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Argonne National Laboratory

THE ROLE OF THE ADJOINT FLUX
IN DETERMINING REACTOR STABILITY

by

Richard Harry Jason

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THE ROLE OF THE ADJOINT FLUX
IN DETERMINING REACTOR STABILITY

by

Richard Harry Jason

Reactor Physics Division

Reproduced from Thesis Submitted in Partial Fulfillment
of the Requirements for the Degree Doctor of Philosophy in
the Graduate School of Purdue University

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PREFACE

This is the second volume of a two-volume report. Volume I describes the FARET facility design and the fuel-development program. (The contents of Volume I are summarized in Section 10 of Appendix F of this document.) Volume II covers the safety analysis and related work which took place during AEC considerations of the FARET preliminary safety analysis. It covers the PSAR (Preliminary Safety Analysis Report), its supplements No. 1 and No. 2, and related correspondence between the AEC and the Laboratory during the period mid 1964 to late 1965. This period culminated in the ACRS formal project review on November 10, 1965. At that time, the ACRS indicated that as a result of their review, they would write a report advising that FARET could be built at the proposed site with the following general provisions:

1. There should be a continuing effort to improve the design of the reactor control system, particularly under postulated accident conditions, to avoid the possibility of inadvertent control-rod movement that would increase reactivity.
2. There should be a continuing effort to develop a reliable and independent secondary shutdown system for the reactor.
3. The Committee should be kept informed of significant design changes pertinent to safety that may occur from time to time subsequent to the meeting.

Following an introduction (Section I), this report consists of two main parts the first of which describes and evaluates the possible circumstances leading to and culminating in the maximum credible accident. This accident and its subsequent effects on the FARET surroundings is described in Section II. The second main part of this report is contained in Section III. It describes the results of investigations and analyses performed in connection with the FARET PSAR and which resulted in conditions less severe than the maximum credible accident.

VOLUME II

TABLE OF CONTENTS

	<u>Page</u>
I. INTRODUCTION.	13
II. THE MAXIMUM CREDIBLE ACCIDENT AND ITS ASSOCIATED EFFECTS.	15
A. MCA Description and Evaluation.	15
B. Core Meltdown Leading to an MCA.	17
1. Introduction.	17
2. Analytical Procedures.	19
3. Energy Available to Do Work.	21
4. Methods of Reactivity Insertion and Excursion Results.	22
C. Pressure and Temperature in Containment Following the MCA.	23
1. The Problem and Method of Solution.	23
2. Input Parameters.	24
3. Results.	24
D. Evaluation of Containment Structure.	28
1. Summary.	28
2. Guaranteed Maximum Leak Rate.	28
E. Release of Fission Products.	28
1. Core Fission Product Inventory.	31
2. Containment Leakage Fraction.	32
3. Atmospheric Diffusion.	32
4. Inhalation Dose.	34
5. Direct Gamma Dose from Containment.	35
6. Results.	36
III. OTHER ANALYSES.	40
A. Core Meltdowns.	40
1. General.	40
2. Methods of Reactivity Insertion and Excursion Results.	40
B. Fuel Handling and Storage Accidents.	42
1. Reactor Vessel Cover Removal.	42
2. Power Loss during Fuel Handling.	43
3. Jamming of a Spent Fuel Subassembly.	43
4. Dropping of a Fuel Subassembly.	44
5. Criticality in the Fuel Storage Tank.	44

TABLE OF CONTENTS

	<u>Page</u>
C. Coolant System Failures	45
1. General System Malfunctions	45
2. Inlet Coolant Pipe Failure	56
3. NaK Annulus System Leaks	63
D. Miscellaneous Considerations	65
1. Hydraulic Ejection of Control Rods	65
2. Reactor Vessel Cover Blow-off	70
3. Sodium Evaporation within Containment	79
4. Electrical and Control Disturbances	80

APPENDIXES

A. Reference Cores	88
1. General	88
2. Reference Zoned Core	88
A. Reactivity Effects	95
B. Measurements of Neutron Reaction Rates	96
C. Reactor Dynamics	96
3. Reference Small Core	96
A. Irradiation Studies	101
B. Reactivity Coefficients	101
C. Vented Fuel Experiments	102
B. FARET Containment Structure Analysis	103
1. Nuclear Excursion Effects	103
A. Analytical Procedures	103
B. Energy Release and Distribution	104
C. Energy Absorption	106
D. Missile Containment	111
E. Additional Energy Absorption Mechanisms	113
F. Thermal Shocking of Cell Liner	114
2. Containment Design	114
A. Design Stress Basis	114
B. Margin of Safety	114
3. Determination of Containment Leak Rate	115
4. Periodic Leakage Rate Test Practices	116
C. Core Meltdown Studies and Energy Yields	117
1. Description of the AX-1 Code	117
2. Description of Reactor Meltdown Code	118

TABLE OF CONTENTS

	<u>Page</u>
3. Small Core Excursion Analyses	120
A. Ramp Insertions.	120
B. Small Reactivity Insertions	124
4. Zoned Core Analyses Excursion	125
A. Ramp Insertions.	125
B. Small Reactivity Insertions	129
5. Other Core Analyses.	130
6. General Comments	133
7. Sensitivity of AX-1 Results to Parameter Changes	133
D. Criteria Related to Safety	135
1. Seismic	135
2. Containment Structure.	136
3. General Reactor Facility	138
A. Reference Cores	138
B. Planned Reactivity Excursions	139
C. Reactivity Limitations.	139
D. Excess Reactivity.	140
E. Reactivity Shutdown Margin	140
F. Core	140
G. Reactor Vessel	141
H. Control Rod Drives.	141
I. Coolant System	142
J. Control and Instrumentation.	143
K. Auxiliary Systems	144
L. Fuel Handling and Storage	145
M. Power	146
N. Ventilation System	147
E. Site Data.	148
1. Hydrology	148
A. Soil and Subsurface Characteristics.	148
B. Drainage.	148
2. Seismology	149
3. Meteorology	151

TABLE OF CONTENTS

	<u>Page</u>
F. Miscellaneous Subjects	157
1. Reactor Vessel	157
2. Critical Experiments	157
3. Materials	157
4. Seismic Analysis	163
5. Sodium Chemical Reactions	164
A. Chemical Reactions	164
B. Sodium Fire Code (1922/RE)	165
C. Cell Pressure as a Function of Time after a Sodium-Air Reaction.	165
6. Sodium Pool Burning in the Cell	169
A. Burning Rate 5.0 lb/hr-ft ² for Case C, Table 24	170
B. Burning Rate 5.0 lb/hr-ft ² and Burning Area 1200 ft ²	171
7. Filter Protection	171
8. U-Pu Separation Effects	172
9. Ground Level Activity Release	172
10. Summary of Facility Description in Volume I	173
REFERENCES	177

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1.	Heat Energy Converted to Kinetic Energy	19
2.	Post-MCA Conditions for Zero Leakage	25
3.	Post-MCA Conditions for Low Leakage	26
4.	Post-MCA Conditions for Guaranteed Maximum Leakage.	27
5.	Relative Inhalation Doses Downwind	39
6.	Reactor Analytical Model.	47
7.	Case 1a	50
8.	Case 1b	51
9.	Case 2a	52
10.	Case 2b	53
11.	Post-Scram Design Conditions	55
12.	Post-Scram Design Conditions	56
13.	Reactor Coolant Systems Elevations and Hydraulic Profile	57
14.	Reactor Annulus System and Guard Piping	58
15.	Double Contained System Static Pressures	64
16.	Simplified Primary Coolant Systems Diagram	66
17.	Arrangement of Reactor Vessel Internals	67
18.	Abnormal Flow Distribution in Vessel	68
19.	Energy Distribution at Time of Peak Core Pressure	77
20.	Core Conditions at Disassembly	78
21.	Reference Zoned Core	89
22.	Reference Small Core	97
23.	Reactivity of Collapse vs Change in Core Height	119
24.	Doppler Reactivity vs Temperature Rise	120
25.	Inverse Power Period vs Time for Ramp Reactivity Input of \$3/sec in Reference Small Core	122
26.	Reactivity of Collapse vs Percent of Core Meltdown for Reference Small Core	123
27.	Inverse Power Period vs Energy Density for Reference Small Core	124

LIST OF FIGURES

<u>No.</u>	<u>Title</u>	<u>Page</u>
28.	Total Energy Available vs Inverse Power Period for Reference Cores	124
29.	Inverse Power Period, Power Density, and Energy Density vs Time for Reference Small Core	125
30.	Averaged Sodium Coefficient per Unit of Radial Area vs Radius for Reference Zoned Core	126
31.	Inverse Power Period vs Time for Ramp Reactivity Input of \$10/sec in Reference Zoned Core	127
32.	Inverse Power Period and Power Density vs Time for Ramp Reactivity Input of \$2/sec in Reference Zoned Core	127
33.	Inverse Power Period vs Energy Density for Reference Zoned Core	129
34.	Net Reactivity Insertion, Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core	130
35.	Net Reactivity Insertion, Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core	131
36.	Net Reactivity Insertion, Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core	131
37.	Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core	131
38.	Small Core Meltdown	131
39.	NRTS East Area Seismic Faults and Lineaments	151
40.	NRTS Annual Average Wind Roses	153
41.	NRTS Central and TAN Annual Wind Roses	154
42.	NRTS Central Wind Roses at 20 ft	155
43.	NRTS Central Wind Roses at 250 ft	156
44.	Peak Cell Pressure vs Sodium-to-Oxygen Mole Ratios	166
45.	Cell Gas Pressure vs Time after Ejection of 15,000 lb of Sodium into Cell	168
46.	Pressure Rise in Cell due to Cool Burning of Sodium	170

LIST OF TABLES

<u>No.</u>	<u>Title</u>	<u>Page</u>
1.	FARET Safety Considerations.	14
2.	Assumed Fractional Release of Fission Products.	29
3.	Fission Product Inventory after 400 days of Irradiation at 50 MW	31
4.	Total Integrated Inhalation Doses from Stack Release-- Inversion Conditions	36
5.	TID at EBR-II for Neutral Lapse.	37
6.	Total Integrated Inhalation Doses at EBR-II from Ground Level Activity Release.	37
7.	Inhalation Dose Rates to FARET Personnel.	38
8.	Maximum Total Integrated Dry Deposition at EBR-II	38
9.	Estimated Energy Available to Do Work.	40
10.	Calculated k_{eff} for Fuel Storage Tank	45
11.	Potential Coolant System Malfunctions	46
12.	Delayed Neutron Data for FARET Transient Studies	48
13.	Transient Problem Parameters	49
14.	EBR-II and NRTS Power Outages	81
15.	Calculated Parameters for Reference Zoned Core	90
16.	Calculated Parameters for Reference Small Core	98
17.	Energy Release Damage Factors.	105
18.	Summary of Meltdown Studies--Reference Small Core	121
19.	Calculated Average Doppler Coefficient	122
20.	Summary of Meltdown Studies--Reference Zoned Core	126
21.	Allowable Stresses for Ferritic and Austenitic Steels	160
22.	Estimated Allowable Stresses for Superstrength Alloys.	161
23.	Summary of Results for Pool Burning of Sodium	169

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I. INTRODUCTION

The FARET Facility is designed to accommodate a variety of experiments essential to the design of future large fast reactor systems. The preliminary analysis of the potential hazards and consequences associated with these experiments is based on two reasonably typical cores to be used in the FARET reactor.

The two cores chosen are the reference zoned core and the reference small core. They are considered reasonably typical of the assemblies which can be operated in FARET. They also serve as models to generally bracket limits of the expected core characteristics for the experimental program. (Core I, which is described in Volume I as the core for the first FARET experiments, falls between the limits generally bracketed by the two reference cores considered in the preliminary safety analysis.)

The scope of safety analyses performed in connection with FARET is outlined in Table I. The conclusions drawn from them are as follows:

1. The maximum credible accident could result from either the partial loss of coolant (or coolant flow) or alternately, the dropping of a fuel subassembly into the zoned core. Either of these situations may result in a core meltdown and subsequent collapse. Such an extremely unlikely nuclear accident could have a mechanical energy equivalent to the detonation of a 25- to 30-lb bare charge of TNT in the core. The consequences could include destruction of the reactor vessel, lifting of the reactor vessel cover, generation of smaller projectiles, ejection of sodium into the cell, and release of fission products from the fuel to the cell and cavity.
2. Structural analysis indicates that the energy-absorption capabilities of the containment structure are significantly higher than the postulated energy release.
3. Although a fraction of the resulting activity could conceivably escape to the atmosphere through the exhaust stack, calculations indicate that the total integrated dose at a point 14.4 miles distant (the point of maximum theoretical dose) would be below permissible limits.
4. The probability of the postulated accident actually occurring is extremely small because of facility design and conservative operating procedures. It is considered extremely unlikely that the necessary sequence of events involving equipment and system failures and operational errors can occur in the precise manner required to permit the maximum credible accident to occur.

TABLE 1. FARET Safety Considerations

<u>Fuel-handling Accidents</u>	<u>Core Meltdown Studies and Energy Release</u>
Reactor vessel cover removal	Loss of coolant
Malfunction of, or loss of power to fuel-handling machine	Zoned core (maximum credible accident)
Failure of fuel-handling machine coolant system	Test zone only
Jamming of spent fuel subassembly partially removed from reactor	Unplanned removal of control rod
Structural failure of fuel-handling machine or subassembly, causing release of subassembly:	Small core
Into reactor during loading	Zoned core
Into reactor during unloading	Dropping of fuel subassembly into the core
Onto cell floor during transit to storage tank	Small core
Into wet fuel storage tank	Zoned core
<u>Fuel Storage Tank Considerations</u>	Startup-type accidents
Loss of sodium coolant	Loss of coolant
Effect of loss of sodium coolant on reactivity	Unplanned removal of control rod from zoned core
Critical loading configurations	Dropping of fuel subassembly into the core
	Small core
	Zoned core (maximum credible accident)
	Structural changes
	Change in system temperature, e.g., introduction of cold sodium into the reactor
	Introduction of moderator
	Failure of experimental subassembly
<u>Convective Cooling of the Reactor</u>	<u>Containment Structure Considerations</u>
Failure of main power	Atmosphere
Failure of emergency power	Inert gas
Failure of main coolant system	Air
Main primary pump fails	Introduction of water
Main secondary pump or sodium-air heat exchanger fails	Sodium vapor
Failure of auxiliary coolant system	Missiles
Auxiliary primary pump fails	Reactor vessel cover
Auxiliary secondary pump or NaK-air heat exchanger fails	Small objects
Main and auxiliary coolant systems fail simultaneously	
Inadvertent drainage of reactor vessel	
Gross failure of reactor vessel	
Gross failure of main or auxiliary coolant systems	
High blanket gas pressure	
Syphoning of vessel	
<u>Sodium Fires and Spills</u>	<u>Activity Release</u>
Highly efficient sodium-air reaction in cell	Core meltdown
Sodium fire in cell (pool burning)	Stack release
Sodium spill in containment structure	Ground release
	Total integrated doses
	Direct gamma dose
	Single subassembly meltdown
	<u>Nuclear Instrumentation</u>
	Circuitry
	Minimum count rate

II. THE MAXIMUM CREDIBLE ACCIDENT AND ITS ASSOCIATED EFFECTS

A. MCA Description and Evaluation

The maximum credible accident for FARET is identified with the zoned core. It may result from either of two independent causes. These causes could be a significant loss of reactor cooling which results in gravitational collapse of the molten core or the dropping of a fuel subassembly into the core. Either of these accidents with the zoned core conceivably could, though not necessarily would, lead to an incident which could possibly approach in severity an explosive energy release equivalent to about 25 to 30 pounds of TNT.

An accident such as this could conceivably lead to the destruction of the reactor vessel, lifting of the reactor vessel cover, generation of small miscellaneous missiles, ejection of sodium into the cell, and release of fission products from the fuel to the cell. The vessel cover might possibly strike the cell ceiling liner but it would not penetrate the liner. Small missiles are not considered serious. The transient pressure differential created by the accident could possibly cause leakage of fission products from the cell to the building and a fraction of this material might be released via the stack. Even if all these conditions should exist as a result of an accident, the combined effect would not pose an unacceptable hazard to the NRTS site or the general public.

As previously stated, the accidents associated with the zoned core give the largest energy yields. A maximum energy yield could result from an initial reactivity insertion rate of about \$20/sec (with no negative feedback). The dropping of a fuel subassembly under very unlikely circumstances could result in such an insertion rate. At a low initial power, a maximum inverse power period of $1 \times 10^{-3}/\mu\text{sec}$ was calculated. The energy yield available to do work was estimated to be 6×10^7 joules. This is the high-explosive equivalent of about 30 lb of TNT, assuming an equivalence factor of 2.1×10^6 joules/lb TNT. In this case, the high rate of insertion did not result in significant core collapse before disassembly terminated the accident. A lower initial insertion rate increased the contribution to the reactivity insertion from core collapse. However, the inverse power period at the time of disassembly and the energy yield did not increase.

A reactivity insertion resulting only from the gravitational collapse of a molten core was also studied. This could be initiated by the partial loss of coolant or coolant flowrate. The energy yield was found to be approximately 5×10^7 joules, which corresponds to a maximum inverse power period of about $2 \times 10^{-3}/\mu\text{sec}$. This was obtained by introducing a "slow power rise" having a period of $1/\alpha = 110$ msec before melting started. Subsequent computations where the initial power periods were increased to several seconds resulted in a similar period and energy yield.

The model used in the FARET safety analysis considers that the collapse of a molten core causes a positive reactivity insertion during the early stages of collapse. It is during this phase that a "continuous reactivity" model is considered appropriate. This approach provides less pessimistic energy yields than have been reported in the safety analysis of the EBR-II and Fermi reactors. However, the analyses for these systems were concerned with defining extreme upper limits for determination of maximum hypothetical accidents. By contrast, the FARET safety analysis is primarily concerned with defining a maximum credible accident.

In determining the reactivity insertion due to core collapse, the assumption was made that the whole of the core collapsed. This is an overly pessimistic condition. If the realistic reactor power and temperature distribution are taken into account, only a fraction of the core would be expected to be involved in the excursion. This should result in reduced energy yields.

The probability of the postulated MCA occurring will be influenced significantly by the design and operation of the facility. To initiate a nuclear accident, several unusual and coincident conditions must prevail. For the situation to proceed to serious proportions would require the simultaneous malfunction of many design safety features, the loss of normally operative physical deterrents and, in most cases, the coincident loss of administrative control.

For example, to provide the proper circumstances to initiate an accident caused by the dropping of a fuel subassembly, it is necessary to postulate that:

- (1) The reactor is subcritical by less than the worth of one fuel subassembly.
- (2) A gross loading error is made.
- (3) The subassembly is located and aligned precisely above, or in, the proper, empty, subassembly location.
- (4) There is a gross malfunction or failure of the fuel-handling machine, or a gross failure of the fuel subassembly.
- (5) The subassembly achieves a substantial free-fall velocity.
- (6) There is complete loss of administrative control.

Similarly, in the case of the loss of coolant only from the test zone of the zoned core it is necessary to postulate that:

- (1) There is a total blockage of coolant to the entire test zone only,
or,

- (2) Overheating of the test zone occurs with resultant boiling and expulsion of the coolant from the test zone only.
- (3) The control drive mechanisms fail to scram and shut down the reactor.
- (4) No other shutdown mechanism is available.

That these events will occur in the proper coincidence or sequence is highly unlikely.

The manner of operation and use of the facility will also have a significant affect upon the probability of an incident occurring. The facility is expected to accommodate a variety of experiments. Each experiment will be carefully planned and its safety aspects meticulously analyzed. Critical experiments, laboratory tests, and other verification techniques will be used, as necessary, to ensure the safety of each proposed experiment. Subsequent reactor operations will be designed to limit the available reactivity and accidental reactivity input rate. With any reasonable inherent shutdown mechanism, the reactor should not go above prompt critical and no explosive energy release should occur.

B. Core Meltdown Leading to an MCA

1. Introduction

Studies have been made of both the reactor kinetics for a variety of positive reactivity insertion rates, and the effects of temperature-dependent negative reactivity feedbacks on a series of accidents which may lead to core meltdown. These accidents were assumed to be terminated only by disassembly of the core. The study of core meltdown conditions was made to obtain a more realistic estimate of the energy yield.

FARET is designed to accommodate a variety of fast reactor configurations, compositions, sizes, and experiments. However, the maximum credible accident would occur with a zoned core. The analysis described in this section pertains to a zoned core. Other lesser accidents which were also analyzed are described elsewhere in this report as are the two reference cores. (See Section III, "Other Analyses" and Appendix A-- "Reference Cores.")

The range of external reactivity insertion rates studied for this system was considered to be associated with credible or near-credible conditions for FARET. For example, the ramp insertions include dropping a fuel subassembly, unplanned removal of a control rod, and expulsion, or sudden loss of sodium coolant. Small reactivity insertions which result in a slow power rise until the fuel eventually melts and collapses were also analyzed. These conditions could be associated with possible startup accidents as well as loss of bulk coolant or coolant flow.

The estimated credible yields of energy available to do work are much smaller than the "hypothetical" energy yields reported in the safety analysis of the EBR-II and Fermi reactors (but they are larger than those considered credible for these facilities) for two reasons:

(1) Part of the reduction in energy yield arises from the use of the "continuous reactivity model" rather than the "reactivity threshold model." The latter model is usually used to establish upper limits for very pessimistic sets of initial circumstances. Such accidents are usually considered "incredible." The FARET analysis utilizes more realistic initial conditions, but the results are classed as "credible."

