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CALCULATIONS OF COHERENCE OF FAILURE
FOR HYPOTHETICAL MELTDOWN ACCIDENTS
IN AN EBR-II-LIKE REACTOR

by

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I. INTRODUCTION

A. General

In the event of an accident in a fast reactor leading to fuel element failure, the time distribution of meltdown of different fuel elements could play a significant role in the ultimate severity of the accident. Though one could conceive of situations in which the fuel element meltdown leads to reduction in reactivity, it is possible that under certain circumstances fuel element failure is accompanied by the addition of reactivity. In such cases, if many of the fuel elements were to fail simultaneously, or within a short interval of time, the rate of reactivity insertion could be large enough to lead to extremely severe accidents. Hence, in safety analyses for fast reactors it is of interest to study the coherence of fuel element failure and its effect on the further accentuation of the accident. As a step towards such a study, this report presents an estimate of the time distribution of failure of fuel elements in different positions of an EBR-II-like core under different hypothetical accident conditions and its possible effects on the rate of reactivity addition.

B. Scope of Analysis

The basic parameters in reactor design that determine the temperature rise in a fuel pin are: (1) power generated in the fuel; (2) coolant flow rate; and (3) coolant cross-section area. It was decided to study the temperature transients of EBR-II fuel pins having different values of the above parameters.

The EBR-II reference core⁽¹⁾ consists of a cluster of fuel "sub-assemblies." These are grouped into different "rows" depending upon their location with respect to the center of the core. Sodium coolant enters a subassembly at the bottom through an "orifice." In order to minimize the difference in the outlet temperature of the coolant from different subassemblies, the coolant flow rate is varied from row to row (constant for all subassemblies within a row) by adjusting the coolant-entrance orifice.

Each subassembly contains a number of fuel pins. Within such a subassembly the fuel pins can be classified as (1) internal pins and

(2) peripheral pins. Internal pins have effective coolant cross-section areas appreciably smaller than those of the peripheral pins.

Detailed spatial and time distributions of temperature in internal and peripheral pins of different rows have been calculated for the following accidents:

- (1) coolant flow decay due to pump failure;
- (2) a. linear reduction of coolant flow due to blocked orifice;
b. "instantaneous" reduction in coolant flow due to blocked orifice;
- (3) reactor power excursion due to rapid insertion of excess reactivity.

A study of the times at which the threshold for fuel pin failure are attained has been done for these accidents. The conditions were chosen to range from a relatively incoherent failure case (Case 1, in which the flow decays slowly due to coolant inertia with a time constant of the order of 10 sec) to a particularly unrealistic, but fast failure case (Case 3, in which the reactor goes super-prompt-critical). In all calculations, it was assumed that scram did not occur.

Cases 1 and 2 were studied both with and without feedback (i.e., constant design EBR-II power). Case 3 was run with simplified feedback corresponding to fuel expansion resulting from adiabatic heating, and with a more detailed description of feedback. In all cases in which feedback was used, however, only those feedback data for intact fuel elements served as input. Hence, these calculations give estimates of the degree of coherence in attainment of threshold for fuel element failure, and not a detailed representation of reactivity changes caused by post-failure movement of fuel and coolant. Because of the large negative temperature coefficients of fuel and coolant for an EBR-II-like system,⁽¹⁾ however, Cases 1 and 2 without feedback may be sufficiently conservative that the calculated order of magnitude of time spread for fuel element failure is correct.*

Three different techniques were used for this study:

- (1) ARGUS, a transient heat transfer code for cylindrical geometry;⁽²⁾
- (2) RP-129H, a space-independent, one-energy group reactor kinetics code with feedback;**
- (3) an electronic analog.⁽⁴⁾

*As will be seen in Section III below, Cases 1 and 2 with feedback are relatively uninteresting.

**RP-129 is the IBM-704 version of the AVIDAC code RE-29.⁽³⁾

Calculations for an excursion of a fast reactor with a ceramic core have been reported;⁽⁵⁾ these were made with the TRANSAC 2000 digital computer code FORE, which combines transient temperature and space-independent one-energy group techniques.⁽⁶⁾ That code, however, does not have provision for coolant flow decays of the types studied under Cases 1 and 2.

All three cases were studied previously as part of the safety analysis of the EBR-II reactor.⁽¹⁾ However, simpler models and less complete analytical descriptions than those used in this report had to be utilized at that time. Since then, ARGUS, the H version of 129, and the analog have been developed as part of the Argonne Fast Reactor Safety Program. Information has become available on the meltdown behavior of EBR-II-type fuel^(7,8) and the time for penetration of cladding by molten fuel under in-pile conditions.⁽⁹⁾ Thus, a new study of these cases appeared to be timely for the following reasons:

- a. as an exposition of the use of recently evolved tools;
- b. as an initial investigation into the degrees of coherence to be expected from some hypothetical meltdown accidents;
- c. as orientation for future studies of calculation techniques to take into account such additional complications as reactivity effects from fuel or coolant movements.

