In that region, of course. With a quality of 1 % the void fraction is 90 %. If so, and you saw the first limiting curves as function of quality, there is a large dependence upon the quality.

Mr. Fauske: I'm talking about a different quality region now. I'm talking about 50, 60, or 70 % quality, which you don't have on the plot at all. If you assume thermodynamic equilibrium or no slip at all, you cannot explain the sonic velocity obtained because it does appear as a function of quality and this will show up experimentally.

Mr. Häfele: I think one should be aware of the fact that after the reflection of a shock wave or after inter-

action of shock waves together, pressure can increase tremendously. This can provide a mechanism to increase pressure besides what can happen in a regular flow. Just because the velocity of sound is in many cases so very small, the probability of shock waves has increased, and by the same token there is an increase of the probability of a pressure increase. Thus the picture becomes even more complex. Mr. Fischer and I were very astonished to see that after a shock front passes, both vaporization and condensation can take place. The more you look at the problem the more complex it becomes.

Mr. Bump (ANL): In the study of coolant ejection by Friz, was there any vertical temperature gradient in the water? In other words, the hotter material would tend to go to the top and therefore superheat would be greater at the top.

Mr. Friz (EURATOM, Ispra): The temperature difference was less than 0.3°C because we could control

the temperature in the interior and in the thermostat itself. The only reason that acceleration starts at the exit is the big fall in pressure related to the superheating temperature. On the other hand, it is impossible or nearly impossible that the acceleration start at the bottom, because the whole column must then be accelerated, which takes a much greater dynamic pressure gradient than when it starts at the exit.

Mr. Fischer (ANL): What I'm concerned about is the kinetics which initiate a fast reactor accident. The point I'm concerned about is as we go towards pancakes or other types of large fast reactors, we get very different power gradients axially. Now suppose you have, for example, a pancake reactor. What does a very flat axial gradient do to the kinetics of sodium ejection?

Mr. Hafele: We have been working many months to get the answer and don't have it yet.

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