(2) The second factor which contributes to a lower estimated energy yield is the 10% efficiency factor used to determine the amount of total energy available to do work. Although further study is needed in the area of nuclear and high explosive energy equivalence with respect to fast reactor excursions, the 10% efficiency factor is believed to be conservative and is probably an upper limit for the efficiency of this process.

The 10 per cent efficiency factor was based on the computation of energy yield using the AX-1 code. This program is basically a numerical computation of the Bethe-Tait explosion analysis. However, a most important modification is the determination of the displacement of material. This provides a computation of the kinetic energy as well as the volume-integrated pressure. Either or both of these quantities can be taken as a measure of the total heat energy being converted into mechanical energy.

As indicated by the analysis in the following paragraphs, the kinetic energy and volume-integrated pressure were investigated for the zoned system having an inverse power period, α , of $0.01/\mu\text{sec}$. This is a factor of five greater than the value related to the maximum credible accident. For this case the percentage of heat energy going into kinetic energy was found to be less than one per cent. Since the heat energy converted into kinetic energy increases rapidly as α increased (Fig. 1), a kinetic energy fraction of 0.01 should be conservative.

The volume integrated pressure was estimated to be less than 8% of the total heat energy.

The efficiency factor was taken to be the sum of these two quantities, i.e., the kinetic energy and the volume integrated pressure. This "efficiency factor" was applied to the case for maximum energy yields corresponding to the inverse power periods of $\alpha = 2.0 \times 10^{-3}/\mu\text{sec}$.

Fig. 1. Heat Energy Converted to Kinetic Energy

Some experimental evidence which relates to this matter indicates that the value determined by this approach is a reasonable one. Scaling studies¹ indicate a thermal to mechanical energy conversion of about 4 to 8 per cent. The results of the SPERT-I Test Program² report the fraction of thermal energy converted into mechanical energy was less than one per cent.

2. Analytical Procedures

The following is a brief description of the procedures and computer codes employed in the analysis. A more detailed description is given in Appendix C, "Core Meltdown Studies and Energy Yields."

The analysis was separated into two independent phases: core disassembly and reactivity insertion.

In the disassembly phase, the estimate of total energy yield and explosive force was made with the aid of the AX-1 Code.³ In addition to the usual reactor parameters and equations of state, the input data must include

the inverse power period at the start of core disassembly. The energy yield is very sensitive to this parameter which, in turn, is dependent upon the reactivity insertion rate during the initial stages of disassembly.

In the second phase the maximum inverse power period was estimated for realistic initial reactivity insertion rates. In this respect, the reactivity insertion eventually resulting from core melting was based on the model in which the core collapses under gravity in a time- and space-wise continuous manner. It was further assumed that as the core collapses, fuel is redistributed over the new configuration by displacing the coolant.

This analysis is a departure from the "reactivity threshold" model, which assumes that approximately the upper half of a molten core drops as a single unit into a somewhat more densified lower core region. This most unlikely and extreme situation has been used in the studies of nuclear accidents in fast reactors,^{4,5} although the authors do not necessarily give credence to such a model.

The degree and the amount of core which densifies and reassembles as indicated above is questionable, and counter arguments as to the credence of this model have been advanced by Bethe.⁶ The more reasonable situation is advanced in which the fuel as it is molten and comes in contact with sodium will vaporize the sodium which, in turn, will expell the molten fuel. This probably inhibits any extensive, abnormally high concentration of fuel from forming in the lower portion of the core. It appears to be a reasonable assignment of events for the case of rapid power rises where the fuel becomes molten in tenths of milliseconds before expelling the coolant.

The situation differs in the case of a slow power rise where the coolant is boiled off before the fuel melting process begins. However, the credibility of the "reactivity threshold" model remains questionable for the following reasons. To rearrange the core and create conditions for the "reactivity threshold" model to be valid would probably require more than 100 msec. The time required for complete meltdown of the fuel is of the order of tens of milliseconds, even for the slow power rise. During the intervening time between melting in situ and rearrangement of core configuration, it can be argued that some reactivity is inserted by collapse under gravity. This would lead to power periods of several milliseconds. With such periods, the molten fuel could vaporize and create pressures within tens of milliseconds which would tend to inhibit the formation of the extremely pessimistic geometrical configuration suggested by the "reactivity threshold" model.

The assertion made here is that the collapsing of a molten core would result in a positive reactivity insertion during the early stages of collapse. It is during this phase that a "continuous reactivity" model would

appear to be more appropriate. For conceivable realistic accidents which could result in a partial core meltdown, this model, at present, is considered to be more appropriate for the purpose of estimating the energy yield.

In the FARET analysis, the collapsing of the core was assumed to proceed until the core started to disassemble from vaporization. Total energy input reached a value of 2×10^4 joules/cc of fuel. This was taken as the energy required to vaporize the fuel. The inverse power period corresponding to this energy for the particular accident being considered was then used as the input parameter for the computation of the energy yield (AX-1 Code). This approach affords a more detailed evaluation of core conditions and parameters which may exist during meltdown and reassembly, and thus provides a more reasonable estimate of the energy yield.

3. Energy Available to Do Work

The energy yield pertinent to the FARET safety analysis is defined as the amount of the total energy available which is converted into energy to do work. This quantity was obtained by first determining the total energy yield for a given inverse power period, and multiplying this quantity by the factor: $(0.60) \times (0.10)$.

The first term (0.60) is based on a slightly less pessimistic equation of state used in the disassembly code. (See Appendix C.) This essentially allows a slightly more dense configuration at the time of disassembly, or equivalently slightly lower temperatures at which disassembly starts.

The second term is an estimate that a reactor system is only 10% efficient in converting the total heat energy into mechanical energy. The fraction of the total energy which is available for work includes the kinetic energy and the volume-integrated pressure at the end of the excursion. From the AX-1 computation, an estimate of this quantity was made for the zoned system, using the overly pessimistic inverse power period of $\alpha = 0.01/\mu\text{sec}$. For this case, the maximum energy available to do work was computed to be about 8% of the total energy yield. Since further analytical work is required to establish such a fraction more firmly, the 10% efficiency factor was used to convert the total energy into mechanical energy.

The remainder of the total energy yield in the nuclear burst is dissipated as heat energy which raises the temperature level of the system. Based upon a uniform distribution of this heat energy to the reactor vessel and its contents only, the temperature rise of these components should not exceed 2°F per each 10^7 cal of energy released, e.g., for an energy release of 2×10^8 cal (400 lb of TNT), a uniform temperature rise of 40°F or less is estimated.

It should be noted that the maximum credible accident associated with FARET corresponds to an inverse period of about $0.002/\mu\text{sec}$, used in computing the efficiency factor. As the severity of the excursion decreases, the fraction of energy available to do work is expected to decrease significantly.

4. Methods of Reactivity Insertion and Excursion Results

a. Loss of Coolant

The total loss of coolant flowrate to, or loss of coolant from, the reactor vessel is considered unlikely because of the design safety features previously described. However, other mechanisms could effect a rapid loss of coolant from certain areas of the core. For example, the expulsion of coolant by boiling from the test section of the zoned core, or, possibly, the sudden blockage of sodium to a single subassembly.

Loss of coolant could also lead to a slow rise in power and eventual collapsing of the core after melting starts. To estimate the energy yield for accidents of this type where the power period is increased only due to the core collapse, a very low inverse power period was introduced initially. The power increased very slowly until melting started, at which time the reactivity increase resulted from the collapse of the core. Negative feedback was assumed to be inoperable.

In the zoned core, at an energy input of 2×10^4 joules/cc of fuel, the inverse power period did not exceed $2 \times 10^{-3} \mu\text{sec}$. The energy available to do work was estimated at 5×10^7 joules for the accident resulting from sudden loss of coolant.

b. Dropping of Fuel Subassembly into Zoned Core

Based on reasonable heights from which a subassembly might be dropped and undergo free fall (~ 6 ft above the core boundary), the rates of fuel insertion into the core region could range up to about 10 ft/sec.

The reactivity insertion rates associated with this type of accident are considered to be pessimistic for several reasons. First, the inadvertent addition of reactivity will not be significant unless the loading is near critical. Since the reactor normally will be subcritical by the worth of several subassemblies during loading, a gross loading error must occur. Secondly, the normal loading procedure will prescribe sequential placement of fuel subassemblies from regions of high worth to those of low worth. Finally, if a subassembly is inadvertently dropped, it will fall into a compacted array of other subassemblies; hence, it would be highly unlikely that a 16-ft element would attain the average velocity due to free fall. The ramp insertion rates studied ranged up to \$20/sec. The corresponding energy available to do work is estimated to be 5×10^7 joules.

C. Pressure and Temperature in Containment Following the MCA

1. The Problem and Method of Solution

The following situation was analyzed with the assistance of Code RE-327-X. The code is used to calculate containment volume pressure, containment liner temperature, and total integrated leakage from the containment. The analytical model assumes that at time zero, any given weight of sodium at any given temperature is ejected into the containment, carrying along with it the entire fission product inventory. The decay heat of the fission products and sensible heat of the sodium are added directly to the gas. Fission product decay heat is assumed to be reduced in time due to leakage from the containment region as well as decay of the fission products.

The postulated sequence of events is as follows:

(1) Core partially melts--mechanism for initiating the meltdown is not important in this analysis.

(2) Fission products and plutonium are released into the sodium above the core.

(3) The fission product and plutonium-laden sodium is ejected into the argon-filled cell--particle size and dispersal pattern of the sodium are not pertinent to the ensuing calculations.

(4) Due to the addition of heat to the cell argon from the sensible heat of the sodium and the decay heat of the fission products, the pressure rises in the cell and subsequently in the vault after the low pressure barrier between vault and cell ruptures.

(5) The leakage of argon from the containment is accompanied by leakage of fission products.

(6) The fission products escaping to the building are exhausted to the atmosphere via the ventilation stack.

A transient heat balance is performed on the gas, containment liner, and concrete. In the case of the cell only, the steel webs and outer liner which provide the structural strength of the cell are also considered in the heat balance. The webs and concrete are assumed to transport heat with equal temperature gradients, i.e., a composite parallel conductance is taken equal to the sum of the "UA" individual conductances of the webs and concrete. The heat capacities of steel and concrete are also lumped. For conservatism, the expected concrete shrinkage away from the liner is assumed to be entirely on the inner (or hot) face. Heat is ultimately dissipated through the containment structure to the environment which is assumed to remain constant in temperature.

Due to the poor thermal diffusivity of concrete, the walls are broken into 3-in.-thick nodes for the heat transfer calculation. This results in twenty-five simultaneous, first order differential equations which are solved using a subroutine which employs a Runge-Kutta solution.

2. Input Parameters

For the purpose of the analysis, the following values were used in the computation:

- A. Surface area inside containment = 26,890 ft²
- B. Surface area outside containment = 33,420 ft²
- C. Area of steel webs = 223 ft²
- D. Weight of sodium ejected = 15,000 lbs
- E. Sodium temperature = 1000°F
- F. Ambient temperature = 100°F
- G. Containment volume = 100,000 ft³
- H. Containment wall thickness = 5 ft
- I. Heat transfer coefficients, Btu/hr-ft²-°F
 - (a) Argon to cell and/or vault steel--1.0
 - (b) Sodium to argon--2.0
 - (c) Sodium to steel floor--100
 - (d) Containment surface to ambient--1/2
- J. Fission product decay heat based on infinite reactor operation at 50 MWt
- K. Cell floor thickness area--1 in.
- L. Thickness of containment liners--3/4 in.

3. Results

In order to identify maximum possible temperatures, pressure and total integrated leakage, three problems using different leakage rates were solved. These leakage rates were:

- (a) Zero
- (b) 0.6 w/o/day at 30 psig
- (c) 30 w/o/day at 30 psig.

In all cases, the mass leakage rate was assumed proportional to the over-pressure raised to the 1/2 power.

(a) Zero

Figure 2 gives the containment pressure and temperatures of the inner and outer cell liners. This zero leakage case yields upper limits on pressure and temperature. A heat balance on the containment gas gives rise to the differential equation:

$$(mc)_g \frac{dT_g}{dt} = Q(1 - L) - kT_g + \dots, \quad (1)$$

where

T_g = gas temperature

$(mc)_g$ = heat capacity of gas at time t

Q = heat input from fission products at $t = 0$

L = total integrated leakage fraction at $t = t$

k = constant.

Fig. 2. Post-MCA Conditions for Zero Leakage

Over a period of time the two terms on the right yield the only significant contributions to dT_g/dt . Now, note that $(mc)_g = (mc)_{g,0}(1 - L)$ where the subscript "0" signifies initial value. Therefore, rearranging Eq. (1) we get

$$\frac{dT_g}{dt} = \frac{Q}{(mc)_{g,0}} - \frac{kT_g}{(mc)_{g,0}(1 - L)} + \dots \quad (2)$$

The positive contribution to dT_g/dt is independent of leakage whereas the negative contribution varies inversely as leakage. The net effect of decreasing the leakage, therefore, is to make dT_g/dt more positive and hence to develop higher gas temperatures and pressures relative to a case with a higher leakage fraction.

The values for pressure and inner liner temperature peak is about 4×10^6 seconds (about 46 days) at 4.3 psi and 277°F, respectively.

(b) 0.6 w/o/day at 30 psig

The design leakage rate of the FARET containment structure is 0.6 w/o of the containment volume per day at 30 psig. Figure 3 shows two pressure peaks of about the same magnitude, the first being 3.5 psi and the

Fig. 3. Post-MCA Conditions for Low Leakage

second 3.26 psi which occurs at about 300 sec and about 1.7×10^6 sec (about 20 days), respectively. After 2.3×10^6 sec (about 27 days) the pressure is down to 3.2 psi and the inner liner temperature which is still increasing is 263°F. The maximum liner temperature should be less than 277°F as calculated for the zero leakage case above.* The total integrated leakage is about 5 per cent.

(c) 30 w/o/day at 30 psig

This leakage rate represents the guaranteed leakage rate of the FARET containment. While the calculated temperatures and pressure are lower than for (a) and (b) above, the total integrated leakage is much greater--on the pessimistic side.

Figure 4 shows the containment pressure, the temperatures of the inner and outer cell liners and the total integrated leakage from the containment. The peak pressure occurs shortly after the accident and is only 3.5 psi. The high leakage rate causes the pressure to decrease thereafter. After 10^6 sec (about 10 days), the total integrated leakage is about 20 per cent and the overpressure almost zero. The inner liner temperature is 213°F.

Fig. 4. Post-MCA Conditions for Guaranteed Maximum Leakage

* Ed. Note. Subsequent calculations yield a maximum inner liner temperature of about 268°F.

D. Evaluation of Containment Structure

The FARET containment structure consists primarily of three, reinforced concrete-shielded enclosures: cell, cavity, and vault. Surrounding this structure is the reactor building, which provides still another envelope. The structure alone is designed to adequately absorb energy released by the MCA and subsequently maintain containment as necessary to assure no more leakage than the guaranteed maximum rate.

1. Summary

The analysis of the FARET structure (Appendix B) is based on a sudden energy release equivalent to the detonation of a 40-lb bare charge of TNT. The analytical model used indicates that the short-term energy release of a TNT-type detonation within the core coolant would generate quickly, in sequential order: (1) a shock wave; (2) coolant momentum due to the shock wave; and (3) a high-temperature and high-pressure gas bubble which would push on the surrounding media. The released energy is absorbed primarily by mechanical strain in structures in the immediate vicinity of the core, by the heating of these structures plus the gases which result in containment overpressure, and finally if the release is severe enough, by missiles which are ultimately stopped by the containment structure. It is concluded that the energy absorption capabilities of the FARET containment are significantly greater than the energy release equivalent to the detonation of a 40-lb bare charge of TNT at the center of the core. It is further concluded that adequate containment can be guaranteed by the structure during the period of overpressure after the blast.

2. Guaranteed Maximum Leak Rate

The guaranteed maximum leakage rate is 30 w/o of the contained atmosphere or less in 24 hr. This rate is at the full design pressure of 30 psi. The design criteria for leakage rate from the containment structure is set at a factor of 50 lower than the guaranteed maximum leakage rate. By the contained atmosphere is meant the combined gas volume in the cell, cell annex, vault, and cavity.

E. Release of Fission Products

In order to assess the effects on the surrounding population of an accidental release of fission product activity, it is necessary to make several assumptions regarding the fuel meltdown and fission product leakage fractions through the various containment barriers. A fuel meltdown fraction of 50% was used. The assumed fission product leakage fractions for iodine and solids from each barrier are listed in Table 2.

TABLE 2. Assumed Fractional Release of Fission Products

Barrier	Iodine	Solids
Melted Fuel	0.5	0.01
Sodium	0.02	1.0
Containment Atmosphere*	$0.5 L_t$	$0.1 L_t$
Building Atmosphere	0.5	0.1
AEC Filters	1.0	0.001
Total (L_B):	$2.5 L_t \times 10^{-3}$	$L_t \times 10^{-7}$

* L_t is total containment leakage fraction [see Eq. (3)].

The release fractions for iodine from sodium were based upon recent experimental information.⁸⁻¹⁰ The referenced sources indicate that release values for iodine from sodium as low as 0.0002 may be considered. This value has been increased by a factor of 100 to account for differences between experimental conditions and those that may prevail in FARET during an accident. There is, of course, no data from experiments that duplicate the FARET conditions. However, experiments are planned to investigate the release of iodine fission products into a sodium vapor. Results of these experiments are expected to be applicable to FARET. This subject will be covered in the final safety analysis report.

The release fractions of iodine from the building spaces, i.e., the containment and the building atmospheres, are based upon recommendations in TID-14844.⁸

The combined release fraction of solids from the containment and building atmospheres is assumed to be $0.01 L_t$ ($0.1 L_t \times 0.1$) and is based upon the following considerations and judgments. In order for particulate matter to escape from the containment, it must traverse rather tortuous leakage paths such as through the 5-ft thick concrete cell wall or through shielding window or penetration seals. These paths would consist of high- and/or very low-velocity regions with large available surface areas. Precipitation or plating out of the particulate matter within these leakage paths is expected to decrease substantially the quantity of solids available for release from the containment. Some settling or plating out of solids within the containment is also expected. Within the building, the air velocity is so low that considerable settling of particulate matter is expected, e.g., the building space is $\sim 10^6 \text{ ft}^3$ as compared to an exhaust rate of 10^4 cfm through the stack. In addition to these considerations, many of the fission products are chemically very active. For example, strontium, which accounts for about 50% of the bone dose, is higher than most elements in the electromotive

force series. Consequently, it is expected that strontium would reduce virtually all elements in oxide form, resulting in the formation of strontium oxide. This would decrease the amount of strontium available for release from the stack.

Plutonium is expected to be released in the same manner and proportion as solid fission products. Experimental results of dispersion accompanying plutonium combustion are summarized in Ref. 11 and some are briefly discussed below.

Carter et al.¹² investigated the dispersion of plutonium oxide following the combustion of plutonium cylinders and turnings. They estimate that even in a turbulent air stream, only 5×10^{-4} of the original plutonium burned would be available as an inhalation hazard.

Plutonium burning tests reported by Cheever¹³ indicate that the maximum fraction of burned plutonium collected on filters, and hence presumed to have been airborne, was 1.2×10^{-5} .

Hilliard,¹⁴ in a summary of references concerned with the dispersion of plutonium oxide following the combustion of plutonium, concludes that evaporation cannot make a significant contribution to the amount of plutonium dispersed at temperatures up to 1350°C. He further concludes that only about 8×10^{-4} of the original plutonium might result in an inhalation hazard, i.e., remain airborne.

Finally, when an explosion accompanied combustion of plutonium in a 1959 incident at Oak Ridge National Laboratory,¹⁵ the door of the building which housed the shielded cell in which the explosion took place was forced open. The estimated peak pressure was 1100 psig. Even in this instance, only about 5×10^{-4} of plutonium originally present escaped to the outside. Subsequent fallout was rapid and confined to a small area of the site.

In view of the above evidence, a total release fraction of 0.01 L_t from both the containment and building atmospheres is considered to be suitably conservative for both plutonium and solid fission products.

In designating a release fraction for absolute filters, the primary considerations are based on initial testing and periodic surveillance. All filters purchased for NRTS use are subject to a stringent specification.¹³ All filters are tested at Hanford and must have a minimum efficiency of 99.97% for 0.3 micron diameter particles of dioctylphthalate (DOP), tested in accordance with Military Standard 282, Method 102-91, dated May 28, 1956. Following the Hanford tests and on arrival at NRTS, all filters are visually inspected for tears or other signs of damage. Filters are rejected if damage is apparent.

Following installation in Laboratory facilities, all reactor building exhaust filters are tested using a portable DOP aerosol generator which produces particles having a mean diameter of 0.9 micron. Filter efficiencies of 99.9% can be measured.¹⁴ Subsequent to initial installation, the filters are routinely tested in place semiannually. Based upon these tests and operating procedures, the release fraction in Table 2 is 0.001.

Combination of the release fractions of Table 2 with the 50% melt-down fraction gives a total fractional release (L_B) of $1.25 L_t \times 10^{-3}$ and $5 L_t \times 10^{-8}$ for iodine and solids, respectively. The computed doses in this section may be modified in direct proportion to any release fraction(s) in Table 2, if a different calculational model were to be used.

1. Core Fission Product Inventory

The amounts of important fission products present in the core were obtained from data of Burnett.¹⁸ The isotopes and their activities considered in this study are listed in Table 3. Some of the concentrations are pessimistic, since 400 days of irradiation at 50 MW is about a factor of 4 greater than anticipated.