It must be emphasized that these calculations are preliminary calculations in the sense of those in References 1 and 5; that is, only reactivity feedback data relevant to an intact core are used. The results of fuel movement as it loses its integrity and of coolant boiling are not included. These calculations go one step beyond those of References 1 and 5 in that quantitative knowledge of fuel failure thresholds, and time of cladding penetration by molten fuel is used to indicate the relative degrees of coherence in fuel failure. Because of lack of detailed knowledge of the effective axial expansion of the fuel upon melting, no change in the temperature coefficient of reactivity corresponding to fuel axial expansion is made for temperatures above melting.

II. DATA AND MODELS

A. Digital Calculations

1. ARGUS

Calculations were made for the first two cases by means of ARGUS. Briefly, the code is an IBM-704 Fortran code* developed for the Argonne Fast Reactor Safety program to calculate time and spatial variation in temperature of multiregion cylindrical bodies with internal heat generation. The coolant can be static or flowing, and there is provision for variations of coolant flow. Coolant regions have physical properties specified as continuous functions of temperature by means of polynomials in temperature. In solid regions, only step changes of the material properties are possible. Since this is only a transient temperature code, no calculations are made of the changes of reactivity of the system caused by temperature changes. Although the heat source term is given as a product of functions of radius, axial position, and time, these functions must be specified as input. For these ARGUS problems, power was taken to be constant at EBR-II design and specifications. Table I gives the power distribution and coolant flow rate through subassemblies of different rows in an EBR-II core.

Table I

POWER AND COOLANT FLOW DISTRIBUTION IN AN EBR-II CORE

Row No.	Specific Power, W/cc of Fuel	Rated Coolant Velocity, cm/sec	
1	3404.5	722.7	Average values
2	3333.3	709.8	Average values
3	3143.5	637.5	Average values
4	2753.2	480.5	Average values
4	2867.0	480.5	Peripheral pins nearest core center
4	2649.4	480.5	Central pins
4	2431.8	480.5	Peripheral pins farthest from core center
5	2260.0	374.2	Average values

Figure 1 gives the mesh layout used in these calculations. Table II presents the fuel element dimensions, and Table III the material properties. In order to conserve computing time, bond and cladding were homogenized into one equivalent region.

*The code has also been translated for the CDC-3600.

Fig. 1
Mesh Layout of EBR-II-type
Element for ARGUS Program

Table II

DIMENSIONS OF EBR-II-TYPE FUEL PIN

	cm
Length	36.12
Outer radius of fuel	0.183
Outer radius of bond sodium	0.198
Outer radius of steel cladding	0.221
Outer radius of sodium coolant	0.2895 (internal pin) 0.326 (peripheral pin)

Values for the properties of sodium were taken from a standard source.⁽¹⁰⁾ Values of fuel specific heat, and of heats of transformation and fusion were obtained from measurements made for the Fast Reactor Safety Program.⁽¹¹⁾ Thermal conductivity data for the fuel were taken from

measurements up to 900°C,⁽¹⁾ were extrapolated linearly for 110°C up to the value for uranium at 1010°C, were then arbitrarily extrapolated linearly over the melting range to 0.6* of the 1010°C datum, above which conductivity was held constant.

Table III

PROPERTIES OF FUEL AND CLAD + BOND

Material	Phase Change Temperature, °C	Transformation Energy, W-sec/cc	Conductivity, W/(cm)(°C)	Volumetric Heat Capacity, W-sec/cc
Fuel				
phase-1	630	258.1	0.259	2.83
-2	1010	0	0.468	3.70
-3	1100	0	0.468	19.41*
-4	-	-	0.347	2.18
Bond + cladding				
phase-1	400	0	0.278	2.57
-2	600	0	0.282	2.63
-3	850	0	0.264	2.72
-4	-	-	0.239	2.82

*Fissium alloy has no sharp melting point. It starts melting around 1010°C and continues up to 1100°C. The specific heat of 19.41 W-sec/gm given for this region includes the heat of fusion.

Coolant properties are taken as a function of temperature of the form

$$a_0 + a_1T + a_2T^2.$$

In Table IV are given the values of these coefficients for different properties of the coolant.

Table IV

TEMPERATURE COEFFICIENTS OF COOLANT (SODIUM) PROPERTIES

Property	a_0	a_1	a_2
Density, gm/cc	0.949	-2.23×10^{-4}	-1.75×10^{-8}
Conductivity, W/(cm)(°C)	0.9067	-4.856×10^{-4}	0
Volumetric Specific Heat, W-sec/(cc)(°C)	1.3270	-6.642×10^{-4}	2.447×10^{-7}
Viscosity	6.05×10^{-3}	-1.07×10^{-5}	6.78×10^{-9}

*A brief literature search of thermal conductivity data showed that 0.6 is a typical value for the ratio of metal thermal conductivity above the melting point to that below.