TABLE 3. Fission Product Inventory after
400 days of Irradiation at 50 MW
(Data from Refs. 18 and 19)

Isotope	Curies Present ($\times 10^{-6}$)	D_i mRem/ μ C Inhaled ^a	Critical Organ
I ¹³¹	1.3	1476	Thyroid
I ¹³²	2.0	56.1	Thyroid
I ¹³³	3.0	392	Thyroid
I ¹³⁴	3.5	27.1	Thyroid
I ¹³⁵	2.7	108	Thyroid
Sr ⁸⁹	2.2	413	Bone
Sr ⁹⁰ - Y ⁹⁰	0.073	44000	Bone
Ba ¹⁴⁰ - La ¹⁴⁰	2.9	94.2	Bone
Ce ¹⁴⁴ - Pr ¹⁴⁴	1.74	1212	Bone
Ce ¹⁴¹	2.72	22.8	Bone
Y ⁹¹	2.7	328	Bone
Xe ¹³⁵	2.6	14.6 ^b	Bone
Kr ⁸⁹	0.77	413 ^c	Bone

^aSee Eq. (8).

^bAs Cs¹³⁵.

^cAs Sr⁸⁹.

2. Containment Leakage Fraction

To determine the quantity of fission products leaking from the containment to the building under the influence of an elevated pressure, it is assumed that:

$$L = K(\Delta P)^{1/2} \quad (1)$$

where

L = leak rate from containment per day

ΔP = pressure differential between containment and building, psi

K = proportionality constant, $(\text{day})^{-1}(\text{psi})^{-1/2}$

For the guaranteed maximum leakage rate of the containment of 30%/day at 30 psi differential, $K = 0.055$, and

$$L = 0.055(\Delta P)^{1/2} \quad (2)$$

Now

$$L_t = \int_0^t L \, dt = \int_0^t 0.055(\Delta P)^{1/2} \, dt \quad (3)$$

where L_t is the total fraction of containment volume which leaks out in t days and is directly proportional to $(\Delta P)^{1/2}$.

The solution of

$$\int_0^t (\Delta P)^{1/2} \, dt$$

is obtained by integration of the calculated pressure-time curve of the containment space after an accident. The curves in Figs. 2, 3, and 4 represent situations that might prevail as a consequence of sodium ejection into the containment.

3. Atmospheric Diffusion

Sutton's equation for atmospheric diffusion was employed to compute the downwind distance at which the ground level concentration is

maximum.²⁰ That distance in meters is

$$d_{\max} = \left(\frac{h^2}{C_z} \right)^{1/(2-n)} \quad (4)$$

where

h = stack height, m

C_z = vertical diffusion coefficient, $m^{n/2}$

n = Sutton's stability parameter, dimensionless.

The concentration at $d_{\max}$ is given by:

$$\left(\frac{X}{Q} \right)_{\max} = \frac{2C_z}{e\pi\bar{u}h^2C_y} \quad (5)$$

where

X = concentration, $\mu C/m^3$

Q = release rate, $\mu C/sec$

$\bar{u}$ = wind velocity, m/sec

C_y = lateral diffusion coefficient, $m^{n/2}$.

Although the duration of release is greater than 30 min (see Figs. 3 and 4), the more conservative value of C_y is assumed, namely,

$$C_y = C_z \text{ [From Ref. (21)]}$$

and Eq. (5) becomes

$$\left(\frac{X}{Q} \right)_{\max} = \frac{2}{e\pi\bar{u}h^2} \quad (6)$$

Finally, with a stack height of 200 ft (60 m), diffusion parameters for strong inversion conditions are as follows:²¹

$$\bar{u} = 2.0$$

$$n = 0.5$$

$$C_z = 0.032.$$

Therefore,

$$\left(\frac{X}{Q}\right)_{\max} = 3.26 \times 10^{-5} \text{ sec/m}^3 \quad (7)$$

4. Inhalation Dose

(a) Thyroid and Bone

Burnett's data were also used to calculate the dose to the thyroid and bone due to inhalation of isotopes in the diffusing fission product cloud. The integrated dose was obtained as described below.

$$\text{TID}_i = D_i I_i \quad (8)$$

where

TID_i = dose in mRem due to isotope i

D_i = mRem/ μC of inhaled isotope i (from Table 3)

I_i = total μC of isotope i inhaled.

Assuming a breathing rate of $5 \times 10^{-4} \text{ m}^3/\text{sec}$,

$$I_i = 5 \times 10^{-4} \bar{Q}_i t \quad (9)$$

where

$\bar{Q}_i$ = ground level concentration of isotope i , $\mu\text{C}/\text{m}^3$

t = exposure time, sec.

But

$$\bar{Q}_i = X \text{ from Eq. (7)}$$

or

$$\bar{Q}_i = 3.26 \times 10^{-5} Q_i \quad (10)$$

where

Q_i = release rate of isotope i from stack, $\mu\text{C}/\text{sec}$.

The total amount of microcuries of isotope i released to the stack in time t is:

$$Q_i t = Q_{i,0} L_B \quad (11)$$

where

$Q_{i,0}$ = core inventory of fission product i , μC

L_B = total leakage fraction from all barriers (Table 2).

Finally,

$$TID_i = 1.63 \times 10^{-8} D_i Q_{i,0} L_B \quad (12)$$

(b) Plutonium

The reference Core I loading was used for purposes of calculating the effects of a plutonium release upon the surrounding population. This core is estimated to contain 100 kg of Pu^{239} . Concentrations of 15% Pu^{240} and 5% Pu^{241} in total plutonium were assumed. Equation (12) may be modified slightly to give the inhaled dose in terms of micrograms of each plutonium isotope. The modified equation is:

$$TID_{p_i} = 1.63 \times 10^{-8} Q_{p_i} L_B \quad (13)$$

where

TID_{p_i} = amount of plutonium isotope inhaled, μgm

Q_{p_i} = core inventory of plutonium isotope, μgm .

The other terms have been defined previously.

The maximum permissible body burdens of Pu^{239} , Pu^{240} and Pu^{241} are given in Tables 4 and 6. Although only a fraction of the plutonium inhaled is assimilated by the body, it was assumed that the maximum permissible intake was equal to the maximum permissible.

5. Direct Gamma Dose from Containment

In accordance with the criteria and computational methods set forth in Ref. 8, calculations were performed to determine the direct gamma dose due to fission product release into the containment. No credit was

taken for the shielding value of the cell or building. For this purpose, the exclusion radius was taken to be 4000 ft (1219 m), the distance to the EBR-II.

6. Results

(a) Inhalation Dose from Stack Release--Inversion Conditions

The inhalation dose from a stack release based upon a total cell leakage fraction of 0.054 was calculated. It was found that the ground level concentration is maximum at a point 14.4 miles (2.31×10^4 m) downwind of the reactor. Table 4 lists the inhalation doses for individuals standing at this point and at other locations on or near the NRTS for the assumed duration of the release.

TABLE 4. Total Integrated Inhalation Doses from Stack Release--Inversion Conditions^a
Total Containment Leakage Fraction (L_t) = 0.054

Location	Personnel Group Classification	Thyroid, Rem	Bone, Rem	Pu ²³⁹ , μ gm		Pu ²⁴⁰ , μ gm		Pu ²⁴¹ , μ gm	
				TID	Allowable	TID	Allowable	TID	Allowable
EBR-II	(1) or (2)	~0	~0	~0	0.029	~0	0.0054	~0	0.0018
US-20 (nearest approach)	(3)	2.3×10^{-4}	2.0×10^{-8}	2.7×10^{-10}	0.0095	5.2×10^{-11}	0.0018	1.7×10^{-11}	0.0006
Central Facilities	(1) or (2)	3.9	3.2×10^{-4}	4.4×10^{-6}	0.029	8.5×10^{-7}	0.0054	2.7×10^{-7}	0.0018
14.4 miles (2.31×10^4 m) downwind	(3)	3.9	3.2×10^{-4}	4.4×10^{-6}	0.0095	8.5×10^{-7}	0.0018	2.7×10^{-7}	0.0006

^aPu doses based on estimated Core I Pu²³⁸ loading of 100 kg.

The allowable plutonium inhalation doses in the table were calculated on the basis of the same plutonium isotope concentrations in the fission product cloud as in the core. Also, the following special categories of personnel were considered (Column 2):

(1) Adults working in the vicinity of a controlled area who are not employed to work in radiation fields.

(2) Adults who enter a radiation field occasionally.

(3) Members of the public living in the neighborhood of controlled areas.

For these calculations, all NRTS personnel were considered to belong to either group (1) or (2). The maximum permissible doses were taken as 0.3 times the allowable occupational doses for groups (1) and (2) and 0.1 times the allowable occupational doses for group (3).

The values do not allow for fission product decay within the building or during time of flight. Also, it was assumed that the wind persists in the direction of each location for the duration of the release. In any event, the doses are well below permissible limits.

(b) Inhalation Dose from Stack Release--Neutral Lapse Conditions

The total integrated doses at the EBR-II site for neutral lapse meteorological conditions are given in Table 5.

TABLE 5. TID at EBR-II for Neutral Lapse ($L_t = 0.054$)^a

Thyroid, Rem	Bone, Rem	Pu ²³⁹ , μgm	Pu ²⁴⁰ , μgm	Pu ²⁴¹ , μgm
1.6	1.3×10^{-4}	1.3×10^{-6}	2.4×10^{-7}	8.4×10^{-8}

^aPoint of maximum concentration at EBR-II.

(c) Inhalation Dose from Ground Release ($L_t = 0.054$)

Table 6 lists the total integrated inhalation doses from a ground release at the EBR-II site for both inversion and neutral lapse conditions.

TABLE 6. Total Integrated Inhalation Doses at EBR-II from Ground Level Activity Release
Total Containment Leakage Fraction (L_t) = 0.054

Meteorological Conditions	Thyroid, Rem	Bone, Rem	Pu ²³⁹ , μgm		Pu ²⁴⁰ , μgm		Pu ²⁴¹ , μgm	
			TID	Allowable	TID	Allowable	TID	Allowable
Inversion	726	58	0.82	0.029	0.15	0.0054	0.053	0.0018
Neutral Lapse	1.6	0.14	1.9×10^{-3}	0.029	3.6×10^{-4}	0.0054	1.2×10^{-4}	0.0018

(d) Whole-body Gamma Dose from Fission Product Cloud

The whole-body direct gamma doses due to passage of a fission product cloud at points of maximum concentration were calculated. For a total leakage fraction (L_t) of 1.0, the results were: (1) 23.6 rad at 14.4 miles downwind for inversion conditions, and (2) 9.4 rad at the EBR-II site for neutral lapse conditions.

(e) Direct Gamma Dose from Containment

The direct gamma dose for a 2-hr exposure at the EBR-II is 14 mR. This is based upon a fission product release into the reactor building, with no credit taken for shielding by the containment or building structure. The more credible condition would be the release of activity within the cell. In this case, the dose would be orders of magnitude lower.

(f) Dose from Fission Product Release from a Highly Irradiated Fuel Subassembly

The meltdown of a highly irradiated fuel subassembly during fuel handling would release fission products to the cell. Normal operation of

the facility would preclude the uncontrolled release of this activity to the environment. However, if certain extreme circumstances were to exist whereby release of this activity could be effected, the integrated dose at the EBR-II site would be undoubtedly well within allowable tolerances.

(g) Doses to FARET Operating Personnel

An estimate was made of the dose to operating personnel due to immersion in the fission product cloud within the building. It was assumed that one-half of the total noble gas inventory is released in a uniformly distributed manner to the building space and contributed to the external dose. Under the hypothetical conditions of a "puff" release to the building and no radioactive decay, the dose rate to an individual remaining in the building would be approximately 26 rad/sec.

The dose rates due to inhalation by operating personnel also have been estimated. The results listed in Table 7 are based upon a "puff" release into the building of the entire fission product inventory available for release from the cell, uniform dispersion within the building, and a breathing rate of 5×10^{-4} m/sec.

TABLE 7. Inhalation Dose Rates to FARET Personnel

Bone, Rem/sec	Thyroid, Rem/sec	Pu ²³⁹ , μgm/sec	Pu ²⁴⁰ , μgm/sec	Pu ²⁴¹ , μgm/sec
47	159	0.64	0.12	0.04

(h) Dry Deposition at EBR-II

The worst case of dry deposition at EBR-II occurs under inversion conditions. Table 8 lists the calculated maximum total integrated deposition for a total leakage fraction (L_t) of 0.054.

TABLE 8. Maximum Total Integrated Dry Deposition at EBR-II

Iodine, μC/m ²	Bone Seekers, μC/m ²	Pu ²³⁹ , μgm/m ²	Pu ²⁴⁰ , μgm/m ²	Pu ²⁴¹ , μgm/m ²
5600	0.11	6.6×10^{-4}	1.2×10^{-4}	4.2×10^{-5}

(i) Miscellaneous Considerations

The effects of Na²⁴, Kr⁸⁹, and Xe¹³⁵ were estimated, based upon a total leakage fraction (L_t) of 1.0. Under inversion conditions, the whole-body irradiation from Na²⁴ at the point of maximum concentration

should not exceed 0.08 mRem. The total integrated dose (TID) to the bone from that Sr^{89} which is released as Kr^{89} would be about 5 mRem, or less. The TID to the bone from that Cs^{135} which is released as Xe^{135} would be 0.14 mRem, or less.

(j) Dose as a Function of Distance

The anticipated inhalation dose at any point downwind of the FARET site can be determined from Fig. 5. The "F" values are normalized on the basis of the inhalation dose at Central Facilities. To determine the dose at a given location under inversion conditions, multiply the "F" factor by the inhalation dose at Central Facilities listed in Table 4.

Fig. 5. Relative Inhalation Doses Downwind

III. OTHER ANALYSES

A. Core Meltdowns

1. General

As stated previously with regard to the MCA, the reactor kinetics for a variety of positive reactivity insertion rates, and the effects from temperature-dependent negative reactivity feedbacks on a series of accidents which may lead to core meltdown, have been studied. These accidents were assumed to terminate only by disassembly of the core. The analytical procedures used for the accidents described in this section are the same type as used for the MCA described previously. The analysis is based on the two reference core loadings described in Appendix A. The results of this analysis are summarized in Table 9.

TABLE 9. Estimated Energy Available to Do Work
(in joules)

Type of Accident	Small Core	Zone Core
Loss of Coolant	-	5×10^{7a}
		5×10^{7c}
Unplanned Control Rod Removal	4×10^6	2.5×10^7
Dropping of Fuel Subassembly into the Core	4×10^6	5×10^7
Startup-type Accidents ^b		
Loss of Coolant	7×10^6	-
Unplanned Control Rod Removal	-	3×10^7
Dropping of Fuel Subassembly into the Core	-	6×10^{7c}

^a Test zone only.

^b Low initial power level.

^c Maximum credible accident.

2. Methods of Reactivity Insertion and Excursion Results

Reactivity changes in the reactor could be promoted by various mechanisms. For example:

- (1) Loss of coolant.
- (2) Unplanned removal of control rod.
- (3) Dropping of a fuel subassembly.
- (4) Startup-type accidents.

- (5) Structural changes.
- (6) Change in system temperature, e.g., introduction of cold sodium into the reactor.
- (7) Introduction of moderator.
- (8) Failure of experimental subassembly.

Each of these mechanisms could potentially cause a core meltdown. Experience suggests that certain of these mechanisms are of particular concern during the early stages of reactor design. The first four mechanisms are in this category, and are discussed in this document. It is planned to consider mechanisms (5) to (8) in detail in the final safety analysis.

a. Loss of Coolant

In addition to the MCA resulting from loss of coolant with the zoned core, there is another coolant loss type accident of some interest related to the zoned core. This involves loss of coolant from the test zone only. This is very improbable, unless a mechanism particularly related to the specific zone is operative, e.g., boiling. However, because the sodium coefficient in the test zone of the zoned system is estimated to be positive, this condition has been evaluated. The expulsion of the sodium from the test zone could result in a maximum reactivity insertion rate of \$20/sec provided the sodium was expelled in 30 msec. In the event of a positive ramp insertion of this magnitude, the energy available to do work would be about 5×10^7 joules.

b. Unplanned Removal of Control Rod

The unplanned removal of a control rod could result in excessive introduction of reactivity, provided the reactor was subcritical by less than the worth of a control rod. The modes of unplanned removal of a control rod considered were: (1) malfunction of the control rod drives; (2) malfunction of the fuel-handling machine; or (3) manual means. The speeds of the control rod drives and fuel-handling machine are inherently limited so that excessive rates of reactivity insertion cannot be inserted by erroneous actions. Accordingly, the investigation of the unplanned removal of a control rod was limited to studies of rod removal rates that can be obtained manually.

It has been demonstrated that the manual withdrawal of a control rod, with maximum effort by two men, could be as great as 3.3 in. in 45 msec, or approximately 6 ft/sec.²²

(1) Small Core: For the small core, the maximum external reactivity insertion rate is expected to be \$10/sec, or less. However, with a temperature-dependent negative feedback, the maximum energy yield occurs when the external insertion rate is \$3/sec. The energy available to do work in this case is 4×10^6 joules. This corresponds to an inverse power period of $1 \times 10^{-3}/\mu\text{sec}$.

(2) Zoned Core: In the zoned core, a \$2/sec external reactivity insertion rate is estimated. Neglecting any negative feedback, the energy available to do work in this case is 2.5×10^7 joules.

c. Dropping of Fuel Subassembly into Core

Dropping of a fuel subassembly into the zoned core was considered with regard to the MCA. This can also happen to a small core. Under these conditions an external reactivity insertion rate of \$20/sec would result in a work energy release of 4×10^6 joules. The inverse power period of $1 \times 10^{-3} \mu\text{sec}$ was the maximum computed for a variety of ramp insertions.

d. Startup-type Accidents

Startup-type accidents, such as dropping of a fuel subassembly or unplanned removal of a control rod, would occur at low initial power levels. The results indicate that an increase of about 70% in the inverse period can be expected at the time of core disassembly (See Table 18, Appendix B). The estimated energy yields are as follows:

(1) Loss of Coolant from Small Core: At low initial power levels, an inverse period of about 3 to $4 \times 10^{-3}/\mu\text{sec}$ was attained when the total energy input of 2×10^4 joules/cc of fuel was reached. The energy available to do work was estimated at 7×10^6 joules.

(2) Unplanned Removal of Control Rod from Zoned Core: The rapid, unplanned removal of a control rod (\$10/sec ramp) at low initial power is estimated to yield a work energy of 3×10^7 joules.

(3) Dropping a Fuel Subassembly into Zoned Core: A work energy yield of 6×10^7 joules is expected from a \$20/sec ramp insertion.

B. Fuel Handling and Storage Accidents

1. Reactor Vessel Cover Removal

Since the control rod drive mechanisms will be integral with the reactor vessel cover, lifting of the cover preparatory to fuel-handling operations will pose the potential hazard of unplanned simultaneous removal of one or more control subassemblies. Accordingly, safety devices will be installed to preclude substantial raising of the cover unless all control rods are disconnected from their drives.

The details of the cover-lifting procedure have not been established, but it is expected that a procedure similar to the following will be employed.

- a) The control subassemblies will initially be fully inserted into the core and released.
- b) The control rod drives will be raised by the lifting platform at a rate-limited speed.
- c) Any increase in reactivity will be cause for reversal of motion.
- d) A final check by suitable interlocks will be made to ensure that all drives are disconnected.
- e) The cover will be raised to expose the core for fuel-handling operations.

2. Power Loss during Fuel Handling

The fuel-handling machine includes an electric-powered cooling system to dissipate the decay heat generated in spent fuel during transport to the wet fuel storage tank. In the event of failure of the main electric power system, the cooling system will be energized by the emergency power system. If both power systems fail simultaneously, loss of cooling will occur with consequent meltdown of the fuel subassembly and escape of fission products to the cell atmosphere.

Subsequent retention and/or release of these fission products from the cell would be effected in a controlled manner. No serious hazard beyond the confines of the FARET Facility is anticipated.

3. Jamming of a Spent Fuel Subassembly

During withdrawal of a spent fuel subassembly from the reactor, it is conceivable that the subassembly could become lodged, or its motion arrested, with the active fuel region above the sodium level in the vessel. This could be caused by jamming of the subassembly in the core or by malfunction of the fuel-handling machine. In either case, decay heating of the subassembly could result in a meltdown and some, or all, of the fuel might re-enter the reactor under force of gravity.

The consequences of this situation cannot be predicted with any degree of confidence. However, it is not expected that the melted fuel would enter the core in a more reactive geometry than prevailed at the time the subassembly was withdrawn. Furthermore, the reactivity worth of one subassembly is substantially less than the shutdown margin of the control rods.

The fission products escaping to the cell atmosphere would be confined to the cell and/or subsequently released in a controlled manner. Accordingly, this type of accident should not pose a serious hazard.

4. Dropping of a Fuel Subassembly

Many safety features will be incorporated into the fuel-handling machine to prevent inadvertent release of a fuel subassembly during the various stages of remote-controlled fuel-handling operations. Among others, these features will include:

- (a) system interlocks to ensure conformance to the prescribed sequence in which these operations are to be performed
- (b) force-limiting devices to limit the load which the machine can impose upon the subassembly
- (c) positive-locking mechanisms to retain the subassembly during other than normal engagement or release operations.

Nonetheless, it is possible that a subassembly could be released due to failure of a structural member of the machine or the top fitting of the subassembly. For purposes of this safety analysis, it was assumed that such failures could occur. The first failure considered involved the release of a spent fuel subassembly during withdrawal from the reactor. Another considered failure involved the release of a new fuel subassembly during insertion into the reactor.