Coolant inlet temperature is taken as 372°C and the saturation temperature as 1018°C * Coolant vaporization has not been taken into account in any of these problems

The relationship used with ARGUS to calculate heat transfer coefficients for Case 1 was

$$Nu = 6.96 + 0.024 Pe^{0.8},$$

where Nu is the Nusselt number and Pe the Peclet number. This correlation was recommended by Viskanta for estimating heat transfer coefficients for fully developed turbulent flow of sodium through a cluster of fuel elements of EBR-II-size on an EBR-II pitch ⁽¹²⁾. Entrance-region effects are

neglected, based on calculations by Viskanta which showed that the entrance region for liquid metals is small and that fully developed coefficients were satisfactory for EBR-II-like clusters ⁽¹²⁾.

Fig 2 Relative Coolant Flow after Complete Pump Power Failure

Figure 2, taken from data of Reference 1 and based on the assumption of complete power failure to the primary system pumps, is the flow decay curve used for Case 1. Axial power profile is that given previously for EBR-II ⁽¹⁾. Specific powers and coolant velocities are those of Table I.

2 129H

For the third case, power excursion due to rapid insertion of excess reactivity, dropping of the central subassembly into an already critical core is studied. It is assumed that reactor power at the start of the accident is 8 W. The subassembly, when its fuel portion is just above the core region, disentangles itself from the fuel gripper and drops free of hydraulic or frictional restraint into the core. Although this is a very improbable accident, since it assumes both dropping of a subassembly and an unrecognized criticality, it is considered herein because it is severe and serves as a limiting case. The central subassembly is worth about 2.7 dollars, and the insertion of reactivity is sufficiently rapid that a fast excursion results.

It was initially assumed that the excursion would be short enough that reactor periods would be sufficiently small for heat input to the

*Based on the pressure head in the core; see Reference 1

fuel to be essentially adiabatic.⁽¹⁾ Hence, the kinetics code RP-129H could be used. The H version, which was developed for analysis of the kinetics of the Transient Reactor Test Facility (TREAT), has provision for k_{ex} as a function of time t and neutron density n , given by the form

$$k_{ex}(t) = \sum_{i=1}^4 A_i(t - t_0)^i + \sum_{i=1}^4 B_i \left(\int_{t_0}^t n \, dt \right)^i.$$

The coefficients A_i can be chosen to fit reactivity insertion due to dropping of the subassembly, and the B_i to fit the negative feedback from heating of the fuel.

Reactivity input from the falling subassembly, as calculated from data of Reference 1, is shown in Figure 3. In the earlier study,⁽¹⁾ the curve of Figure 3 was approximated by

$$\begin{aligned} k_{ex}(t) &= 0.06 \, t \quad \text{up to prompt critical} \\ &= 0.2 \, t \quad \text{above prompt critical.} \end{aligned}$$

Fig. 3
Reactivity Input due to Dropped
Central Subassembly

The expression used in the present analysis (see Appendix A for derivation)

$k_{ex}(t) = 0$	$t < 0.080 \text{ sec}$
$= 5.24 \times 10^{-3} t - 2.85 \times 10^{-2} t^2 + 4.67 t^3$	$0.080 < t < 0.160 \text{ sec}$
$= 0.15 \, t$	$0.160 < t < 0.265 \text{ sec}$
$= 0.0025 \, t$	$0.265 < t < 0.303 \text{ sec}$
$= 0$	$t > 0.303 \text{ sec.}$

Since the total reactivity insertion above prompt critical would be expected to be approximately proportional to $(dk_{\text{ex}}/dt)^{1/2}$ at prompt critical, the power peak shape and integrated neutron density of the peak would be expected to be similar for the two calculations. Hence, the assumption that the reactor period is short enough that heat input is essentially adiabatic during the power peak could be expected to be good, on the basis of the earlier results. For the earlier case, $B_1 = -2 \times 10^{10}$,⁽¹⁾ and $B_2 = B_3 = B_4 = 0$. It should be noted that this feedback does not provide for the large increase in averaged fuel specific heat during the melting range (see Table III). Since the molten fuel has been found to take a finite time to penetrate the steel cladding,⁽⁹⁾ a change in B_1 at 1010°C is indicated for this accident. However, since the effect of the large heat of fusion would be to slow down the rate of temperature rise during the latter part of the excursion, inclusion of the large heat capacity above 1010°C was deferred until it could be included with heat transfer effects on the analog.

Table V

DELAYED NEUTRON DATA

i	λ_i	β_i
1	0.0127	0.000232
2	0.0318	0.001423
3	0.1153	0.001333
4	0.3110	0.002956
5	1.40	0.001121
6	3.870	0.000282
Total		0.007347

Prompt neutron lifetime used is 8×10^{-8} sec.⁽¹⁾ Delayed neutron data are given in Table V.