The first failure considered should not pose a serious condition, since the reactor will be completely shutdown prior to withdrawal of the subassembly. The second failure is potentially more serious, since the state of the reactor must be ascertained from available supervisory information. In order to prevent this type of accident, the worth of the control subassemblies will provide a reactor shutdown margin in excess of the worth of any single fuel subassembly available to the operator.

Similar failures could also result in the release of an irradiated fuel subassembly during transit to, or insertion into, the wet fuel storage tank. The consequences of dropping a spent fuel subassembly on the cell floor could involve a meltdown and release of fission products to the cell. The hazard would be confined to the cell.

The effective multiplication (k_{eff}) of the storage tank will be maintained sufficiently low at all times so that meltdown of a subassembly over the tank, or dropping of a subassembly into the tank, will not cause a serious accident.

5. Criticality in the Fuel Storage Tank

The wet fuel storage tank may have the capacity for a full core loading of subassemblies. Therefore, calculations were made to determine the effective multiplication of several storage configurations. All calculations were based on the tank being filled with sodium and loaded with fuel

and reflector subassemblies on a triangular pitch of 5 in. (Less reactive conditions are encountered when the tank is voided of sodium.)

The loading arrangements and the results are listed in Table 10. The most reactive loading was Case 5, for which $k_{\text{eff}} = 0.75$.

TABLE 10. Calculated k_{eff} for Fuel Storage Tank

Case	Description	Fuel Composition	k_{eff}^*
1	Small core; same loading pattern as Fig. 22.	(Pu-U)C; 34% enriched in Pu^{239} .	0.3
2	Zoned core; same loading pattern as Fig. 21.	Test Zone: (Pu-U) O_2 ; 12.5% enriched in Pu^{239} . Buffer Zone: Depleted UO_2 . Driver Zone: U metal; 67% enriched in U^{235} .	0.5
3	Same as 2, except loading pattern is: Driver/Test Zone/Buffer Zone.	Same as 2.	0.64
4	Loading of 76 driver subassemblies.	U metal; 67% enriched in U^{235} .	0.68
5	Same as 4, except 160 subassemblies.	Same as 4.	0.75

*Assumes no internal structural material within the wet fuel storage tank.

Loading of the wet fuel storage tank will be strictly controlled. Loadings such as in Case 5 will not be permitted. Further, experiments using a 160-subassembly driver zone are not anticipated.

The NaK cooling annulus around the tank will function also as a secondary containment vessel. Therefore, it is unlikely that sodium would be accidentally drained from the tank through leakage. Sufficient heat removal capability will be effected by natural convection of the coolant system in the event of loss of coolant pumping power.

C. Coolant System Failures

1. General System Malfunctions

The sodium coolant systems are designed to cool the reactor by forced convection. Following normal reactor shutdown, either the main or the auxiliary system will have adequate cooling capacity and both will operate simultaneously. In event of a main power failure, (followed by automatic reactor scram) the reactor will still be cooled by forced convection provided

by pump P-35 and the auxiliary cooling system, both running on emergency power. The systems will switchover automatically to emergency power (diesel-generator).

The circumstance of a main power failure and a coincident failure of the diesel-generator to start promptly is of no concern either. Failure of the diesel to start promptly will not cause undesirable transients in the reactor because adequate forced convection is maintained by pump P-35 which is then battery operated. The P-35 batteries are sized to ensure cooling of the reactor until adequate natural convection flow can be established in the main coolant system. These and a number of other circumstances are listed in Table 11. Detailed examination of these cases will be a part of the final safety report.

TABLE 11. Potential Coolant System Malfunctions

Type of Malfunction	Effect on System Operation*
(1) Normal power outage.	Main primary pump stops. Auxiliary coolant system functions on emergency power. Forced convection flow provided in main coolant system by pump P-35 on emergency power.
(2) Main primary pump fails.	Auxiliary coolant system operates on normal power. Main secondary coolant system operative. Forced convection flow available in main primary coolant system from pump P-35.
(3) Loss of sodium from main primary coolant system.	Minimum coolant level maintained in reactor vessel. Auxiliary coolant system operates on normal power. Emergency power available.
(4) Main secondary coolant system fails, e.g., secondary pump or Na-air heat exchanger blower fails.	Auxiliary coolant system operates on normal power. Main primary system operative. Natural convection flow available in main secondary coolant system.
(5) Auxiliary primary pump fails.	Main coolant system operative. Natural convection flow available in main coolant system as a back up.
(6) Auxiliary secondary coolant system fails.	Main coolant system operative. Natural convection flow available in main coolant system as a back up.

*Reactor assumed to scram.

A very preliminary study has been made of the capability of the system to cool the reactor under the most adverse circumstances. These circumstances were assumed to be promoted by the coincident loss of forced convection flow in both the main and auxiliary coolant systems due to simultaneous failure of the normal and emergency power systems. The results are presented to illustrate the performance characteristics of the reactor in the event of such a hypothetical accident.

a. Analytical Basis

The performance characteristics of the reactor and main coolant system were investigated using an IBM-7094 computer code written especially for the FARET system. The work was performed by the Bechtel Corporation, utilizing reactor equations and constants furnished by Argonne.

Fig. 6. Reactor Analytical Model

The reactor model consisted of ten lumped zones as shown schematically in Fig. 6. The primary sodium enters the reactor vessel and core support cylinder in region 1. Region 2 represents the high pressure plenum, and region 3 is the first region within the subassemblies but below the reflector region. After leaving region 3, the coolant splits into two parallel flow paths. These paths consist of regions 4, 6, and 8, and 5, 7, and 9, respectively. Regions 6 and 7 represent the core, and regions 4, 5, 8, and 9 the axial reflector sections. Finally, region 10 represents the outlet coolant mixing plenum.

The reactor core chosen for this study was the small, high-performance assembly. The ratio of power and flow between regions 6 and 7 was assumed to be about 1:3 and 1:5, respectively. This ratio corresponded to 7 and 30 fuel subassemblies, respectively, and was based on differing hydraulic and power generation characteristics. The core pressure drop was assumed to be 85 psi for a total flowrate of 2.5×10^6 lb/hr. Core bypass flow was assumed to be zero. Only the main coolant system was simulated in the model.

The reactor equations are similar to those described elsewhere.^{23,24} The delayed neutron data which were used are given in Table 12. Calculations indicated that the effective neutron lifetime was about $0.5 \mu\text{sec}$.

TABLE 12. Delayed Neutron Data for FARET Transient Studies

Group, i	Decay Constant, sec^{-1}	Delayed Neutron Fraction, β_i
1	0.0129	8.836×10^{-5}
2	0.0311	6.732×10^{-4}
3	0.134	5.565×10^{-4}
4	0.331	9.386×10^{-4}
5	1.26	3.628×10^{-4}
6	3.21	1.223×10^{-4}
		$\beta_{\text{eff}} = 0.00274$

b. Reactor Shutdown Circumstances

Two basic circumstances of reactor shutdown were investigated:

(1) Loss of main coolant system power, followed by reactor scram from 100% power, and subsequent cooling by natural convection in the main system only.

(2) Scram of reactor from 100% power, followed by coast-down of main coolant system to 10% of full flow of sodium and air.

For each of these circumstances, two different delay times of 0 sec and 0.3 sec were considered for initiation of the pump coastdown or reactor scram.

The shutdown condition described in Item (1) corresponds to the rather improbable situation wherein power to the main coolant system is lost, the auxiliary coolant system is lost simultaneously, and the reactor is scrammed from full power at maximum coolant steady-state temperature conditions. Under these conditions, the only mechanism of heat removal is natural convection in the main coolant system. The condition in Item (2) corresponds to the anticipated mode of normal shutdown following a reactor scram.

These four cases are listed in Table 13. For each case, the total control rod worth was assumed to be $0.03 \Delta k/k$, and the initial rod insertion was 40% of the core height.

TABLE 13. Transient Problem Parameters

Case	Description	Delay to Start Reactor Scram or Pump Coastdown, sec
1a	Loss of power to main coolant system, followed by reactor scram from 100% power.	0
1b	Same as Case 1a.	0.3
2a	Reactor scram from 100% power, followed by main coolant system coastdown to 10% full flow of sodium and air systems.	0
2b	Same as Case 2a.	0.3

Two simplifications were used to obtain solutions to each of the above problems. Each was due to programming considerations associated with the solution of the neutron kinetics equations. First, the fission power generation as a function of time after negative reactivity insertion was calculated, using the kinetics equations for about the first two seconds of real time. Thereafter, a straightline extrapolation on a log-log plot was used to describe the fission power decrease with time. This extrapolation leads to reactor powers, during this portion of the calculation, that are conservatively high.

Finally, for Case 1b, power was constant for the 0.3 sec between the loss of pump power and initiation of reactor scram. This results in an initial heat generation rate that is pessimistically high, since the temperature coefficients of reactivity are predominately negative.

c. Results

1) Initial Studies

The initial results of this study are given in Figs. 7 through 10. For each of the four cases listed in Table 13, curves are plotted for main primary sodium flowrate and reactor power vs. time after start of the transient. Also given are pertinent system temperatures during the transient. These include average core coolant (T_c); average core outlet coolant (T_{co}); average reactor plenum outlet coolant (T_{po}); and maximum fuel (T_f) temperatures.

Reactor Power and Main Primary Sodium Flowrate

Reactor Temperatures

Fig. 7. Case 1a

Reactor Power and Main Primary Sodium Flowrate

Reactor Temperatures

Fig. 8. Case 1b

Fig. 9. Case 2a

Reactor Power and Main Primary Sodium Flowrate

Reactor Temperatures

Fig. 10. Case 2b

The average core coolant temperature (T_c) represents the average throughout the core and does not include hot channel, hot spot, or other uncertainty factors. It is the coolant temperature which, when combined with a calculated average fuel temperature, promotes the proper heat flow to the coolant. The average core coolant temperature was assumed to be midway between the average core inlet and the average core outlet coolant temperatures.

The average core outlet coolant temperature (T_{co}) is the mixed mean average coolant outlet temperature from the entire core.

The average reactor plenum outlet coolant temperature (T_{po}) represents the average mixed mean temperature of the coolant effluent from the reactor outlet plenum.

The maximum fuel temperature (T_f) is the peak fuel temperature in the core. The temperature at any given time was estimated based upon instantaneous values of core coolant temperature rise and of reactor power as obtained from the dynamic analyses. Temperature rises from coolant to fuel were based upon steady-state calculations. The coolant temperature rise in the hottest channel was taken to be 1.20 greater than the average core coolant temperature rise to account for maldistribution of coolant flow or other effects. Otherwise, uncertainty factors were not included.

The main primary coolant pump uncontrolled coastdown is fairly short, due to the decoupling of the motor from the pump upon loss of power. The mechanical inertia is due primarily to the impeller.

For Cases 1a (Fig. 7) and 1b (Fig. 8), a mismatch of power and flow occurs about 50 sec after scram. (The mismatch is aggravated by the assumption for power decay as noted before.) This leads to higher coolant ΔT 's across the core than during full-power, steady-state operation. The temperatures peak at higher than full-power, steady-state values, since the reactor inlet temperature does not change significantly during the transient. As natural convection flow becomes fully established, the fuel and core coolant temperatures decrease from the point of maximum mismatch and eventually reach quasisteady-state values. The plenum outlet temperature appears to be approaching a maximum and should decrease with time thereafter.

For Cases 2a (Fig. 9) and 2b (Fig. 10), system temperatures start to rise about one second after the start of flow coastdown, but never reach the full-power, steady-state values. As the flow levels off at 10% of full flow, the temperatures decrease to quasisteady-state values :

The maximum fuel temperatures shown in Figs. 7, 8, 9, 11, and 12 at time zero are substantially less than the value given in Table 16, i.e., 1565°F vs. 3680°F. This is attributed to the fuel thermal resistance which was smaller in the core model used for the thermal transient analysis than in the steady-state calculations reported in Table 16.

2). Later Studies

During the course of the FARET Title II design, the computer code was modified to keep abreast of the evolving FARET design. In particular, two significant changes were made: (a) Sodium bypassing the core was introduced to more accurately account for and investigate mixing of the bypassed (cold) sodium with the core outlet sodium in the core support cylinder. This bypassed sodium reduced the mixed mean reactor vessel outlet temperature from 1200 to 1150°F, (b) the main primary shutdown pump P-35 was added to insure forced circulation in the main primary loop following the loss of the main primary pump.

Typical plots of results using the modified code are given in Figs. 11 and 12. The problem in Fig. 11 is analogous to Case 2a of Fig. 9, except that the main primary loop coasts down to 6% of full flow instead of 10%. This simulates the main primary shutdown pump (P-35) capacity. Although core temperatures are not given in Fig. 11, for the

Fig. 11. Post-Scram Design Conditions

Fig. 12. Post-Scram Design Conditions

first ten seconds after scram they should be very close to those in Fig. 9, since the flow and power decay are similar for both cases in that period of time.

The problem shown in Fig. 12 is also analogous to Case 2a except that the flow coastdown to 10% of full flow is controlled to a 40-sec duration. This problem illustrates expected transients following a normal scram not caused by a loss of electric power. The main effect of programming the 40-sec flow coastdown was that computed equipment transients were reduced.

2. Inlet Coolant Pipe Failure

The primary cooling systems and the reactor vessel have been designed such that the break of a single wall portion of these systems or vessel will not impair the shutdown cooling of the reactor. To meet this criterion double-wall construction has been used for those portions of the primary system in which a sodium leak could impair the shutdown cooling of the reactor. The portions of the system treated in this manner are the auxiliary primary system, the reactor vessel, and a common portion of the main primary system reactor vessel inlet line as shown in Fig. 13. The space between the double pipes is filled with NaK. This NaK-filled space is connected by appropriate piping to the drain tank of the NaK annulus System (Fig. 14).

Fig. 13. Reactor Coolant Systems Elevations and Hydraulic Profile

Fig. 14. Reactor Annulus System and Guard Piping

The second wall guards against the inadvertant loss of sodium coolant in event of a break in the inner wall. The concurrent failure of both walls leading to impairment of the shutdown cooling of the reactor is considered to be incredible except in cases where the core no longer requires cooling anyway because it is concurrently destroyed and the status of the shutdown cooling system becomes academic.

It is important to note that the piping system has been designed by an experienced and competent organization. Further, it will be analyzed for seismic conditions by a recognized consultant. The piping design is in accordance with the ASA-B31.1 Code for Pressure Piping with the additional restriction that no credit is allowed for the difference between the allowable stresses in the hot condition and the sum of longitudinal stresses due to pressure, weight and other sustained external loads in calculating the allowable stress range during steady-state conditions. In addition, some examples of other design practices used are, thin-walled piping expansion bellows are not permitted; internal thermal baffles are provided where fluid temperature changes are of concern; and the reactor vessel primary outlet piping in the cavity is made of the same material as the reactor vessel.

In consideration of the question concerning the effect of a complete break of an inlet coolant pipe to the reactor vessel, the effects of various piping failures were investigated as presented below. In all cases, except that of a complete compound failure of both the piping and the secondary containment barrier (Case VI) which is considered incredible, the shutdown cooling of the reactor is not impaired.

Case I - Failure of the main primary outlet piping, the shell of the main intermediate heat exchanger, the suction piping of the main primary pump, the casing of the main primary pump or the single wall portion of the reactor inlet piping.

Case II - Failure of the main reactor inlet piping without failure of its guard piping (NaK annulus system).

Case III - Failure of any portion of the auxiliary primary system including the piping, the shell of the auxiliary intermediate heat exchanger and the auxiliary primary pump including the primary reactor vessel without failure of the guard system.

Case IV - Failure of a tube(s) in the main intermediate, auxiliary intermediate or reactor flange cooling heat exchanger.

Case V - Failure of the reactor flange cooling heat exchanger shell.

Case VI - Failure of both the guarded sodium piping and its guard piping.

In the analysis of each case, it is assumed that the reactor is operating at full power (50 MWt) and the temperature across the core is a maximum when a failure occurs. Should a failure occur when the reactor is not operating and the core Δt is not maximum, the consequences would be similar to but of a lesser magnitude than those described below.

Case I

If the leakage rate is small (less than 100 gpm), a low level alarm in the primary drain tank is actuated since continuous makeup at 100 gpm is supplied to the reactor vessel from the drain tank. Loss of level in the reactor vessel would not occur until the storage tank level dropped below the makeup pump suction. Smoke detectors are also located in the vault to cause an alarm. Either alarm would lead to shutdown of the reactor by the operator and stopping of the main primary pump and continuous makeup (reactor flang cooling) pump. The auxiliary primary system will provide decay heat rejection. The reactor sodium level would not drop below the reactor main outlet piping. Shutdown cooling of the reactor would not be impaired.

If the leakage rate is above 100 gpm, the reactor would eventually scram due to low reactor sodium level. With this exception, the subsequent action and consequences would be the same as for the low leakage example above.

Case II

Failure of the reactor inlet piping without failure of its guard piping would result in the loss of sodium to the NaK annulus system. The loss would be reflected as a gain in the NaK system inventory and would be annunciated as a high level alarm in the NaK expansion tank. This alarm would lead to an orderly reactor shutdown by the operator with a resultant reduction in main primary flowrate. The flowrate reduction would minimize or stop the leakage since the pump discharge head would be reduced to very low values (<1 psi). In fact, the leakage would reverse since the NaK system is at 6 psi pressure. Shutdown reactor cooling would not be impaired and would be supplied by either the main or auxiliary system.

If the leakage rate is quite large, the NaK annulus expansion tank fills at a high rate. A redundant liquid level device within this tank actuates a reactor scram when the tank level approaches overflow or minimum level. Other subsequent actions and consequences would be the same as for the small leak condition shown above.

Case III

Failure of the auxiliary primary system piping or associated equipment or the reactor vessel without loss of the guard piping would lead to loss of NaK from the annulus system to the primary sodium system. Such a NaK loss would be reflected as a low level in the NaK expansion tank. Upon loss of about 100 gal of NaK, a low level device in this tank would annunciate and an orderly shutdown of the reactor would be initiated by the operator. A redundant low level device would automatically scram the reactor if the tank level reached its lower limit. The NaK system pressure would be reduced (normal blanket pressure is 5 psig) until the leakage stopped (sodium and NaK pressure balanced at the leakage point. Adequate shutdown cooling of the reactor could be performed either with the main or auxiliary systems at this time.

Case IV

Failure of a tube(s) in either of the intermediate heat exchangers of the reactor flange cooling heat exchanger would result in the addition of secondary sodium to the primary system since the pressures within the tubes will be higher than those in the heat exchanger shells. Very small leaks would be detected by a gradual change of the associated system levels. If the coolant makeup system is functioning and the leakage rate is smaller than the makeup rate, the sodium levels in the reactor vessel and secondary expansion tank will not vary, and the leakage will show as a lowering of the inventory in the secondary drain tank with an associated rise in level in the primary drain tank.

With high leakage rates ($>$ the secondary coolant makeup system flow rate), the level in the secondary expansion tank levels will drop and the expansion tank low level alarm will be actuated.

Once leakage of this type has been discovered an orderly reactor shutdown would be initiated and cover gas pressures and/or pumping rates will be adjusted to stop the leakage (by reducing the pressure differential across the leak point). Shutdown cooling of the reactor is not impaired.

Case V

If the shell of the reactor flange cooling heat exchanger should fail, the continuous sodium makeup rate to the reactor would be reduced by the amount equal to the leakage rate. Leakage would be detected by smoke detectors or a low level alarm in the primary drain tank. An orderly reactor shutdown would result and the makeup pump stopped to reduce the leakage. Significant siphoning of the coolant from the reactor vessel would not result since the discharge piping into the reactor vessel is provided with a siphon breaker. Shutdown cooling of the reactor would be effected in a normal fashion.

Case VI

The incredible, concurrent failure of the sodium piping and its guard piping would cause the drainage of sodium from the primary system to the level of the break. The rate of loss would be dependent upon the size of the pipe break and the pressure differential across the break. In all cases the following events would take place.

- (1) The sodium level in the reactor vessel would be lowered.
- (2) The reactor scram would be initiated by a low coolant level. Other scram signals would be initiated if the coolant low level scram failed e.g., loss of primary system flow, high rate of change of primary system flow rate, high coolant temperature from the reactor vessel loss of pump discharge pressure, etc.
- (3) As a consequence of the reactor scram the main primary pump is reduced to 10% flowrate.
- (4) Loss of coolant down to the reactor coolant outlet pipe elevation would cause loss of main primary pump suction and reduction of flow in the main system to zero.
- (5) Low main primary pump flow would cause the auxiliary primary pump flow to increase thru the core.

Dependent upon the location and type of failure the following conditions might prevail:

- (1) If the double-walled main sodium inlet pipe was completely broken, the sodium in the reactor vessel and core support cylinder would drain down to an elevation of approximately 90 ft 11 in., or about 1 ft $3\frac{1}{2}$ in. above the top of the largest planned core. For a massive rupture, i.e., there is no resistance to flow, the time to drain to this elevation is about 40 sec.

Neglecting flow out the main sodium outlet pipe, the total volume of sodium remaining inside the core support cylinder and having access to the core is about 60 ft³ or 3000 lbs. Assuming that the reactor is scrammed and that the decay heat is transferred to the sodium with zero resistance, and neglecting meltdown of the core, it would take over two hours to completely void the core of sodium by boil-off. The top of the fuel would become uncovered in about 11 min.

With the present design, it is not possible to add sodium to the core if there is a break in the double-walled inlet pipe. However, it is possible to add sodium to the reactor vessel. In addition, when using core bypass, the bottom of the core support structure is open to the large volume of sodium available in the reactor vessel causing a small supplemental convection cooling within the reactor vessel.