B. Analog Calculations

The dependence of the heat transfer coefficient on the coolant velocity and one phase change (at 1010°C) are included in the analog simulations. The fuel element is described by a bank of point integrators, with five axial and five radial mesh points. Of the five radial points in each axial row, three are in fuel, one in the homogenized bond and cladding region, and one in the coolant. As a test of the mesh layout and the averaging techniques for physical properties, accidents of both Case 1 and Case 2 types were run without feedback for comparison with ARGUS results. In both Case 1 (flowing coolant plays a dominant role) and Case 2 (coolant is stagnant), the agreement with the finer-mesh ARGUS results was satisfactory.⁽⁴⁾ It has been observed before that even a coarse analog mesh with judiciously chosen points can give accurate results in solving time-dependent diffusion equations.⁽¹³⁾ A detailed description of the simulator is given in Reference 4. Table VI presents the values of the physical properties used in the analog calculations.

The flow decay curve used for Case 1 was that of Figure 2. Two flow assumptions were studied for Case 2:

- Coolant flow decays linearly from 100% to 1% of full design flow over a one-second interval.
- Coolant flow stops "instantaneously," by decaying to 0.1% of full design flow in 4 msec.

Design flow values are those of Table I. Fuel element and coolant channel dimensions are the same as those used in the ARGUS calculations. Axial distribution of power is the same as for ARGUS. Specific powers and coolant velocities are those of Table I.

Table VI

PHYSICAL PROPERTY DATA FOR ANALOG CALCULATIONS

	$\rho C_p,$ cal/(cc)(°C)	$k_2,$ cal/(cm ²)(sec)	Remarks
Fuel	1.061	0.1118	ρC_p of fuel includes energy of transformation at 630°C
Fuel (melting range, 1010°C and above)	4.444	0.1118	ρC_p includes heat of fusion
Bond + cladding	0.633	0.065	
Coolant	0.234	0.231	

Prompt neutron lifetime and delayed neutron data are the same as for the 129H digital calculation. The Case 3 analog calculations were made with a circuit calculating log neutron density instead of neutron density,⁽¹⁴⁾ in order to cover the wide range of power necessary.

To obtain the temperature coefficients of reactivity feedback, only core and upper blanket are considered. Average temperature change in the upper blanket region is assumed to be the same as that in core. Though this is not correct, the error is not serious, for the feedback coefficients in the blanket region are an order of magnitude less than those of core region.

Reactivity feedback due to structure and clad temperature change is included in the coolant coefficients, on the assumption that the temperature average for structure is equal to the coolant temperature. These data are given in Table VII.

Table VII

FEEDBACK DATA FOR ANALOG CALCULATIONS

(1) For change of fuel temperature:

Axial growth of the fuel	- 0.39×10^{-5}
Radial growth of the fuel	- 0.09×10^{-5}
Axial growth of blanket uranium	- 0.012×10^{-5}
Radial growth of blanket uranium	- 0.008×10^{-5}
Total	$0.5 \times 10^{-5} \Delta k / (k)(°C)$

(2) For change of coolant temperature:

Density change in coolant	- 0.87×10^{-5}
Radial growth of support structure	- 0.97×10^{-5}
Density change in gaps	- 0.38×10^{-5}
Total	$2.22 \times 10^{-5} \Delta k / (k)(°C)$

For calculating the feedback reactivity, the temperature in the fuel element is averaged over all axial sections for each radial region, multiplied by the respective weight factor and the reactivity coefficient, and summed. Weight factors are as follows:

<u>Radial Region</u>	<u>Weight Factor</u>	<u>Coefficient</u>
1	1/16	3.1×10^{-7}
2	1/2	2.5×10^{-6}
3	7/16	2.2×10^{-6}
Coolant	1	2.22×10^{-5}

As the reactivity coefficients are given for the reactor as a whole, they are applicable only for the average fuel pin. Hence, first the accident is simulated for a pin with the average power and its power transient is obtained. For all the other pins, this power transient multiplied by the ratio of their respective power to the average is used as the input power.

C. Failure Data

In order to estimate the degree of coherence in attainment of fuel failure conditions, the failure threshold and rates of penetration of cladding must be known, at least approximately.

Experiments with EBR-II-type fuel pins subjected to transient melt-down experiments in TREAT, in the absence of sodium coolant, showed that the pins failed rapidly and extensively for maximum cladding surface temperatures of 1015°C and above.⁽⁷⁾ These failures were attributed to local dissolution of cladding with subsequent expulsion of molten fuel by the vapor pressure of bond sodium and the pressure of argon inside the expansion space in the top of the fuel pin. The time for penetration of the cladding by molten fuel was measured, in-pile, in TREAT experiments to be about one second (and to range from about 0.7 to about 1.5 sec).⁽⁹⁾ At 1200°C, failure is by bursting of the cladding, and the delay for penetration does not occur.⁽⁹⁾ Results of TREAT experiments on the failure of EBR-II pins in (initially) stagnant sodium were correlated with maximum calculated fuel-cladding interface temperatures, and found to have a failure threshold temperature essentially the same as that for the cladding surface failure threshold of the previously investigated dry samples.⁽⁸⁾

Recent results from transient experiments run in TREAT with previously irradiated EBR-II pins in the absence of sodium indicate that the presence of appreciable quantities of fission gases in the fuel does not affect markedly the threshold of failure, for sample burnups up to 1 a/o, at least.⁽¹⁵⁾

The range in failure time observed in TREAT⁽⁹⁾ is not interpreted as principally due to a statistical effect; rather, this range is believed to

result from the wide variation in rates of steel penetration by molten uranium in the relevant temperature range and to early pressure-induced failure of cladding weakened by partial penetration. However, for excursions in which the fuel temperatures rise from 1010°C to 1200°C in the order of a second, the variation should occur.

For slower temperature excursions, failure can occur for a different reason. Fuel ejected from a failed internal pin could attack an intact peripheral pin from the outside (or vice versa). Thus a maximum spread of about 2 sec is indicated within a single subassembly.

III. RESULTS

A. Failure of Pump Power

1. Constant-power Case

In the case of failure of pumping power, transport of heat along the axis of the fuel element by flowing coolant is appreciable, and the hottest axial segment of the fuel pin surface is the top segment. Figure 4 is a graph of maximum fuel-cladding interface temperatures calculated by ARGUS for the constant-power case as a function of time for five fuel pins:

- (a) central subassembly, internal pin;
- (b) central subassembly, peripheral pin;
- (c) fourth-row subassembly, internal pin;
- (d) fourth-row subassembly, peripheral pin nearest core center;
- (e) fourth-row subassembly, peripheral pin farthest from core center.

Fig. 4. Maximum Fuel-Cladding Interface Temperatures for Pumping-power Failure for the Constant-power Case

The central subassembly has the highest steady-state design power, but the fourth row has the highest outlet coolant temperature. The figure shows that, because of the orificing of the coolant flow to subassemblies, there is less difference between internal pins in the central and fourth rows than between extreme positions in each subassembly. Internal pins reach the failure threshold of 1015°C at times about 1.6 sec apart. Within the central subassembly, however, the spread in attaining the threshold is about 5.7 sec. The spread is less inside the lower-power fourth row: 5 sec. Above the phase change of 1010°C , the rates of temperature rise are lower because of the higher average fuel specific heat used in the calculations, but the magnitudes of the time spread remain about the same.

Heat stored in an internal fuel pin during this excursion is only about 1.8% of that carried away by the coolant; that stored in a peripheral pin, about 1.1%.

2. Calculations with Feedback

Inclusion of the negative temperature feedback from fuel elements and coolant changes the course of the temperature excursions entirely, since the feedback reduces reactor power markedly. Figure 5 is a plot from the analog output for the "average" core fuel pin, showing the decrease in power as the flow coasts down following power failure. By 25 sec after the power loss, coolant flow is 10% of design, and the reactor power has dropped by 60%. Because of uncertainties in the flow and heat transfer, once natural convection has taken over, this analysis was not extended beyond 25 sec.

Fig. 5. Relative Reactor Power and Coolant Flow for Pumping-power Failure with Feedback Included

Maximum fuel-cladding temperatures are shown in Figure 6 as a function of time for four of the fuel pins considered previously: central-row internal pin, fourth-row internal pin, fourth-row peripheral pin nearest core center, and fourth-row peripheral pin farthest from core center. Because the calculation is stopped at 25 sec, the time spreads in attaining 1015°C cannot be given. However, as an indication of the degree to which the excursion has been slowed down, the spreads in reaching 850°C can be noted: for the central pins in central and fourth-row subassemblies, 3.4 sec, and within the fourth-row subassembly, 7 sec.

Fig. 6. Maximum Fuel-Cladding Interface Temperatures for Pumping-power Failure with Feedback Included

B. Blocked Orifice

In the case of a blocked orifice, unlike the failure of pumping power, change in the heat content of the fuel is dominant. Hence, the following approximate relationship can be set up for any given axial section if feedback is neglected:

$$\tau_t = \frac{\sum_i V_i \int_{T_0(i)}^{T_t(i)} (\rho C_p)_i dT}{q},$$

where

τ_t is time to reach the failure threshold temperature

V_i is volume of the i^{th} radial region

$T_0(i)$ is temperature of the i^{th} region at the start of the accident

$T_t(i)$ is temperature of the i^{th} region at the time of failure

$(\rho C_p)_i$ is volumetric heat capacity of the i^{th} region

q is the power of the j^{th} axial section.