A sodium flowrate of about 10 gpm would be required to prevent uncovering of the core. If such a flowrate could be achieved, and if the sodium vapor were not condensed and recirculated (the vapor might vent at the pipe break) enough primary sodium could be made available via the reactor flange cooling pump to keep the core covered for over three hours. The reactor flange cooling pump has a throttle valve which can be operated outside the containment and is adjacent to a flowrate indicator. It would then be possible to pump sodium into the reactor vessel at about the same rate at which it is boiled off.

(2) Failure of the auxiliary primary system at or near its lowest elevation would not siphon the reactor vessel to a level below the core. If the break is a complete or massive type failure, i.e., the line physically separates with a complete loss of fluid communication across the broken section, and is located in the auxiliary primary pump discharge line, the pump flow will increase to the maximum consistent with near zero head conditions. The reactor vessel would be drained below the top of the core in about one minute.

3. NaK Annulus System Leaks

The NaK in the annular volume between the inner and outer walls of the reactor vessel and the guarded piping serves three purposes:

- (1) Detect leaks in either the vessel or the piping.
- (2) Initially heat up the vessel prior to loading sodium.
- (3) Provide a medium for the transfer of the heat generated in the outer vessel wall to the sodium in the vessel.

During normal reactor operations the NaK system is stagnant. However, the NaK system is furnished with a heater and a circulating pump for dry heatup of the reactor vessel. This eliminates the need for electric heaters in the vessel. Moreover, by the use of NaK as a heat transfer medium, ΔT between the inner and outer vessel walls can be kept to a minimum. Leaks of the order of 100 gal should be detectable. Leaks from the system would be into an inert gas environment. In the event of a detectable leak, the reactor would be shut down immediately and an appraisal of the situation made.

The calculated pressures in the NaK system and sodium systems at various locations are shown in Fig. 15.

Fig. 15. - Double Contained System Static Pressures

D. Miscellaneous Considerations

1. Hydraulic Ejection of Control Rods

The control subassemblies (control rods) are hexagonal in cross section, and contain a neutron absorbing material in a region corresponding to the active region of a fuel subassembly. Each control rod is located within a guide thimble locked to the lower grid plate. During reactor operation each rod is connected to its individual drive mechanism which provides positive vertical movement of the rod within the stationary thimble (Figs. 17 and 18). Coolant from the low pressure plenum enters the bottom of the guide tube, passes upward through an orifice, and then in-parallel through (1) the annulus between the thimble and rod and (2) a central channel in the rod. The coolant leaves the control rod through holes in the thimble in the vicinity of the upper plenum.

Under normal conditions, sodium enters the high pressure inlet plenum at pressures up to 85 psig. The principal coolant flow is into and upward through the fuel subassemblies. Approximately 15 per cent of the flow into the high pressure plenum passes into the low pressure plenum. It then flows upward through the reflector subassemblies and control rods, and into the reactor vessel through orifices in the low pressure plenum structure. The low pressure plenum operates at pressures up to about 5 psi greater than the reactor vessel pressure at a corresponding elevation.

Fig. 16. Simplified Primary Coolant Systems Diagram

Fig. 17. Arrangement of Reactor Vessel Internals

Fig. 18. Abnormal Flow Distribution in Vessel

During fuel handling the reactor vessel cover is removed, and the control rods rest by gravity at their lowest position in the guide thimbles. They are not connected to the drive mechanisms and therefore, under fuel handling circumstances the control rods could be raised hydraulically if sufficient forces could be generated. An evaluation of this situation did not identify a credible set of circumstances that would cause the control rods to rise.

For a control rod to be raised by hydraulic forces, it is necessary to overcome its weight, which is approximately 90 pounds submerged in sodium. The cross-section area over which pressure forces can act is about 3 sq in. Therefore, a pressure buildup of about 30 psi between the control rod orifice and the bottom of the control rod could just lift a rod. Because of the design (Fig. 18), the pressure which must exist in the low pressure plenum to lift a control rod must be substantially greater than 30 psig. In fact the pressure must be greater than could practically exist.

Pressures in the low pressure plenum greater than the normal operating pressure of 5 psig could occur if:

1. the primary pump is inadvertently energized when a subassembly is being removed during fuel handling
2. a substantial failure of the control rod thimble occurs in the region of the high pressure plenum
3. a transient pressure buildup occurs in the core as a consequence of an accident.

The primary pump control is interlocked so that removal of the reactor vessel cover prevents startup of the pump. It is necessary for this interlock to fail to operate properly and for an operator error to occur in order to start the primary pump during fuel handling. Even under these unlikely circumstances, it would take about 9 to 10 min to bring the pump to full capacity. Should these conditions prevail when a subassembly is in process of removal, the pressure in the low pressure inlet plenum would increase. Flow into this plenum would increase substantially over normal conditions as a consequence of the open grid opening between the high and low pressure plenums that is created by the removal of the subassembly. Flow out of the low pressure plenum would continue to be (1) upward through the control rod thimbles, (2) upward through the reflector subassemblies, and (3) into the reactor vessel via the orifices in the low pressure plenum. Based upon a pressure of 85 psig in the high pressure plenum, the calculated pressure in the low pressure plenum is 15 psig as measured with reference to the pressure in the vessel annulus at the same elevation. Because of the very high relative resistance of the orifice in the control guide thimble and the very low pressure drop through the control rod, the pressure acting to

lift a control rod is no more than this; about 2 psi. It is impossible for the pump to develop enough pressure to lift a control rod under these circumstances providing the coolant passages in and around the control rod are open.

A failure in the thimble where it passes through the high pressure plenum can result in maximum pressure on the upstream side of the orifice in the control rod guide thimble of almost 85 psig as generated by the primary sodium pump. Here again, if the cooling passages of the control rod are free, no significant lifting force is developed. Complete separation of the thimble from its locking mechanism will result in lifting the thimble which in turn will raise the control rod. However, the design is such that complete separation is considered incredible.

If a transient pressure should exist within the reactor which could be transmitted preferentially to the inlet plenum region, it might be possible to raise the control rods. First, it is felt that such conditions are highly unlikely. Second, any pressures transmitted from within the sub-assembly downward into the inlet plenums affects first the high pressure plenum and then the low pressure plenum. The normal pressure reduction of 85 psi to 5 psi between these two plenum regions drastically reduces the effect of a pressure buildup in the high pressure plenum on the low pressure plenum. The normal pressure reduction of 5 psi to about 2 psi across the orifice in the control rod guide thimble reduces still further the effect on the control rod. Even with the very high pressures discussed later in this section under "Fuel Subassembly Separation" it is believed inconceivable that control rods could be hydraulically lifted and cause a greater accident than previously postulated.

2. Reactor Vessel Cover Blow-off

If excessive pressures are imposed on the reactor vessel cover, the cover may fail and be blown off. Under these circumstances, with the control rods attached to the rod drive mechanisms and the core in a near-critical condition at the time, an autocatalytic type accident condition could occur.

In order to establish the credibility of this set of circumstances, the mechanisms for imposing substantial loadings on the underside of the cover have been investigated. The results of this investigation do not identify a credible set of circumstances that could lead to an autocatalytic accident more severe than the MCA.

The reactor vessel cover is designed for the same internal design pressure as the vessel, i.e., 15 psig at 1200°F. It is fabricated of Type 304 stainless steel. The short-time rupture strength of the material at 1200°F is at least nine (9) times the allowable design strength. For example, for

Type 304 stainless steel, the ASME Code allowable design stress is 4500 psi at 1200°F. The corresponding ASTM short-time tensile strength for 18 Cr-8 Ni material is reported to be at least 40,000 psi at 1200°F.²² Moreover, with very short-time loadings such as are considered in the analyses discussed in these paragraphs, the strength of the material is even greater--possibly as much as 1.5 to 2 times the usual short-time tensile strength values reported. The tensile strength also increases with reduced material (i.e., operating) temperature--as high as 55,000 to 70,000 psi or greater at 350°F. Finally the weight of the cover, which will be about 50,000 lb or greater, will counterbalance an internal pressure of 10-15 psi. Considering each of these factors, it appears conservative to assume that the cover will resist as a minimum an internal pressure of at least 135 psi before failure. (Control rod ejection at 135 psi is not a concern since these are conservatively designed for 200 psi.)

There are a number of ways that the cover could be pressurized. These are

- 1) with the argon blanket gas,
- 2) with the liquid sodium in the vessel,
- 3) by meltdown of the fuel causing a sudden vaporization of the fuel and coolant, and
- 4) by coolant vaporization without (prior to) fuel meltdown.

Each of these cases has been considered and is discussed below. In no case was a credible sequence of events identified that leads to a greater accident than the MCA.

a) Pressurization of vessel with blanket gas. Normally the free space in the reactor vessel above the sodium liquid level is filled with the argon blanket gas. The normal operating pressure in the vessel is 0-2 psig. The argon source pressure for the entire facility is 100 psig. This is the maximum pressure that could be imposed on the vessel. To do so, however, requires that a number of the pressure regulators and relief valves fail to function properly at the same time, and that for a considerable period of time this condition prevail in order to add sufficient argon to the system to raise the pressure. In any event, the maximum rate of argon addition to the system is 32 cfm. With the total system volume of 2600 cu ft in the vessel and the drain tank, the vessel pressure would reach 75 psig in approximately 12 hr. During that time two high-pressure alarms would be sounded, and operators could take steps to control the gas pressure manually to avoid high pressure buildup. It is considered extremely unlikely that pressure could buildup under these circumstances. However, in the unlikely event it should, the vessel cover should still remain in place.

b) Pressurization of vessel with liquid sodium. Another conceivable but highly unlikely way to pressurize the vessel cover is by overfilling the vessel and pressurizing it with sodium. The vessel sodium level is maintained by a 4-in. overflow line. This line permits sodium to flow by

gravity from the vessel to the primary drain tank. A reactor flange cooling pump, P-39 recirculates the sodium from the drain tank to the reactor vessel. In the event the 4-in. overflow line should become blocked with pump P-39 continuing to function, and the change in levels in both the reactor vessel and the drain tank being unobserved, it would be possible to overfill the vessel. However, level alarms in both the primary drain tank and the vessel would signal a loss of level in the drain tank and an increase of level in the vessel. The pump would be shut down and a normal reactor scram and shutdown procedure followed. The time lapse between the activation of the level alarms and the overfill of the vessel is estimated to be about eight minutes. During this time it should be possible for the operator to investigate the problem and start an orderly shutdown. Finally, since the maximum shutoff head of pump P-39 is 120 ft of Na @ 960°F (60 psig) the vessel could sustain overfilling and pressurization by the reactor flange cooling pump without displacement of the vessel cover.

c) Fuel meltdown. Meltdown of the fuel as discussed in Appendix C will lead to sudden vaporization of the fuel and coolant within very short intervals of time. An increase of the Core I volume of 8 to 14 per cent will cause reactivity reduction of 6 per cent, which is the upper value of excess reactivity proposed for the Core I control system. At or before the instant that vaporized and expanded fuel exerts a force on the cover, the core volume has already increased by far more than 14 per cent. Therefore, a subsequent lifting of the cover and control rods cannot return the reactor core to criticality.

Accordingly, it is concluded that a postulated lifting of the cover due to movement of fuel caused by melting as described in Appendix C does not present a problem any greater than those already considered.

d) Coolant vaporization. The effects of vaporization of the coolant on the reactor and the reactor vessel were considered for both slow and rapid heating of the coolant. In all cases, the reactor power level is assumed to be increasing exponentially on a constant period. Reactor power periods greater than one (1) second are considered slow for these analyses, whereas periods less than one second are considered to cause rapid heating of the coolant.

The reactor is composed of hexagonal subassemblies which are located within the core support structure. The upper end of each subassembly consists of a 5-ft shielding section. The only direct upward passages for sodium from the coolant outlet plenum are the 30-mil clearance regions between the subassembly shielding sections. All other flow paths between the outlet of the core and the upper regions of the vessel require the coolant to change directions and, in general, are also quite restricted in flow area. Thus, it is clear that large quantities of sodium cannot impinge

directly upon the underside of the vessel cover; a large fraction of the hydraulic effects of sodium ejected from the core will be expended upon the structure within the vessel.

The specific effects of sodium boiling upon the reactor proper cannot be stated with confidence. For example, it is not known what the degree of superheat will be under various conditions of flow, energy release rates, and heat transfer characteristics. Moreover, the pressure forces in the sodium as a function of energy release rate and mass expulsion are not well known. However, calculations based on conservative assumptions have been made in order to obtain upper-bound estimates of these pressure forces and mass expulsion rates.

Brittan²⁶ has studied the effects of excursions on core coolant pressure rises in an EBR-II subassembly in the large TREAT sodium loop. In his calculations, the temperature history of the core coolant was determined when heat is transferred into the coolant during exponential power increases of different periods. From this history, the increase in core coolant volume with time was evaluated, and this increase was considered to be accommodated by upward movement of the coolant above the core. Before core coolant vaporization occurs, the core coolant pressure is determined by the inertial force imposed by the above-core coolant. After core coolant vaporization occurs, the core coolant pressure is the saturation pressure of the coolant (no superheat is assumed) and the motion of the above-core coolant is established by that pressure. The core coolant pressure increases until all liquid coolant is ejected from the core. After that, heat transfer to the coolant is greatly reduced, and the core pressure at that time is an upper limit. The resultant quasisteady vapor pressure within the vessel, a short time after ejection of the liquid sodium from the core, is the peak sodium vapor pressure produced in the core proper by the power transient, reduced by isentropic expansion into the free gas volume in the vessel.

For power periods of 1 to 0.02 sec, Brittan's calculations give the following values for peak core sodium vapor pressures and sodium riser velocities at time of peak pressure.

Period, sec	1	.20	.10	.05	.02
Peak pressure, psia	21	43	84	430	3620
Velocity at peak pressure, ft/sec	7.8	14.2	16.6	33	95

These results are considered to be directly applicable to FARET, primarily because of geometrical similarities. The data has been analyzed on the basis previously stated, i.e., slow power periods are defined as those greater than one (1) second and rapid coolant heating is associated with shorter periods.

1) Rapid Coolant Heating

When the reactor period is shorter than 1 sec, Brittan's work shows that substantial local pressures can be developed within the core. It should be emphasized that these local pressures are not exerted on the cover. The principal source of these pressures is the inertia of the coolant surrounding the core. This inertia resists expansion of the rapidly heated coolant within the core. Under conditions of rapid vaporization of the core coolant, the expanding sodium vapor accelerates the liquid sodium above the core, forcing it upward through the 30-mil annuli between the subassembly shielding sections and against the vessel cover in the form of sodium spray or jets. Following expulsion of the coolant, the expanding sodium vapor imposes a pressure on the cover. Each of these forces, i.e., the sodium spray and the expanding vapor bubble, contribute to loading the underside of the cover. Since the free space volume underneath the cover is about 50 times the volume of Core I, there is a corresponding reduction of the peak local pressures when considering the pressure imposed on the cover.

The maximum pressure that the sodium spray can impose on the cover is determined by the kinetic energy or stagnation head of the spray, $V^2/2g$. For a core peak pressure of 3600 psi, the maximum velocity of the spray emerging from the 30-mil annuli is calculated to be about 200 fps, which produces a stagnation pressure of about 230 psi. This pressure acts on an area equivalent to the area of the 30-mil annuli, which is about 1/100 of the total area of the cover. The effective sodium spray pressure is thus estimated to be about 2.3 psi applied uniformly to the underside of the cover, a negligible load.

The calculations also considered the upward acceleration of sodium in the other flow passages between the core outlet and the upper regions of the vessel. This sodium was found to reach a maximum velocity of about 65 fps, again for a core peak pressure of 3600 psi, producing a stagnation pressure of about 24 psi. This pressure acts on an area only about 1/3 the total cover area, so the effective cover load is 8 psi, again negligible.

The quasisteady pressure loading on the vessel cover, P_{cover} , caused by the sudden vaporization of core sodium, is determined approximately by the isentropic expansion of saturated sodium vapor at P_{core} to the peak local core pressure, from a volume equal to that of the core coolant to a volume equal to the initial gas space in the vessel. A number of checks indicate that an isentropic expansion of saturated sodium vapor reduces the vapor pressure inversely with the volume. Thus

$$P_{\text{cover}} = (\text{vol core coolant/vol gas space}) P_{\text{core}}$$

Since the core coolant volume for Core I is about 1.5 ft³, the free gas space volume is about 75 ft³, and 135 psi is necessary to lift the cover, it is estimated that the minimum peak local core pressure which can cause the cover to lift is

$$P_{\text{core}} = (135)(75)/(1.5) = 6750 \text{ psi.}$$

The validity of the assumed pressure-volume relationship above the critical pressure of 5040 psia may be questioned. Nevertheless it is expected that a peak local core coolant pressure in excess of 5040 psi would be required to cause the vessel cover to lift. Core peak pressures of this type and magnitude should not create conditions more severe than analyzed in Appendix C since fuel vaporization will have disassembled the core before the effects of pressure on the cover can be significant. For example, pertinent milestones of fuel and coolant conditions (during a 0.02 sec period-\$0.99-excursion) can be pointed out.

First, due to the thermal diffusivity characteristics of the system, the fuel will begin to vaporize at least one reactor period before the coolant reaches 2050°F. Second, by the time the coolant reaches 3500°F and the corresponding 3600 psi saturation pressure, at least 2/3 of the fuel will be vaporized. (An energy density of 2×10^4 joules/cc vaporizes the fuel completely.) Since the cladding will melt at these temperatures, the vaporized fuel will expand axially, thus disassembling the core. If the excursion should continue, all of the fuel will be in a vaporized condition at least one period before the pressure of the expanded vapor bubble is exerted on the cover. During excursions of shorter periods, less and less of the energy produced has time to transfer into the coolant, so that more and more of the fuel is vaporized before a 3500°F coolant temperature is produced.

The calculations made in this regard are conservative because good conditions for heat transfer between fuel and coolant are assumed. In addition, single phase fuel and coolant conditions are assumed for heat transfer purposes; thus, the temperature difference, and heat transfer, between fuel and coolant is unrealistically large after fuel vaporization begins. Excursions commencing both with full power operation and with zero power (1 watt) operation were investigated.

Thus, the effects of the conditions postulated do not lead to an autocatalytic accident because fuel is vaporized and core disassembly is in process before core coolant pressures can significantly affect the cover.

2) Slow Coolant Heating

As can be seen from Brittan's work, the peak pressures for excursions having periods longer than 1 sec are negligible. This obtains because the coolant expands so slowly that the inertial forces developed by the core surroundings are negligible.

Under slow (power periods greater than 1 sec) heating conditions, the core coolant temperatures do not exceed the mixed-mean reactor-outlet temperature by much more than during normal operating conditions. Therefore, undue pressures cannot be exerted on the reactor vessel cover unless the free surface of the sodium is heated above the boiling point. This requires increasing the temperature of a sizable amount of coolant and structure by at least 400°F. Due to the large heat capacities involved, this would be inherently a slow process. It is concluded that under these conditions there would be adequate time to shut down the reactor and terminate the incident.

e) Fuel Subassembly Separation

The effects of vaporization of the coolant on the reactor and the reactor vessel cover were considered in the preceding paragraphs for both slow and rapid heating of the coolant. The following analysis is in regard to the partition of energy during the postulated excursions and extends the analysis of the preceding paragraphs to consider the effects of fuel subassembly separation.

The energy developed during a power excursion is partitioned between:

- (a) the energy retained in the fuel
- (b) that transferred to the coolant which raises the bulk coolant temperature in the reactor, and
- (c) the portion which causes coolant vaporization.

This partition of energy is a strong function of reactor period as shown in Fig. 19. For reactor periods longer than 0.1 sec Brittan's work indicates that the power excursion is sufficiently slow that most of the energy is transferred to the coolant without causing a potentially dangerous concentration of energy. Also power excursions with periods of 0.1 sec or greater generate very moderate peak core pressures. On the other hand as will be shown in the following paragraphs, reactor periods shorter than 0.02 sec clearly cause core disassembly before any significant loads can be generated on the reactor vessel cover. Thus, the range of reactor periods of interest is 0.1 to 0.02 sec.

It is of interest to note that the range of reactor periods of 0.1 to 0.02 sec encompasses a very narrow range of reactivity--about 3%. Accordingly, the probability that such an accident will occur is correspondingly small.

As shown in Fig. 19, the total energy developed varies from about 250×10^6 ft-lb for an excursion with a 0.1 sec period to about

550×10^6 ft-lb where a period of 0.02 sec is assumed. For the longer period case, about 140×10^6 ft-lb (56%) of the total energy is retained in the fuel and 110×10^6 ft-lb (44%) is transferred to the coolant. In the latter case with the 0.02 sec period, 480×10^6 ft-lb (87%) is retained in the fuel and only 70×10^6 ft-lb is transferred to the coolant (13%). Moreover, only about 20×10^6 ft-lb (or 3.6% of the total) of that transferred to the coolant is in the form of high pressure vapor and available to do work. Clearly the fraction of the total energy that is transferred to the coolant decreased with decreasing reactor period, over the range of periods of interest.

Fig. 19. Energy Distribution at Time of Peak Core Pressure

Conversely, the fraction of energy retained in the fuel sharply increases with decreasing reactor period. The consequences of this increase are shown in Fig. 20 which gives the fraction of fuel vaporized during power excursions. Three cases are plotted for periods of 0.1, 0.04 and 0.02 sec. It is apparent that for the shorter periods energy is being generated at a rate that far exceeds the heat transfer capability of the fuel and the principal effect is to elevate the fuel temperature.