Evaluation of this expression for the axial center of the element yields

$$\tau_t = \frac{317 + 570 A}{0.1189 q}.$$

With $A = 0.110 \text{ cm}^2$ for internal elements and 0.181 cm^2 for peripheral pins and with a q of 3000 W/cc of fuel,* the spread in failure time is of the order of 0.1 to 0.15 sec for a typical subassembly.

Results of the more detailed ARGUS calculations for this accident are shown in Figure 7, which is in general agreement with the order of magnitude of the above estimate. The figure shows the initial spread in maximum fuel-cladding interface temperatures caused by the difference in coolant flow between internal and peripheral pins. Two changes in slope occur in each curve: one associated with the fuel phase change at 630°C , and one at the beginning of the melting range of 1010 to 1100°C . Initial temperature rises are slow, during the time that coolant flow is decaying, but the rises become appreciably more rapid as the flow reaches its final value of 1% of design at one second. For the central subassembly, the variation in time of attaining 1015°C is about 0.15 sec for central and peripheral pins.** Because of the greater spread in power across the fourth-row subassembly, the spread in time is about 0.26 sec for central pin and peripheral pin nearest to the core center. The ARGUS calculation results of

Fig. 7. Maximum Fuel-Cladding Interface Temperatures for Blocked Orifice in the Constant-power Case

* This corresponds approximately to a third-row subassembly; see Table I.

** For the relatively fast temperature transients due to flow blockage, maximum interface temperatures occur at, or slightly above, the power maximum at the axial midplane as temperatures approach the failure threshold.

Figure 7 are for the case of linear decay of coolant flow over one second from 100% to 1% of the full design value. However, this decrease is sufficiently rapid that the time spreads of the result are not greatly sensitive to the exact assumptions regarding coolant flow decay. Figure 8 compares maximum fuel-cladding interface temperatures calculated, with constant power, for an "instantaneous" orifice blockage in which the flow decay takes only 4 msec, with results from the linear ramp used to obtain the data plotted in Figure 7. Results for both peripheral and internal pins of the central subassembly are given in the figure. After blockage has been accomplished (0.004 sec for the "instantaneous" block, and 1.0 sec for the linear block), the relative shapes of the two pairs of curves are sufficiently similar that the principal effect of the change in blockage assumptions is a time translation. The spread in time between attainment of 1015°C maximum fuel-cladding temperature has dropped from about 0.15 sec for the linear block to about 0.12 sec for the "instantaneous" block.

Fig. 8. Comparison of Maximum Fuel-Cladding Interface Temperatures for Two Different Assumptions Regarding Orifice Blockage for the Constant-power Case

Inclusion of feedback, by use of the analog technique, may not affect materially the course of the incident involving a blocked orifice. If the accident is assumed to happen to a single subassembly, with only one subassembly undergoing a temperature transient, the temperature rise would be slowed by a feedback that is only a few per cent of that for the entire core. By means of Figure 9 a comparison may be made of the analog outputs of maximum fuel-cladding temperature resulting from "instantaneous" flow blockage for the internal pins of the central core subassembly for the three feedback assumptions: no feedback; feedback is 2% of that for the entire core; feedback is that for the entire core. The figure indicates that a single subassembly blockage is reasonably approximated by the no-feedback case, but that if some mechanism were postulated for a block of the entire core inlet, the course of the accident would be appreciably slower.

Fig. 9. Comparison of Maximum Fuel-Cladding Interface Temperatures for Three Different Assumptions Regarding Feedback When There Is Instantaneous Flow Blockage

Slower rates of temperature rise for complete core blockage imply greater degree of incoherence in attaining the failure threshold of 1015°C . Figure 10 shows the variation in maximum fuel-cladding interface temperatures for peripheral pins in four different rows, assuming instantaneous flow blockage. In this case, the maximum time difference between central and fourth-row peripheral pins shown in Figure 7 has increased to more than one second.

Fig. 10. Maximum Fuel-Cladding Interface Temperatures for Peripheral Elements in Four Different Rows for Full Feedback When Instantaneous Flow Blockage Occurs

C. Dropped Subassembly

Results of the digital calculations of average neutron density and integrated neutron density, as a function of time, are shown in Figure 11. The reactor becomes prompt critical at about 194 msec after the start of the hypothetical accident. Within 3 msec after attaining prompt criticality, the neutron density has risen by a factor of 10^6 . After 205 msec the

integrated neutron density reaches a value of 10^7 . This rise corresponds to an energy input sufficient, in 10 msec, to produce a temperature rise in the fuel of 1600°C .

Fig. 11

Average Neutron Density and Integrated Neutron Density for Dropped-subassembly Accident Obtained by Digital Kinetics Calculation

The time for the average integrated density to reach a value corresponding to 1200°C is about 5 msec. At that point in the excursion, the rate of temperature rise is about $160^\circ\text{C}/\text{msec}$. Since the ratio of maximum radial power (central subassembly) to minimum radial power (fifth-row subassembly) is 1.51, the spread in time at which fuel reaches 1200°C at the hottest positions is approximately 4 msec. The figure is similar to the corresponding figure in Reference 1, except that, in the earlier calculations, the power peak came at about 126 msec instead of between 196 and 197 msec.