If the subassemblies do not fail in a manner which permits the upper portion of the subassembly to rise and strike the cover, then the effects on the cover of the sodium spray and the expanding sodium vapor are as discussed in the previous section of this report.

However, if a subassembly should fail in a gross manner, the upper portion of the subassembly would become a potentially serious energy transfer mechanism since the expanding sodium vapor can impart to it substantial momentum energy--up to about 0.3×10^6 ft-lb per assembly. It is possible to calculate, on a very conservative basis, core coolant pressures in the range of 3000 psi and greater for power excursions with periods of about 0.02 sec. During excursions of this type the temperature of the subassembly hexagonal tubing lags the coolant temperature and rarely exceeds about 1200°F for times of interest. Thus, using a tensile strength of 45,000 psi for the 304 stainless steel subassembly (@ 1200°F) and the entire cross-section of the subassembly as the effective area, a peak core pressure of about 3300 psi could fail the subassembly. Gross failure of a subassembly would permit the upper portion to be accelerated upward against the cover.

Figure 20 shows the time that the peak core pressure obtains for each of three excursions of interest. Also shown are the times that the upper portion(s) of the subassembly(s) would strike the underside of the cover. The "time for parted subassemblies to reach the cover" is not shown on Fig. 20 for periods greater than 0.04 sec since for these types of excursions the peak core pressure is substantially less than that required to sever a subassembly.

Fig. 20. Core Conditions at Disassembly

Examination of these data show that, in the cases of interest, the fuel is completely vaporized before the subassembly fragments impinge upon the cover. Accordingly, it is concluded that the effects of these conditions do not appear to lead to more severe accident than previously postulated.

3. Sodium Evaporation within Containment

There may be visualized potential accident situations in which no appreciable pressure is generated to blow the reactor apart and stop the chain reaction. Perhaps such an accident would only be ended by evaporation of enough sodium. This leads to the question of whether or not such a process over-pressurizes the containment and has consequences more severe than the MCA.

The reactor coolant systems are designed to preclude a "credible" accident involving the complete loss of cooling capability. The reactor, the control system and the mode of reactor operation are designed to prevent the inadvertent release of any substantial quantity of sodium vapor into the containment region, e.g., the vessel cover is only removed when the vessel coolant temperatures are about 350°F or less. Moreover, it is inconceivable that a "credible" set of circumstances could prevail that could lead to the situation where there is uncontrolled evaporation and release of sodium to the containment region until, in some fashion, evaporation of enough sodium ends the accident.

Although the circumstances are considered incredible, an analysis has been made, the results of which indicate that it is impossible to pressurize the containment with sodium vapor generated and transported as described above. The following conservative assumptions were made for the purpose of this analysis:

- (1) The reactor is cooled only by sodium evaporation.
- (2) The sodium vapor escapes from the vessel and pressurizes the containment region, i.e., the vessel cover is off.
- (3) All the sodium in the reactor vessel down to the elevation of the bottom of the core, and the sodium within the primary storage tank that is available to the 100-gpm recirculating pump are considered available for evaporation.
- (4) Evaporation of the sodium commences immediately after reactor scram and continues at a rate commensurate with the decay heat rate of the reactor.

- (5) All the heat produced by the core goes into vaporization of the sodium. The heat capacities of materials in the vessel are neglected.
- (6) The only condensing surface available is the containment liner which is at an initial temperature of 100°F.
- (7) Vapor is condensed as rapidly as it is generated and as long as the condensing surface is below the sodium saturation temperature.
- (8) The core melts when it is completely uncovered, at which time the boiling of sodium is considered terminated.

For the conditions under (3) above, it is estimated that 28,500 lb of sodium could be evaporated before the core is completely uncovered. For the conditions in (4) and (5), it is calculated that this would occur over a period of 1.25×10^5 sec, or about 35 hr. During this period of time, the decay heat would average about 650 kW.

Since condensing heat transfer coefficients are very large, the available surface could condense several times the generated vapor even with a negligible temperature difference between the vapor and the surface. For example, with one degree differential the condensing heat transfer rate is about 3×10^7 B/hr or 8800 kW. Considering sodium vapor at 1600°F or greater, the available temperature difference for condensing is very large initially, ~1500°F.

A heat balance was performed on the containment liner which shows that its temperature would not reach the saturation temperature of the sodium vapor. Under these conditions, it follows that all sodium vapor would condense and that no pressure buildup within the containment due to sodium vapor confinement could occur.

4. Electrical and Control Disturbances

The FARET Facility represents a connected load of approximately 4300 kVA, obtained from the NRTS 138-kV loop. NRTS transmission voltage is stepped down to 13.8-kV and power is then fed to the EBR-II bus which directly supplies the FARET pole line.

The reliability of the NRTS and EBR-II systems is illustrated by Table 14. The information recorded is for the period March 1, 1961 to March 2, 1964.

TABLE 14. EBR-II and NRTS Power Outages

NRTS Outages		Additional EBR-II Only Outages	
Date	Length of Outage	Date	Length of Outage
9/1/61	20 min	3/1/61	1 hr 5 min
9/2/61	10 sec	6/1/61	15 min
6/29/62	10 sec	7/25/61	5 min
7/13/62	2 sec	8/4/63	10 min
8/13/63	17 min	12/4/63	8 min

In the thirty-six month interval covered, a total of ten outages were experienced. FARET power is subject to the same interruptions since it is fed from the EBR-II bus.

From EBR-II circuit breaker #14, power is transmitted over a 4,000 ft 13.8 kV overhead line. Breaker #14 is equipped with a timed (one second minimum) single-shot reclosing relay. Approximately 90% of all faults on this line will clear and will allow a successful reclosure.

A high degree of reliability of the electrical supply to critical loads within FARET is obtained by:

- (1) A 625 kVA rated diesel generator which provides backup protection to all critical loads in event of a normal power failure.
- (2) A continuous power motor-generator set in conjunction with a 60-cell lead acid battery that assures continuity of power to loads such as a critical instrumentation and control circuits.
- (3) A two-machine motor-generator set which feeds critical rotating loads during a power outage. This set is energized by the same 60-cell battery as in item (2).
- (4) A converter which charges a separate 60-cell battery and supports on inverter which in turn feeds the 6-kW main shutdown pump (P-35). This pump runs without interruption.

Careful consideration has also been given to details in design such as effective low resistance grounding, coordinated insulation levels, adequate switching surge protection, lightning protection, etc.

a. Design Features

The following represents some of the specific design features incorporated in the design to assure continuity of service:

Three step-down transformers, E-1, E-2A, and E-2B provide utilization voltage to FARET. These transformers are located outdoors and the Askarel coolant is a nonflammable liquid.

Metal enclosed bus ducts carry power from the transformers to the switchgear room on all 480-volt circuits. The 2.4-kV circuits are routed in conduit. All power switching equipment is located outside of the containment structure. The possibility of an oil fire in switches or circuit breakers has been eliminated by the use of air-insulated devices.

The single 2.4-kV bus is protected by 5-kV switch gear with bus conductors insulated with a fire resistant covering. Power and control cables are fed from the switchgear to the load centers in trays installed in accordance with the National Electrical Code. The cables are provided with a flame-retardant jacket.

Two electrically isolated 480-volt busses are provided for distribution of power at this voltage. This isolation is obtained through the application of a normally open bus tie breaker which will prevent an electrical fault on one bus from affecting the other bus.

All switch gear equipment is of the dead-front metal clad type.

The main power transformers have a higher than normal basic impulse level rating of 110-kV.

The 2.4-kV switchgear has an impulse level rating of 60-kV. The 480-volt switchgear will utilize standard air insulated bus conductors.

Each of the 2.4-kV motors (primary pump P-1, secondary pump P-2 and heat exchanger blowers W-1 and W-2) is equipped with three-phase surge capacitors to provide switching surge protection.

The diesel generator is started by admitting compressed air in the cylinders. There are two separate equal volume reservoirs, one of which is used for automatic and the other for manual starting. The air reservoir for automatic starting contains sufficient air for a continuous twenty second starting effort.

In the event that the diesel fails to start automatically, and depletes the first air reservoir, the automatic start system shuts itself down and an alarm is indicated. Manual starting may then be attempted if the situation warrants. Air from the second reservoir is available for manual starting by opening a manual valve in the air supply line. Meanwhile, the signal which initiated the diesel startup will have shutdown the reactor, and the reactor shutdown pump and essential reactor controls and instrumentation will operate from battery power.

Continuous electrical power to the instrumentation and control circuits are provided by a three-machine 208/120-volt motor-generator set. Normally the synchronous drive motor is energized from the 480-volt bus and drives a direct current machine and an alternating current generator on the same shaft. The d.c. machine float charges a 60-cell lead-acid battery. In the event of an a.c. power failure to the synchronous drive motor the d.c. machine will drive the alternating current generator. The d.c. machine is then energized by the 60-cell battery system and the a.c. generator assumes the reactor instrumentation and plant protection load. This machine will provide uninterrupted power in the following manner; as soon as power to the drive motor is lost the excitation voltage to the d.c. machine will be automatically reduced so that the generated d.c. voltage becomes less than the battery voltage. At this point the battery will supply energy to the d.c. machine which will then function as the drive motor, driving the a.c. generator at rated speed.

In addition a two-machine, 240-volt set is provided to furnish power to critical rotating loads such as those required for the fuel handling machine, fuel handling machine blower, and fuel disassembly machine blower in the event of a power failure. A momentary loss of power to these loads can be tolerated and the loads will normally be connected to a 240-volt circuit by means of an automatic transfer switch. In the event of a failure of the normal power the load will be transferred to the motor-generator set which will be driven by 125-volt battery.

The main primary sodium shutdown pump, P-35, will provide flow of sodium if the main primary system pump, P-1, should stop. This electromagnetic pump will be constantly energized by a converter-inverter.

The converter will float charge a separate 125-volt lead-acid battery which in turn will provide uninterrupted power to the pump via the inverter in the event of failure of the normal power source. No other loads will be energized from this power source. The supply for all other critical electrical loads will be backed up by a 625-kVA, 480-volt diesel generator set equipped with an air starting scheme. If normal power fails the diesel will be automatically loaded in four steps to minimize transient voltage dips.

b. Consequences of Loss of Power

The results, in terms of equipment operation and response, of the failure of various parts of the FARET electrical system are considered below. Four separate cases are outlined.

(1) External Power to FARET Site Fails

(a) Immediate Consequences

(i) Reactor scrams.

(ii) Main system primary and secondary coolant pumps P-1 and P-2, and fans on secondary air-cooled heat exchanger, X-2, are de-energized and begin to coast down, eventually stopping.

(iii) Main system primary auxiliary pump, P-35, continues to run on battery power without interruption. This pump alone can furnish 6% of full main primary coolant flow and thus is able to cool the shut-down reactor. Main secondary coolant natural convection flow suffices for the secondary system.

(iv) Instrumentation and the X-2 damper actuators continue to function without interruption on battery power and accumulated air, respectively.

(v) Auxiliary system primary and secondary coolant pumps, P-3, and P-4, are de-energized and the flows begin to coast down.

(b) After about 15 sec:

(i) The diesel engine starts and energizes the emergency busses.

(ii) Auxiliary system pumps, P-3 and P-4 are re-energized at their normal voltages. (Primary flow at about 6% of main primary flow and secondary flow at about 6% of main secondary flow.)

(2) External Power Fails and Diesel Fails to Start

(a) Same as (1,a).

(b) After about 30 minutes (If power is not restored).

(i) Main primary pump, P-35, battery begins to lose voltage rapidly and main primary coolant flow drops to natural convection flow. This is sufficient to remove the reactor decay heat.

(ii) The instrument power battery becomes exhausted. When either the instrument power, or the instrument air supply is exhausted, the secondary air-cooled heat exchanger dampers can be operated manually.

(3) External Power Fails and Pump, P-35, Fails

(a) Same as (1,a), except that pump P-35, also ceases to circulate main primary coolant. Flow falls to about natural convection values.

(b) Same as (1,b).

(4) External Power, Pump P-35 Power and Diesel Engine all Fail; Instrument Power Remains

The reactor scrams and all flows coast down to natural convection flowrates. Main secondary heat exchanger dampers continue to operate (actually will remain wide open) until instrument air or instrument electrical power is exhausted. The effects of this case was studied and the results are provided in this Section under "Coolant System Failure."

c. Fires in Electrical Equipment

Every consideration is given to the electrical system design including those relating to the possibility of electrical fires. The following analyses, which assume a number of malfunctions due to fires in certain portions of electrical equipment, are presented for illustrative purposes.

1) Nuclear Instrumentation

With one exception, either grounding or open circuiting of the inner conductor of a coaxial cable will result in the transmission of a "channel tripped" output signal to the scram circuit logic. The exception involves the grounding or open-circuiting of the inner conductor of the coaxial cable carrying the ionization chamber compensating voltage. This failure will cause an apparent increase in chamber output signal because the gamma compensation is lost. Depending on previous reactor operating history this greater signal may or may not exceed the high flux trip setting. In either case the effect is not of itself hazardous.

2) Process Instrumentation

The short-circuiting, grounding, or open-circuiting of process instrument signal leads will generally result in either zero signals or off-scale very high signals. However, a thermocouple extension lead short circuit in the presence of the heat of a fire could form a new hot junction that conceivably might be indicated as a reasonable temperature on the associated instrument. It is judged that this delicate effect could be maintained at most for only a few seconds before the junction temperatures would change radically and/or other circuits would begin to behave in definitely abnormal ways. In any case, fire-caused faults in the process

instrumentation can be expected to result very quickly in abnormal signals that will be detected by alarm or scram circuits, depending on the instrument channels affected.

3) Control Rod Drives

A situation may be considered in which a short circuit is initiated by a fire in the electrical leads between the rod drive starters and the reactor control console. This event could mimic closing of the control switch contacts calling for rod drive up, thus adding reactivity. In such an event the reactor would be scrammed by one of the flux or period protective circuits exactly the same as if the rod drive action was initiated by an operator. However, since portions of the scram circuits also traverse the same general area in the cable spreading room it may be inferred that a concurrent disabling of the scram circuit could occur. This requires that two rather select short circuits occur before being detected. The following plant design features are included in order to render the probability of this combination of circumstances negligibly small:

(a) The routing of critical scram circuit conductors between panels is in separate conduits that do not contain other conductors.

(b) The circuit configuration is such as to minimize the likelihood of accidentally disabling the scram circuit or initiating rod drive signals.

(c) High-temperature insulation is specified for critical scram circuit and drive circuit conductors.

4) Cooling System Control

Electrical malfunctions could be visualized as having been initiated by fire which could result in either too much or too little cooling effect in terms of the reactor heat generation and operating program. In the event of an unscheduled shutdown of one of the main coolant circulating pumps or heat exchanger blowers for any reason the reactor will be scrammed by the normal plant protective circuits.

Another circumstance that may be visualized is that due to the heat of a fire, a pump speed controller is caused to suddenly call for full speed from one of the main coolant circulating pumps. If the pumps were running initially at low speed, a rapid increase to full speed and flow could produce a temperature change more severe than that accompanying a normal scram from full power. This event will be avoided because the primary-to-secondary coolant loop flow imbalance resulting from such action at one of the pumps will result in a reactor scram. The probability of simultaneously increasing the speed of both pumps with near-perfect matching of the resultant flows is judged to be negligibly small.

A similar increase in speed demand at one of the main heat exchanger blower speed control units would result in an increase in the rate of heat removal from the secondary coolant circuit. If the reactor was at low power and low flow a large steep drop in the outlet temperature of the air-cooled heat exchanger could be expected to result. In order to limit the magnitude of such a temperature change to a low value, the heat exchanger blowers automatically shut down when the negative rate of change of outlet temperature at the air-cooled heat exchanger exceeds a preset value.

APPENDIX A

Reference Cores

1. General

The basic objective of the FARET experimental program is to investigate and evaluate the nuclear and engineering performance characteristics of potential fast reactor fuel systems. The data obtained from these experiments could be applied to the design of large, fast reactor cores in future, central station power plants.

These investigations relate to test cores which differ in size, configuration, composition, and purpose. For example, physics experiments can be performed on zoned assemblies designed to duplicate the neutron spectra and temperatures of large, fast cores. These environments provide data on power distribution, control rod programming, and reactivity changes due to the Doppler effect, sodium expansion, fuel expansion, and fuel burnup.

On the other hand, the engineering experiments, can be conducted on small, high-performance assemblies designed to duplicate the high power densities, temperatures, and radiation levels anticipated in advanced fast cores. These environments are needed primarily for purposes of evaluating the physical and mechanical properties of various fuels and cladding materials of interest.

Both categories of experiments require considerable flexibility in core loadings, including access to the core for specific instrumentation, and operating flexibility in power level, coolant flowrate, and coolant temperature. The design of the FARET Facility reflects these requirements.

For the preliminary safety analysis, a reference core has been selected for each of the two categories of planned experiments. They are considered reasonably typical of the assemblies which can be operated in FARET. Core I which is described in Volume I falls between the limits generally bracketed by these two reference cores.

2. Reference Zoned Core

Figure 21 is a plan view of the reference zoned core. It consists of a relatively small, central test zone surrounded by a buffer (filter) zone, a driver zone, and a reflector.

The subassemblies in the test zone are fueled with $(\text{Pu-U})\text{O}_2$, while those in the buffer zone contain depleted UO_2 . The buffer zone serves to equilibrate the neutron spectrum in the test zone to that of a corresponding

Fig. 21. Reference Zoned Core (466 liters)

zone in a large, dilute system of interest. The driver zone is composed of metallic uranium alloy and will supply an overriding prompt negative reactivity.

The core design and operating data at full power (50 MWt) are listed in Table 15.

TABLE 15. Calculated Parameters for Reference Zoned Core

Heat Output, MW(t)	50
Primary Sodium Temperature, °F(°C)	
To Reactor	775 (410)
From Reactor	1010 (545)
Primary Sodium Flowrate through Reactor (at 1100°F), gpm	6000
Reactor Pressure Drop, psi	40
<u>Core Dimensions</u>	
Test Zone	
Equivalent Diameter, in. (cm)	14.8 (37.6)
Height, in. (cm)	36 (91.4)
Volume, liters	102
Buffer Zone	
Equivalent Outside Diameter, in. (cm)	27.4 (69.6)
Height, in. (cm)	36 (91.4)
Volume, liters	248
Driver Zone	
Equivalent Outside Diameter, in. (cm)	31.6 (80.3)
Height, in. (cm)	36 (91.4)
Volume, liters	116
Total Volume, liters	466
<u>Subassemblies</u>	
Core	
Test Zone	37
Buffer Zone	82
Driver Zone	42
Total Core	161
Control	8
Radial Reflector	426
Total	595
Configuration	Hexagonal
Dimension across Flats, in. (cm)	2.290 (5.817)
Hexagonal Tube Thickness, in. (cm)	0.040 (0.102)
Lattice Spacing (Pitch), in. (cm)	2.320 (5.893)

TABLE 15 (Contd.)

<u>Fuel Elements</u>	
<u>Test Zone</u>	
Elements per Subassembly	7
Fuel Rod Length, in. (cm)	36 (91.4)
Fuel Outside Diameter, in. (cm)	0.524 (1.331)
Fuel Tube Outside Diameter, in. (cm)	0.594 (1.509)
Fuel Tube Wall Thickness, in. (cm)	0.033 (0.084)
<u>Buffer Zone</u>	
Elements per Subassembly	37
Fuel Rod Length, in. (cm)	36 (91.4)
Fuel Outside Diameter, in. (cm)	0.227 (0.576)
Fuel Tube Outside Diameter, in. (cm)	0.271 (0.688)
Fuel Tube Wall Thickness, in. (cm)	0.012 (0.030)
<u>Driver Zone</u>	
Elements per Subassembly	61
Fuel Rod Length, in. (cm)	36 (91.4)
Fuel Outside Diameter, in. (cm)	0.177 (0.450)
Fuel Tube Outside Diameter, in. (cm)	0.221 (0.561)
Fuel Tube Wall Thickness, in. (cm)	0.012 (0.030)
<u>Upper and Lower Reflector Dimensions</u>	
Equivalent Diameter, in. (cm)	31.6 (80.3)
Length (each end), in. (cm)	12 (30.5)
<u>Radial Reflector Dimensions</u>	
Equivalent Outside Diameter, in. (cm)	60.0 (152.4)
Length, in. (cm)	84 (213)
Radial Thickness, in. (cm)	14.2 (36.1)
<u>Core Composition, vol-%</u>	
<u>Test, Buffer, and Driver Zones</u>	
Fuel	32
Stainless Steel	16
Sodium	52
<u>Control and Safety Rod Composition (Effective Section), vol-%</u>	
Boron Carbide	30
Stainless Steel	20
Sodium	50
<u>Upper and Lower Reflector Composition, vol-%</u>	
Stainless Steel	48
Sodium	52
<u>Radial Reflector Composition, vol-%</u>	
Stainless Steel	82
Sodium	18

TABLE 15 (Contd.)