Although Figure 11 confirms the general features of the earlier results, it still does not indicate the possible effects upon the course of the excursion caused by the fuel heat of fusion,* or by the possible marginal results of heat transfer to the fuel-pin cladding and to the stagnant sodium coolant. In order to take into account both of these effects, the accident was rerun on the analog. Figure 12 shows the axially averaged temperatures of the fuel centerline, fuel surface, and coolant for the average channel† as a function of time. Log neutron density is shown on the same graph for reference. Although the early stages of the incident are the same (fuel temperature in Figure 12 may be compared with integrated neutron density in Figure 11), the latter stages are moderated somewhat: the rate of fuel temperature rise breaks sharply at 1010°C because of the heat of fusion, and an appreciable heating of cladding and coolant occurs after the power peak. Results of these two effects are displayed in Figure 13. In

*When Reference 1 was written, the heat of fusion of neither the uranium-fissium alloy nor uranium had been measured directly.

†The maximum axial temperatures are at the power maximum on the axial midplane.

this figure are shown fuel surface temperatures, along the axial midplane,[†] for the central fuel pins and the average channel fuel pins, which have a 20-msec spread in attainment of the fuel-melting threshold.

Fig 12 Average Channel Temperatures as a Function of Time for Dropped-subassembly Accident by Analog Calculations

Fig. 13. Maximum Fuel Surface Temperatures as a Function of Time for Dropped-subassembly Accident from Analog Calculations

[†]See previous page.

IV. DISCUSSION

A. Failure of Pump Power

Even in the constant-power case, the spread in calculated times of attainment of the failure threshold is large. Time variations across a subassembly in reaching a fuel-cladding interface temperature of 1015°C are large enough that the time spread over a single subassembly should be taken to be about 2 sec. This time increment should also be combined with the approximate increment of 2 sec from subassembly to subassembly, which would yield a duration in initiation of failure of the order of 3 sec.

The significance of appreciable spread in failure can be seen qualitatively in Figure 14. In that figure, the total reactivity changes produced by meltdown of a single subassembly are indicated for two cases. (Within

Fig. 14. Qualitative Effect of Variations in Time at which Fuel Elements Reach Failure Threshold for the Case of Failure of Pump Power

the context of the present study, the sign of the reactivity change, its magnitude, and the detailed physical motions producing the change are not considered.)

Curve A gives the probable reactivity change as a function of time to be expected when pump power fails, assuming that all elements reach the failure threshold simultaneously.

Curve B is the approximate relationship to be expected for the incoherent failure calculated in the preceding section for the central fuel subassembly. Approximately 65% of the reactivity change would occur between 0.7 and 1.5 sec after

the central elements reach the failure threshold. The remaining 35% would be added as the peripheral elements fail, principally between 2 and 3 sec. In this case, then, the overall rate of reactivity change is decreased by about a factor of three. Similar decreases in the overall rate of change would occur for the other subassemblies.

The decrease in overall rate of reactivity change for the entire reactor would be larger, of course, by a factor of about five over that for simultaneous attainment of the failure threshold throughout the reactor.

If the reactivity feedback of intact elements is taken into account, the time spread over the entire core increases, and the incident is much less interesting.

B Blocked Orifice

The degree of incoherence in reaching the failure threshold for the blocked orifice is much less, and rates of temperature rise above the fuel-cladding threshold of 1015°C are sufficiently high that failure due to penetration of cladding should be supplanted by failure due to internal pressure. For simplicity, only the most severe of the three feedback cases considered under Section III B (see Figure 9), the constant-power case, will be discussed here.

Figure 15 presents a qualitative picture of the effects of incoherence in failure for the case in which a blocked orifice occurs at constant power. The figure contains the total reactivity change produced by melt-down of a single subassembly for two cases

Fig. 15 Qualitative Effect of Variations in Time at which Fuel Elements Reach Failure Threshold in the Case of a Blocked Orifice When the Power is Constant

Curve A gives the probable reactivity change for the central subassembly as calculated in the previous section. About 65% of the change occurs, when the internal pins reach 1200°C , during the time for fuel movement upon failure of a single pin, taken here to be about 0.1 sec*. The remaining change, due to failure of peripheral pins upon attaining 1200°C begins at 0.15 sec and lasts 0.1 sec. Overall, then there is a duration of 0.25 sec

(or, if the time for fuel movement of 0.1 sec is neglected, the duration is simply the 0.15-sec spread of Section III)