<u>Fuel</u>	
Test Zone	
Material	(Pu-U)O ₂
Pu ²³⁹ Enrichment, at-%	12.5
Quantity, kg	
U ²³⁸	276
Pu ²³⁹	39
Buffer Zone	
Material	Depleted UO ₂
U ²³⁵ Enrichment, at-%	~0.4
Quantity, kg	
U ²³⁵	3
U ²³⁸	763
Driver Zone	
Material	Enriched Uranium-Fissium Alloy
U ²³⁵ Enrichment, at-%	67
Quantity, kg	
U ²³⁵	462
U ²³⁸	233
<u>Heat Generation in Core</u>	
Power, MW(t)	
Test Zone	6.2
Buffer Zone	2.5
Driver Zone	41.3
Total	50
Power Density	
Average, MW(t)/liter	0.108
Maximum, MW(t)/liter	0.525
Maximum-to-Average Ratio	4.85
Radial Maximum-to-Average Power Density	
Ratio at Reactor Center Plane	3.88
Axial Maximum-to-Average Power Density	
Ratio at Reactor Centerline	1.25
Surface Area, Fuel Element, ft ²	
Test Zone	120
Buffer Zone	630
Driver Zone	446
Maximum Heat Flux, Btu/(hr)(ft ²)	
Test Zone	226,000
Buffer Zone	23,100
Driver Zone	467,000
Average Heat Flux, Btu/(hr)(ft ²)	
Test Zone	175,000
Buffer Zone	13,000
Driver Zone	315,000

TABLE 15 (Contd.)

Temperatures in Core (No Uncertainty Factors), °F(°C)

<u>Test Zone</u>	
Fuel (Max.)	3340 (1840)
Clad Inner Surface (Max.)	1135 (610)
Fuel (Avg.)	2055 (1125)
Clad Inner Surface (Avg.)	995 (535)
Clad Temperature Difference (Max.)	57 (32)
Clad Temperature Difference (Avg.)	45 (25)
Fuel Surface (at hot spot)	1490 (810)
Clad Inner Surface (at hot spot)	1065 (575)
Clad Outer Surface (at hot spot)	1005 (540)
Coolant (at hot spot)	935 (500)
Coolant (Max.)	1075 (580)
<u>Buffer Zone</u>	
Fuel (Max.)	1115 (600)
Clad Inner Surface (Max.)	1080 (585)
Fuel (Avg.)	925 (495)
Clad Inner Surface (Avg.)	900 (480)
Clad Temperature Difference (Max.)	2 (1)
Clad Temperature Difference (Avg.)	1 (0.6)
Fuel Surface (at hot spot)	1075 (580)
Clad Inner Surface (at hot spot)	1075 (580)
Clad Outer Surface (at hot spot)	1075 (580)
Coolant (at hot spot)	1075 (580)
Coolant (Max.)	1075 (580)
<u>Driver Zone</u>	
Fuel (Max.)	1175 (630)
Clad Inner Surface (Max.)	1105 (595)
Fuel (Avg.)	985 (530)
Clad Inner Surface (Avg.)	940 (505)
Clad Temperature Difference (Max.)	43 (24)
Clad Temperature Difference (Avg.)	29 (16)
Fuel Surface (at hot spot)	1095 (590)
Clad Inner Surface (at hot spot)	1085 (585)
Clad Outer Surface (at hot spot)	1050 (570)
Coolant (at hot spot)	1035 (560)
Coolant (Max.)	1075 (580)
<u>Thermal Calculation Assumptions</u>	
<u>Thermal Conductivities, Btu/(hr)(ft)(°F)</u>	
Metal Fuel (Driver Zone)	19.8
Oxide Fuel (Test Zone and Buffer Zone)	1.5
Stainless Steel	11.5
Sodium	36.5
<u>Fuel-clad Interface Conductance (Test Zone), Btu/(hr)(ft²)(°F)</u>	
	600

TABLE 15 (Contd.)

Nuclear DataTest Zone

Average Fission Density, $f/(\text{cm}^3)(\text{sec})$	1.84×10^{12}
Neutron Flux, $n/(\text{cm}^2)(\text{sec})$	
Above 1.35 MeV	0.70×10^{14}
Below 1.35 MeV	7.28×10^{14}
Total	7.98×10^{14}

Driver Zone

Average Fission Density, $f/(\text{cm}^3)(\text{sec})$	1.08×10^{13}
Neutron Flux, $n/(\text{cm}^2)(\text{sec})$	
Above 1.35 MeV	0.98×10^{14}
Below 1.35 MeV	5.32×10^{14}
Total	6.30×10^{14}
Prompt Neutron Lifetime, sec	8.90×10^{-7}

Reactor ControlAverage Reactivity Coefficient, $(\Delta k/\Delta T)/^\circ\text{C}$ Test Zone

Doppler Coefficient at 750°K	-0.81×10^{-6}
Sodium Expansion	$+1.08 \times 10^{-6}$
Fuel Expansion	-0.20×10^{-6}

Buffer and Test Zones

Doppler Coefficient at 750°K	-2.45×10^{-6}
Sodium Expansion	$+3.37 \times 10^{-6}$
Fuel Expansion	$+1.09 \times 10^{-6}$

Driver, Buffer, and Test Zones

Doppler Coefficient at 750°K	-2.72×10^{-6}
Sodium Expansion	$+1.85 \times 10^{-6}$
Fuel Expansion	-2.59×10^{-6}

Whole Reactor

Sodium Expansion	-6.14×10^{-6}
Fuel Expansion*	-3.21×10^{-6}

Total Worth of Control Rods, Δk

No. Rods	8
100% Natural Boron	0.04
75% Natural Boron-25% B ¹⁰	0.07
50% Natural Boron-50% B ¹⁰	0.091

*Includes Stainless Steel Expansion in the Reflector.

A. Reactivity Effects

The principal reactivity effects of interest in large, ceramic-fueled reactors are those related to fuel temperature (Doppler effect and thermal expansion), sodium expansion, and fuel rod bowing and movement. Each of these effects may be investigated in a zoned core.

1) Measurement of Fuel Temperature Effect

It is desired to measure the fuel temperature effect by varying the temperature of the fuel in the test zone and, at the same time, minimizing the reactivity effects generated by temperature changes external to the fuel (cladding and coolant temperatures).

The fuel temperature is dependent upon the fuel power density and the thermal conductance from the fuel to the coolant. Either of these parameters may be varied to obtain changes in fuel temperature with only minor changes in cladding and coolant temperature. For example, high fuel temperatures can be obtained by using relatively large test zone fuel elements (to produce a large thermal gradient through the fuel) with intentionally poor thermal bonds between the fuel and the cladding. The thermal conductance can be varied in selected test zone fuel pins either by using different gases or by varying the gas pressure in the gas gap between the fuel and cladding. The power density will be varied by changing the reactor power level. Changes in coolant and cladding temperatures will be minimized by varying the coolant flowrate in direct proportion to the power level. Concurrently, fuel temperature changes in the buffer and driver zones will be minimized by good fuel element heat transfer characteristics.

2) Measurement of Sodium Expansion Effect

The sodium expansion coefficient of reactivity can be determined by varying the coolant flowrate and maintaining the reactor power and coolant inlet temperature constant. The mean temperature of the sodium can be changed to effect a corresponding change in the state of the reactor. With the fuel temperature effect having been determined previously, the effect of the sodium may be isolated. The sodium coefficient can be investigated also as a function of sodium and fuel temperatures in conjunction with the fuel temperature reactivity measurements.

3) Fuel Rod Bowing and Movement Studies

Fuel rod bowing and movement includes distortions of the core which are induced by differential heating and cooling of the fuel elements, or by thermal expansions of the core support structure. The addition of restraint to the fuel to prevent inward bowing can negate a positive power

coefficient. Although no specific techniques have been developed for this type of experiment, it is planned to investigate the feasibility of performing such experiments in FARET.

B. Measurements of Neutron Reaction Rates

Integral reaction rate measurements could include capture-to-fission cross section ratios of fissile materials, and capture cross sections for U^{236} , U^{238} , Pu^{240} , and Pu^{242} . Attempts could be made to measure the capture cross sections of Np^{239} and Pa^{233} which have relatively short half-lives (on the order of days). The above neutron reactions cannot be measured in critical assemblies.

It is considered feasible to perform accelerated irradiation studies in the center of a zoned system in which the neutron spectrum is similar to that of a large dilute reactor. At the full power level of 50 MW(t), the power density in the test zone may be of the order of 0.06 MW(t)/liter. With a macroscopic fission cross section of 0.0024 cm^{-1} , the average flux in the test zone is $9 \times 10^{14} \text{ n}/(\text{cm}^2)(\text{sec})$. For a reaction cross section of one barn, the reaction rate is estimated to be about 0.2% per month. Therefore, an irradiation time period of one to two months could determine average capture cross sections for isotopes having cross sections in the range from 0.5 to 1 barn, assuming that isotopic contents of 0.1% can be adequately analyzed.

C. Reactor Dynamics

The basic experiments for observing system dynamics will probably consist primarily of transfer function measurements. These measurements can be made by superimposing an oscillatory reactivity input on the operating reactor, and mapping the response of the reactor power to this input. When possible, other variables, such as fuel and coolant temperature, could be correlated with the input signal. The results of these measurements will display incipient resonances or signal frequencies at which potential instabilities might occur.

3. Reference Small Core

Figure 22 is a plan view of the small (41.7 liters), high-performance assembly used as a reference core in this report. It is typical of the small cores which can be employed to evaluate various fuel and cladding materials at the high power densities and temperatures anticipated in large fast reactor cores.

Fig. 22. Reference Small Core (41.7 liters)

The reference small core consists of a uniform array of stainless steel-clad, plutonium-uranium carbide-fueled subassemblies surrounded by a stainless steel reflector. Mixed carbide fuel was selected because of the favorable properties of good thermal conductivity, stability at high temperatures, low vapor pressure, and high fissile atom density. Stainless steel cladding was assumed even though it is recognized that cladding temperatures may be a limiting factor.

The design and operating characteristics of the reference small core are summarized in Table 16. The Pu^{239} content can be made identical with that of a large reactor by replacement of U^{238} with U^{235} to achieve criticality. (Note: The reference core is assumed to be enriched entirely with Pu^{239} .)

TABLE 16. Calculated Parameters for Reference Small Core

Heat Output, MW(t)	50
Primary Sodium Temperature, °F (°C)	
To Reactor	966 (520)
From Reactor	1200 (650)
Primary Sodium Flowrate through Reactor (at 1100°F), gpm	6000
Reactor Pressure Drop, psi	85
<u>Core Dimensions</u>	
Equivalent Diameter, in. (cm)	14.8 (37.6)
Height, in. (cm)	14.8 (37.6)
Total Volume, liters	41.7
<u>Subassemblies</u>	
Core	37
Control	8
Radial Reflector	550
Total	595
Configuration	Hexagonal
Dimension across Flats, in. (cm)	2.290 (5.817)
Lattice Spacing (Pitch), in. (cm)	2.320 (5.893)
<u>Fuel Elements</u>	
Elements per Subassembly	61
Fuel Pin Length, in. (cm)	14.8 (37.6)
Fuel Outside Diameter, in. (cm)	0.198 (0.500)
Fuel Tube Outside Diameter, in. (cm)	0.228 (0.579)
Fuel Tube Wall Thickness, in. (cm)	0.015 (0.038)

TABLE 16. (Contd.)

<u>Upper and Lower Reflector Dimensions</u>	
Equivalent Diameter, in. (cm)	14.8 (37.6)
Length (each end), in. (cm)	18 (45.7)
<u>Radial Reflector Dimensions</u>	
Equivalent Outside Diameter, in. (cm)	60.0 (152.4)
Length, in. (cm)	84 (213)
Radial Thickness, in. (cm)	22.3 (56.6)
<u>Core Composition, vol-%</u>	
Fuel	40.3
Stainless Steel	21.9
Sodium	37.8
<u>Control and Safety Rod Composition (Effective Section), vol-%</u>	
Boron Carbide	30
Stainless Steel	20
Sodium	50
<u>Upper and Lower Reflector Composition, vol-%</u>	
Stainless Steel	48
Sodium	52
<u>Radial Reflector Composition, vol-%</u>	
Stainless Steel	82
Sodium	18
<u>Fuel</u>	
Material	(Pu-U)C
Pu ²³⁹ Enrichment, at-%	34
Quantity, kg	
U ²³⁸	142
Pu ²³⁹	73
<u>Heat Generation in Core</u>	
Power Density	
Average, MW(t)/liter	1.20
Maximum, MW(t)/liter	1.71
Maximum-to-Average Ratio	1.42
Radial Maximum-to-Average Power Density Ratio	
at Reactor Center Plane	1.24
Axial Maximum-to-Average Power Density Ratio	
at Reactor Centerline	1.15
Surface Area, Fuel Elements, ft ²	166
Heat Flux, Btu/(hr)(ft ²)	
Maximum	1,467,000
Average	1,030,000

TABLE 16 (Contd.)

<u>Temperatures in Core (No Uncertainty Factors), °F (°C)</u>	
Fuel (Max.)	3680 (2025)
Clad Inner Surface (Max.)	1370 (740)
Fuel (Avg.)	2635 (1445)
Clad Inner Surface (Avg.)	1200 (650)
Clad Temperature Difference (Max.)	125 (70)
Clad Temperature Difference (Avg.)	85 (47)
Fuel Surface (at hot spot)	2985 (1640)
Clad Inner Surface (at hot spot)	1295 (700)
Clad Outer Surface (at hot spot)	1180 (635)
Coolant (at hot spot)	1125 (605)
Coolant (Max.)	1260 (680)
<u>Thermal Calculation Assumptions</u>	
Thermal Conductivities, Btu/(hr)(ft)(°F)	
Fuel	10
Stainless Steel	11.5
Sodium	36.5
Fuel-clad Interface Conductance, Btu/(hr)(ft ²)(°F)	1000
<u>Nuclear Data</u>	
Average Fission Density at Core Center, f/(cm ³)(sec)	3.04 x 10 ¹³
Neutron Flux at Core Center, n/(cm ²)(sec)	
Above 1.35 MeV	0.75 x 10 ¹⁵
Below 1.35 MeV	2.69 x 10 ¹⁵
Total	3.44 x 10 ¹⁵
Prompt Neutron Lifetime, sec	6.55 x 10 ⁻⁷
<u>Reactor Control</u>	
Average Reactivity Coefficients, (Δk/ΔT)/°C	
Core	
Doppler Coefficient at 750°K	-0.33 x 10 ⁻⁶
Sodium Expansion	-8.75 x 10 ⁻⁶
Fuel Expansion	-4.13 x 10 ⁻⁶
Region 1 (Radius = 0-10 cm)	
Doppler Coefficient at 750°K	-0.05 x 10 ⁻⁶
Sodium Expansion	-1.35 x 10 ⁻⁶
Fuel Expansion	-0.97 x 10 ⁻⁶
Region 2 (Radius = 10-18.796 cm)	
Doppler Coefficient at 750°K	-0.28 x 10 ⁻⁶
Sodium Expansion	-7.40 x 10 ⁻⁶
Fuel Expansion	-3.16 x 10 ⁻⁶
Reflector	
Sodium Expansion	-21.2 x 10 ⁻⁶
Stainless Steel Expansion	-0.1 x 10 ⁻⁶

TABLE 16. (Contd.)

Reactor Control (Contd.)Total Worth of Control Rods, Δk

No. Rods	
100% Natural Boron	8
50% Natural Boron-50% B ¹⁰	0.029
25% Natural Boron-75% B ¹⁰	0.056
	0.065

A. Irradiation Studies

Specimen fuel subassemblies will be exposed for prescribed intervals to determine changes in dimensions, physical properties, and appearance of the fuel and cladding materials. Of particular importance is the determination of burnup capability and the effect of power density and temperature on that capability.

At the proposed high power densities, irradiation results can be obtained in a reasonable period. For example, in the reference small core with an average power density of about 1.2 MW(t)/liter of core volume, 1% Pu burnup can be obtained in about 14 days. This is equivalent to 1% burnup of heavy atoms in about 30 days.

The data obtained from these irradiations can be correlated with the operating conditions during exposure and thus provide a basis for predicting performance of promising material combinations in large power reactors.

B. Reactivity Coefficients

Although the small core experiments are intended primarily to develop engineering information relating to fuel performance, they can provide also an opportunity to investigate thermal and radiation-induced changes in properties affecting reactivity coefficients. Changes related to high fission rate, high power density, and high burnup can be achieved under more severe conditions in the small cores than in the zoned cores. Also, the reactivity of a small, high-neutron-leakage core is extremely sensitive to fuel dimensional changes, and the Doppler effect is quite small.

The actual dimensional response, which has an influence on reactor system safety, is not well known for highly irradiated metallic or ceramic-fueled cores. Therefore, measurement of the temperature and power coefficients initially and periodically during exposure will provide an indication of the effects of radiation-induced property changes on these coefficients.

The technique of measurement should be similar to that used for the zoned core: the fuel temperature is varied while temperature changes in the clad and coolant are held essentially constant (by varying reactor power and coolant flow simultaneously).

C. Vented Fuel Experiments

Vented fuel experiments were considered for FARET. These experiments involve a special fuel rod vented to analytical equipment outside the reactor. Analysis of the vented gas would establish not only which species are volatile under actual power and fuel temperature conditions, but also their absolute and relative concentrations.

Also of interest are experiments in which the volatile gases would be vented directly to the coolant. Subsequent analysis of the coolant, e.g., for cesium as a function of nvt, would afford a reasonable evaluation of the extent of coolant contamination.

APPENDIX B

FARET Containment Structure Analysis1. Nuclear Excursion EffectsA. Analytical Procedures

When a medium is subjected to a dynamic load, the medium response can be considered from two points of view, i.e., analysis of forces or analysis of energy absorption. If one is to consider forces, response of the medium is interpreted in terms of the physical variables. These include pressure (stress), particle velocity, density, and damping.

Using the force analysis approach, calculation of the maximum excursion that any reactor core and surrounding structure can sustain requires knowledge of the dynamic loadings. In the case of FARET, these loadings are obtained from the solution of six main problems:

(1) Determination of the shock wave and gas bubble pressure-time curves as the shock passes through the coolant and structure surrounding the reactor core and impinges upon the inner face of the reactor vessel.

(2) Evaluating the dynamic response of the reactor vessel to the shock wave, and the resulting effect of the vessel response on the shock wave itself. The loading and motion of the vessel and coolant must be treated as a single dynamical problem, since reflection, refraction, and diffraction effects can occur.

(3) Shock wave propagation through the rest of the media, taking into account reflection, refraction, diffraction, and decay.

(4) The effect of the structure on the gas bubble expansion during the period in which the shock wave is being considered.

(5) The response of the structure to the gas bubble after the shock wave has ceased to exist.

(6) Establishment of appropriate damage criteria.

The mathematical solution of these problems is difficult and time consuming. Therefore, the FARET containment structure was evaluated using a relatively easier approach involving the use of energy considerations.

From an energy point of view, the physical variables become strain energy, kinetic energy, temperature rise, and chemical reactions. For any nuclear excursion, the explosive energy released must be dissipated by inertia effects (kinetic energy), damping (temperature rise), and strain energy absorption capabilities of the reactor, reactor vessel, and the surrounding structural complex. In FARET, the means for absorbing this energy transmitted by the shock wave and gas bubble fluid momentum are:

- (1) Sodium-temperature rise only. (Sodium inertia effect must be absorbed by the rest of the complex.)
- (2) Reactor vessel--strain energy.
- (3) Inert gas spaces--temperature rise.
- (4) Borated graphite--temperature rise. (Inertia effect is negligible.)
- (5) Insulation--negligible inertia and damping.
- (6) Reinforced concrete--strain energy.
- (7) Cell roof structure--strain energy.
- (8) Subassembly shielding and reactor vessel cover--inertia and strain energy.

The total energy absorbing capability of the reactor and structure complex is then determined by the summation of energy absorptions for each portion thereof.

B. Energy Release and Distribution

A nuclear excursion in the FARET reactor is expected to provide an energy release within the core region within 100 microseconds. If the energy release is sufficiently high, it will transform the fuel elements and coolant, almost instantaneously, into a mixture of gases at extremely high temperatures and pressures. The high pressure will cause a shock wave to propagate through the media surrounding the core. This shock wave and the effects of expansion of the hot gases behind it are the quantities which cause damage to the surrounding media.

For any high-pressure, high-temperature, short-duration energy release inside a fluid, there are three damage factors. In order of appearance on the time scale, these are: (1) shock wave; (2) fluid momentum due to shock wave; and (3) gas bubble at elevated temperature and pressure pushing on the fluid. The general features of these quantities are shown in Table 17.

The exact values for load duration will depend upon the geometry of the containment structure, and the characteristics of the energy release. However, the ranges given hold for most existing types of containment structures.

TABLE 17. Energy Release Damage Factors

Damage Factor	Type of Load	Load Duration, sec
Shock wave	Extremely high; very short duration	$10^{-5} \rightarrow 10^{-6}$
Fluid momentum	High; short duration	$10^{-2} \rightarrow 10^{-4}$
Gas bubble	Relatively low; long duration	$10^0 \rightarrow 10^{-2}$

The spatial energy distribution of the assumed release is needed in order to predict the dynamic effects of the various damage factors.