Curve B shows the probable reactivity change for the fourth-row subassembly. The time of change due to failure of internal pins upon reaching 1200°C is finite, not only because of the time necessary for fuel movement, but because of the appreciable change in power across the subassembly. As the peripheral pin nearest the core center fails, there is a rise in reactivity change beginning at about 0.26 sec. The failures of peripheral pins near the core edge occur later. For curve B, the time duration is of the order of 0.5 sec

Because of the spread in failure time between subassemblies, a duration of reactivity change somewhere between one sec and the 0.5 sec of curve B is indicated for the case of flow blockage of the entire core with power held constant

*A variety of calculations, based on simple models, suggests that this value is a reasonable estimate for the order of magnitude of time scale of fuel movement following failure of an element in the absence of sodium. The time scale for fuel movement has not been measured directly, and the complexities of describing even one element theoretically are sufficient to preclude a definitive calculation.

C. Dropped Subassembly

The time scale for the temperature excursion involving a dropped subassembly, calculated by means of the digital kinetics code (see Figure 11), is about 4 msec, sufficiently short that the duration of reactivity change for that hypothetical accident would be determined by the time scale for fuel movement.

However, the more detailed analog computations yield a time spread in attainment of the 1010°C fuel-melting threshold of the order of 0.04 sec. This suggests a spread of 0.04 sec in reaching the 1200°C threshold for fuel element failure by internal pressure, greater by an order of magnitude than that from the digital calculations. This time is long enough that it should be "folded" into the time for fuel movement to obtain the duration of reactivity change in the dropped-subassembly case.

D. Conclusions

The degree of coherence in failure of an EBR-II-like core has been studied for three hypothetical accidents - ranging from one dominated by the time for penetration of cladding by molten fuel, by the time scale over which various elements reach the threshold for failure by internal pressure, to a fast accident in which the time duration for fuel movement resulting from failure competes with the time scale for elements to fail by internal pressure. In the first two cases, the duration is of the order of one sec; in the last, of the order of 0.1 sec.

Thus, to this degree of approximation of the actual behavior of fuel elements under meltdown conditions, it appears that the results of TREAT experiments to date on EBR-II Mark I fuel elements do not lead to meltdown incidents appreciably more severe than the maximum credible accident considered for EBR-II.⁽¹⁾ The calculations, however, do not include feedback produced by fuel movement, nor a detailed 3-dimensional dependence. TREAT experiments have not yet included direct measurement of fuel movement upon failure of EBR-II elements, effects of clustering and flowing coolant, a detailed investigation into the effects of fission product gases, or possible secondary reassembly of meltdown products following the initial fuel movements.

APPENDIX A

An Expression for Excess Reactivity Addition during
the Central Subassembly Drop into the Core

Reactivity of the subassembly as a function of position in the reactor core is given as

$$\frac{\Delta k}{\Delta x} = \frac{2W}{L} \sin^2 \left(\frac{\pi x}{L} \right), \quad (1)$$

where

W = total worth of the subassembly

and

L = length of the core.

When the subassembly falls freely under gravitation, the velocity is gt and the distance x travelled up to time t is $\frac{1}{2} gt^2$. Therefore the rate of addition of reactivity at time t is

$$\frac{2W}{L} \sin^2 \left(\frac{\pi gt^2}{2L} \right) gt. \quad (2)$$

Total reactivity added up to time t is given by

$$k_{ex}(t) = \int_0^t \frac{2W}{L} \sin^2 \left(\frac{\pi g \tau^2}{2L} \right) g \tau \, d\tau \quad (3)$$

$$= \frac{2W}{\pi} \left[\frac{1}{2} \frac{\pi g \tau^2}{2L} - \frac{1}{4} \sin \left(\frac{2 \pi g \tau^2}{2L} \right) \right]_0^t = \frac{Wgt^2}{2L} - \frac{W}{2\pi} \sin \frac{\pi gt^2}{L}. \quad (4)$$

If the values $W = 0.02$ and $L = 45$ cm are used,

$$k_{ex}(t) = 0.217778 t^2 - 0.003182 \sin(68.445 t^2). \quad (5)$$

The final time t_m for fall through the entire core is given by

$$\frac{1}{2} gt_m^2 = L$$

or

$$t_m = \sqrt{\frac{2L}{g}} = 0.303 \text{ sec.}$$

The form of a curve obeying Equation 5 is shown in Figure 3. The curve therein was obtained by use of a least-squares calculation. This analysis gave

$$\begin{array}{ll}
 k_{\text{ex}}(t) = 0 & t < 80 \text{ msec} \\
 = 5.24 \times 10^{-3} t - 2.85 \times 10^{-2} t^2 + 4.67 t^3 & 80 < t < 160 \text{ msec} \\
 = 0.15 t & 160 < t < 265 \text{ msec} \\
 = 0.0025 t & 265 < t < 303 \text{ msec} \\
 = 0 & t > 303 \text{ msec}
 \end{array}$$

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