The FARET analysis is based on an assumed energy release equivalent to the energy release in a nonoxygen atmosphere of 40 lb. of TNT. At 1100 cal/g (Ref. 24) the total energy release for 40 lb. of TNT is

$$E_T = 1100 \times 454 \times 40 \times 308 \times 12 = 7.30 \times 10^8 \text{ in.-lb}$$

About 50% of this energy goes into the shock wave while the other 50% remains in the form of a gas bubble at high temperatures and pressures. The energy in the shock wave is

$$E_S = 1/2 E_T = 3.65 \times 10^8 \text{ in.-lb}$$

Assuming spherical expansion of the shock, the shock wave energy in the vertical and radial directions, respectively, is

$$E_V = 2/3 E_S = 2.40 \times 10^8 \text{ in.-lb.}$$

$$E_R = 1/3 E_S = 1.25 \times 10^8 \text{ in.-lb.}$$

Because of the dissipative nature of the shock front some of the energy is converted into heat. This amounts to approximately 10% in the vertical and 20% in the radial direction. The shock wave energy available for doing useful work, therefore, is

$$E_V = 2.16 \times 10^8 \text{ in.-lb}$$

$$E_R = 1.00 \times 10^8 \text{ in.-lb}$$

C. Energy Absorption

At this point at least two possible energy absorption analysis approaches are available to the designer. The first is to pursue the above sequence of energy partition, analyzing each FARET structural component in terms of its absorption capability until all available energy can be safely absorbed. The final step in this procedure would indicate a net energy absorption capability in excess of the remainder available for some outer FARET component either within the containment liner, or ultimately utilizing a portion of the liner capability. This procedure based on the available technical data and state of the art, would provide an analysis which would be open to many questions.

The alternate design procedure is to assume that 100% of each energy component (radial and vertical) is to be accounted for by some single absorption mechanism and to design accordingly. This procedure, while it penalizes the design efficiency, is not nearly so dependent upon interpretation or extrapolation of marginal state of the art technical data. The FARET design was based on this approach. The two absorption mechanisms analyzed are the radial energy absorber for the radial component (E_R) and the cell roof structure for the (50 ton missile) vertical component (E_V).

(1) Radial Energy Absorption

The total amount of energy directed radially from a 40 lb TNT explosion is one-third of the blast energy, or

$$\text{Radial Energy} = 1/3 \times 7.4 \times 10^8 \text{ in.-lb} = 2.5 \times 10^8 \text{ in.-lb}$$

A considerable amount of this energy is absorbed in crushing the canned graphite and rupturing the double-walled reactor vessel. It is assumed, however, for design purposes, that the radial energy absorbing shield must be capable of safely containing this entire amount through plastic stretching of the hoops and shell.

By choosing a reasonable amount of average elongation during the energy absorption process, the required mass of steel represented by the hoops and the steel liner plate can be calculated. It is felt that an average elongation of less than 5% would be conservative for A-36 steel which exhibits an ultimate elongation of about 20%. This margin of safety is intended basically to provide for nonuniformity of loading along the vertical axis of the cylinder. The portion of the shield lying below the bottom of the sodium in the reactor vessel is not considered effective in providing energy absorption. Thus the effective height of the shield is about 17.5 ft.

Under rapid strain rates the yield strength of mild steel (A-36 in this case) increases quite significantly. Since the loading due to fluid momentum and the gas bubble is of the order of 10^{-2} sec or less and a total strain, ϵ_p , of about 0.05 is anticipated, then the average strain rate is about 5 in./in. sec. It is conservative to assume a dynamic yield strength of about 40% higher than static for this strain rate. Thus, for A-36 steel, the dynamic tensile yield strength, σ_{dy} , would be $1.4 \times 36,000 = 50,000$ psi.

The specific plastic energy (E_p) for A-36 required for 5% elongation is then (assuming rigid-plastic behavior)

$$E_p = \sigma_{dy} \epsilon_p = 50,000 (.05) = 2500 \text{ in.-lb/in.}^3$$

or, for steel having a specific weight of 0.283 lb/in.³

$$E_p = 2500 \times \frac{1}{0.283 \text{ lb/in.}^3} = 8850 \text{ in.-lb/lb}$$

Neglecting the elastic energy absorbed the total amount of steel required is then

$$W_s = \frac{2.5 \times 10^8 \text{ in.-lb (total radial energy)}}{8.85 \times 10^3 \text{ in.-lb/lb (specific energy at 5% elongation)}}$$

$$W_s = 28,300 \text{ lb.}$$

Of this amount about half is represented by hoop steel and the other half by the steel shell around the shield. The hoops can be of several sizes depending upon spacing (vertically). The actual amount of steel used is greater than the weights given, basically due to the effects of large openings for piping. The actual weight of steel represented by the effective portions of the outer steel liner and the hoops is about 43,800 lbs. The actual average elongation, $(\epsilon_p)_{act}$, is calculated as

$$\begin{aligned} (\epsilon_p)_{act} &= \frac{\text{total radial energy}}{\sigma_{dy} (W_s)_{actual}} \times 0.283 \text{ lb/in.}^3 \text{ (specific weight of steel)} \\ &= \frac{2.5 \times 10^8 \text{ in.-lb}}{50,000 \frac{\text{lb}}{\text{in.}^2} \times 43,800 \text{ lb}} \times 0.283 \frac{\text{lb}}{\text{in.}^3} \\ &= 0.031 \end{aligned}$$

or an average strain of 3.1%.

Recent tests reported by the Ballistics Research Laboratory²⁸ confirm the validity of blast energy absorption by a plastic deformation mechanism. These tests consisted of exploding charges inside scale models of a typical cylindrical steel containment vessel with hemispherical heads. It is noted in this report that a test vessel of 10 ft diameter, 17.5 ft overall height and 1/4 in. wall thickness, ruptured at a charge weight of 25.75 lb of pentolite (equivalent to about 28.6 lb of TNT). Lesser charges did not cause rupture. A discussion of this test as related to FARET follows.

If the actual amount of steel hoops and liner plate in the FARET radial energy absorbing shield are considered to be a steel cylinder, the equivalent thickness of the cylinder is 1.25 in. From the premise that energy absorption is attained by a certain amount of plastic deformation, it follows that the amount of energy absorbed for a constant value of elongation is proportional to the volume of steel available to elongate. In order to obtain an indication of the ultimate capacity of the FARET shield, its capability will be compared to the BRL test vessel described in the previous paragraph. This can be done by applying an appropriate volumetric scale factor to the 28.6 lb charge of TNT which ruptured the BRL test vessel.

The scale factor is obtained by multiplying the ratios of diameter, height and wall thickness between the FARET equivalent blast shield cylinder and the BRL test vessel. Thus, the scale factor on predicted ultimate charge weight for the FARET is

$$F = \frac{D \times H \times t \text{ (FARET shield)}}{D \times H \times t \text{ (BRL test vessel)}} = \frac{16 \text{ ft} \times 17.5 \text{ ft} \times 1.26 \text{ in.}}{10 \text{ ft} \times 17.5 \text{ ft} \times 0.25 \text{ in.}} \approx 8.$$

The amount of TNT which appears necessary to completely rupture the FARET energy absorbing "cylinder" is then extrapolated from the BRL test to be $8 \times 28.6 = 228 \text{ lb}$ of TNT. This implies a safety factor, on total energy absorption capability, of about 5.7 based on a 40 lb charge.

The actual calculated average elongation for the FARET shield for a 40 lb TNT charge is 3.2%. If the safety factor of 5.7 discussed above is assumed as reasonably correct it follows that for a 228 lb TNT charge the average elongation of the FARET shield would be $5.7 \times 3.2 = 19\%$, for which value of elongation fracture of the steel hoops and shell plate would indeed be imminent.

The actual factor of safety on radial energy absorption capability of the FARET containment complex is greater than the value given above. Neglected are important energy absorption mechanisms such as crushing of the canned graphite, rupturing of the double-walled reactor vessel and strain hardening effects on the hoop steel. It may very well be that the reactor vessel would not suffer complete failure for the loads

postulated. The assessment of this energy absorption, however, is difficult to achieve due to the extremely short duration of loading and the proximity of the reactor vessel to the reactor core. Thus, the design of the shield is felt to be conservative. It is hoped that as more experimental work is accomplished on the energy absorbing mechanisms available, more accurate assessment can be made with resultant cost savings and higher levels of confidence on future reactors.

(2) Vertical Energy Absorption

Most of the problem of vertical energy absorption is related to stopping the vessel cover assembly without compromising containment. This is the vertical upward energy component. There is also a downward component, but the containment liner is heavily protected in this direction both by a thick steel and concrete mat and by the backup of solid rock under the building.

The analysis therefore was required chiefly in investigation of the behavior of the 50 ton vessel cover with its attached shielding and mechanisms. (This missile also includes the upper shielding extensions of the subassemblies.) In this analysis, it is assumed that the entire mass maintains its integrity throughout the power stroke and until the time of impact on the cell ceiling.

The overpressure attained in a closed nonoxygen volume by a TNT explosion is

$$\Delta p = 14.7 + 4000 W/V$$

where

Δp is the overpressure in psi

W is the charge weight in lbs (40)

V is the usable cavity volume in ft³ (738).

Using these figures an initial overpressure of 232 psi is obtained. At the point where the gas bubble vents into the upper cell the pressure is reduced to 183 psi (assuming an adiabatic expansion).

The velocity with which the 50 ton missile moves is obtained from two sources. The first source is the velocity imparted by the impinging shock wave. This amounts to 29 ft/sec. Since the shock wave moves so much faster than the gas bubble expansion, this shock wave velocity can be regarded as an initial velocity for the missile.

The energy provided by the power stroke of the gas bubble to the rods, sodium, and cover plate is obtained from the formula

$$E_p = \bar{p}AX$$

where

E_p is the power stroke energy in in.-lb

$\bar{p}$ is the average gas pressure (207 psi)

A is the area on which the gas bubble acts (4820 in.²)

X is the power stroke distance (13 ft)

This gives a value of 1.52×10^8 in.-lb imparted to the rods, sodium, and cover plate. Inherent in this discussion is the assumption that heat transfer between the heat source (gas bubble) and heat sinks (sodium, construction materials, etc.) is negligible. From NOL experiments on Enrico Fermi this is not the case. It turns out that nearly 50% of the energy in the gas bubble is lost in heat conduction during the expansion, thereby, reducing the pressure and power stroke energy accordingly. Hence, the actual energy imparted to the missiles is 0.76×10^8 in.-lb = 6.34×10^6 ft-lb.

The velocity at the end of the power stroke is

$$V_p = \frac{1}{12} \sqrt{\frac{2E_p g}{W} + V_0^2}$$

where

E_p = power stroke energy (6.34×10^6 ft-lb)

V_0 = initial velocity (29 ft/sec)

W = missile wt (50 tons)

g = gravitational acceleration

The velocity of the missile as it strikes the cell ceiling is

$$V_s = \sqrt{V_p^2 - 2gS}$$

where

S = cell inside height (60 ft)

These values give a striking velocity of about 29.5 ft/sec.

The energy of this missile when it arrives at the cell ceiling will be dissipated by at least three mechanisms:

1. Partial penetration of the steel liner.
2. Bending strain energy absorbed by the cell annex roof structure.
3. Energy absorbed by crushing of the missile and the concrete behind the steel liner.

Using DeMarre's formula for armor penetration (classified), it can be shown that the cell ceiling liner will not be penetrated but will be dented. Containment will not be lost under these conditions.

D. Missile Containment

The dynamic analysis of the response of the FARET cell structure to the effects of a 50 ton impact load generated from a 40 lb TNT equivalent explosion includes several assumptions and conditions necessary to effect a solution to the problem. The nature of these assumptions is intended to be conservative. As one condition of structural design importance, the maximum dynamic deflections of the cell annex roof structure are limited to approximately 10 times the yield deflection. Leading experts in the field of plastic design in steel agree that deflections up to about 40 times yield deflection will not cause structural collapse. However, in order to provide confidence in adequate structural integrity and fission product containment, a factor of safety of about 4 is assigned to this aspect of the design.

The maximum impact load upon the containment structure is defined as a 50 ton weight, consisting of the reactor vessel cover, the top shielding extensions of all subassemblies and a slug of sodium, propelled by the force of an internal explosion located at the center of the reactor core. Although the shock wave is considered to impart an initial velocity to the missile, the majority of the kinetic energy is attained during the power stroke effect of the gas bubble (internal blast pressure). The value of energy imparted to the missile is very conservatively calculated. In addition to the factors listed under "Additional Energy Absorption Mechanisms" in the following paragraphs, there are several more qualitative considerations to take into account.

First, no credit is taken for attenuation of the shock wave due to the sodium-argon interface. Second, no credit is taken for the energy absorbed by the cover latch mechanisms. Third, the missile is assumed to be a constant diameter plug, 13 ft long, which is accelerated by the expanding gas bubble over the entire distance of 13 ft. This last assumption is very conservative, as the pressure forces accelerating the missile begin to decay rapidly as soon as the cover clears the top flange of the reactor

vessel (a distance of about $2\frac{1}{2}$ ft of vertical travel of the missile). The pressure force will quickly reduce to a value associated with the frontal area of the subassemblies exposed to the expanding medium. This effect alone overestimates the total kinetic energy imparted to the missile by a factor of 1.65 since the frontal area of the subassemblies is less than half that of the cover.

The kinetic energy of the weight as it strikes the underside of the cell annex roof structure is drastically reduced by the change in potential energy of the missile, as the roof is about 60 ft above the reactor vessel. One-half of the net kinetic energy of the missile as it strikes the roof is absorbed by the cell annex structure as bending strain energy in the structure. The remaining one-half is absorbed by crushing of the various parts and indentation (but not penetration) of the liner.

If the roof structure behaves in a rigid-plastic fashion then the energy absorbed plastically by the structure is proportional to the maximum central deflection (assuming a center-span hit). Thus it follows that ideally the energy of the impact load could be as much as $40/10 = 4$ times the original design value for the overestimated missile energy, or as much as $40/10 \times 1.65 = 6.6$ times the value of the energy associated with a more realistic, though still conservative, calculation of the missile energy.

The control rods attached to the top of the 50 ton missile strike the lining first, thereby raising the question as to whether they could penetrate the lining. To answer this question, it is necessary to determine the impact velocity of these rods for which the compressive yield stress in the rods is exceeded. If this velocity turns out to be substantially less than the velocity imparted by the shock wave and gas bubble, then the control rods will collapse and buckle upon hitting the cell roof. When a moving object strikes a rigid wall with a velocity v_s the magnitude of the stress wave engendered in the object is related to the impact velocity by the formula

$$v_s = \frac{\sigma g}{2\rho c}$$

where

σ is the maximum stress in lb/ft²

ρ is the density of the material in lb/ft³ (150)

c is the wave propagation velocity in ft/sec (steel--16,000)

g is the acceleration of gravity in ft/sec² (32.2)

The dynamic yield stress of the control rods is 50,000 lb/in.² of 7.20×10^6 lb/ft². Putting in the values, the striking velocity which just produces buckling is 48.0 ft/sec. Since the actual striking velocity of the rods is 70.5 ft/sec which is substantially above the buckling velocity, the control rods will collapse upon hitting the ceiling. Hence, there will be very little, if any, penetration due to these rods.

E. Additional Energy Absorption Mechanisms

As indicated previously, the FARET design procedure was to account for 100% of both energy components (vertical and radial) by some single absorption device for each component. A measure of the minimum margin, over and above the usual allowable stress values, safety factors, and other assumptions built into this design can be obtained. There are several significant energy absorptions which can be identified readily and the sum of these components is a measure of the margin. These include the following:

1. Radial Energy

(a) Sodium: The results of related experiments²⁷ indicate that the approximate amount of shock wave energy converted into heat before it strikes the reactor vessel is 20% or $0.067 E_S$.

(b) Borated-graphite Neutron Shield: The graphite is a compressible material and has some inertia effect. Calculations show that about 10% of the shock wave energy is dissipated at this location.

From the above three mechanisms alone it is seen that more than 30% of the shock wave energy is the minimum design margin to be expected with regard to absorption of the radial energy component.

2. Vertical Energy

(a) Sodium: The amount of shock wave energy transformed into heat is about 10% or $0.067 E_S$.²⁷

(b) Subassembly Shielding Strain Energy: When the shock wave reaches the subassemblies, inelastic deformation may occur because of compression and bending. This effect dissipates approximately 10% of the shock wave energy.

(c) Subassembly and Sodium Momentum: Calculations show that about 75% of the total shock wave energy released in the upward direction goes into producing momentum of the subassemblies and sodium. A further breakdown showed that 30% of this portion goes into kinetic energy.

of the subassemblies, and the remaining 70% into sodium momentum. The amount remaining as sodium momentum generates the sodium spray which impinges upon the cover plate to produce a dynamic pressure.

It can be seen that a significant portion of the vertical shock wave energy component, more than 45%, is not credited to the design.

F. Thermal Shocking of Cell Liner

When sodium at 1000°F comes into contact with the steel cell liner plate, transient thermal stresses arise due to this shock. Calculated on an elastic basis, these are of the order of 200,000 psi. Since thermal stresses are self-limiting, they do not cause failure by rupture in ductile materials upon their first application. They may cause failure by repeated loading, or cycling (fatigue). Applying fatigue criteria specified and described in Section III of the ASME Boiler Code (including Code Case 1331) leads to a "safe" number of thermal shocks, at any one particular point on the liner, of about 20 cycles (at an average metal temperature of about 900°F). The number of "allowable" cycles increase very rapidly with reduced temperature. Since the average temperature at the shocked zone is probably below 900°F the number of allowable cycles is probably greater than 20. It is considered highly unlikely that any one point or area of the liner would be subjected to thermal shocks of this magnitude more than once in its lifetime.

2. Containment Design

A. Design Stress Basis

The design stress basis for the containment structure is that the steel and concrete design stresses at 30 psig be restricted to no more than 80% of the normal allowable stresses. For the steel reinforcement this is then 80% of 40%, or 32% of the minimum yield strength specified for the steel bars. The concrete design stress is specified as 80% of 45%, or 36% of the 28 day compressive strength of the concrete.

The factor of safety in the FARET containment structure is about 3, based on initial yielding of the steel reinforcing material. The safety factor on ultimate strength is probably greater than 3.

B. Margin of Safety

The factor of safety usually provided in an all-welded steel containment vessel is probably between 3 and 4. This safety factor is related to the ultimate strength of the vessel material; initial gross yielding of the steel shell would occur at about 1.5 times the design pressure. The reserve energy which leads to the higher safety factor (3 to 4) depends on the ductility and strain hardening characteristics of the steel.

There are significant differences between the meaning of the factor of safety as related to the type of containment structure used for FARET and that associated with a conventional containment (pressure) vessel. The most important of these differences is that the FARET containment structure consists of a series of interconnected continuous structural frames which resist pressure by bending action as opposed to the normal pressure vessel which resists pressure by direct, membrane stresses, or forces. When the loading of a continuous frame reaches the value that causes initial yielding (in bending) at the most highly stressed locations, failure of the frame is not imminent. As the load is increased further, the yielding at these isolated locations progresses until the section becomes "fully plastic" at those locations. This is tantamount to replacing the sections with a hinge which exhibits a constant rotational friction resistance to further progressive loading. Depending on the degree of restraints existing in the frame, further increases in pressure are accommodated until new "plastic hinges" form at other critical locations. Eventually the framework becomes a link mechanism and is unable (without strain hardening) to undergo further pressure increases. It is then that collapse, or failure, of the framework is imminent. This collapse load is usually much higher than that which causes initial yielding.

The ability of a continuous framework to "throw off" bending moments from highly stressed zones to less stressed zones has been observed for a long time. It has only been recently that the analytical tools and experimental verifications have permitted designers to effectively make use of this phenomenon.

It is important to note that a pressure vessel of, say, cylindrical shape, does not exhibit this additional reserve of energy absorption. Theoretically, once yielding occurs in such a pressure vessel, failure is imminent unless strain hardening or gross changes in geometry occur. (The fact that a cylindrical shape is economically superior to a rectangular shape in resisting internal pressure is not a pertinent factor in these arguments.)

An additional distinction is the manner in which failure occurs. In the case of the cylindrical vessel, failure is a sudden bursting of the vessel shell. A properly designed continuous frame, however, will fail by excessive deformation; the structure need not be disrupted at the collapse load.

3. Determination of Containment Leak Rate

An extensive effort is required to reliably determine the leak rate of any major containment structure. Certain features of the FARET containment structure tend to relieve some of the difficulties usually encountered. Nonetheless, a rigid and comprehensive testing and surveillance procedure is planned. It includes the following steps:

(a) Local inspection will be performed periodically on the liner plate and sleeve or penetration welds during construction to assure a high quality of workmanship. Such means as "spot" radiographing, dye penetrant, and vacuum box testing (using soap bubble technique) will be used.

(b) A leakage rate and structural capability test will be performed on the containment structure, prior to completion of equipment installation. This test will be made with penetrations blanked off and will therefore measure the leakage from the containment envelope plus whatever leakage occurs through the temporary penetration closures. The test pressure will be 30 psig.

(c) Each penetration will be individually leak tested at a pressure differential of 30 psi, after the equipment is installed.

(d) The value of the leakage rate for the completed system will be determined by summing corresponding leakage rates resulting from these two series of tests. As a consequence, the resulting leakage rate as determined by the tests will be somewhat higher than the actual leakage rate since it will include the leakage rate through the temporary closures.

(e) Finally, a leakage rate test on the completed system will be made at a low pressure to determine leakage characteristics at normal operational pressure differences.

The final result of these procedures should be a reliable and realistic measurement of the initial leak rate of the containment structure.

4. Periodic Leakage Rate Test Practices

All penetrations, including equipment hatches, electrical and instrumentation, airlocks, and transfer ports, will be tested periodically to ascertain that leak rates remain within acceptable limits.

In addition to tests on individual penetrations, the continuous monitoring of the inert atmosphere for oxygen will be very sensitive to changes in the total cell in-leakage rate at very low pressure differentials.

APPENDIX C

Core Meltdown Studies and Energy Yields1. Description of the AX-1 Code

The AX-1 Code was used to obtain estimates of the energy yield and explosive force of hypothetical nuclear accidents in the FARET Facility.

This code is capable of following a nuclear accident throughout the course of a severe burst, performing in a space-dependent spherical model, alternately neutronic calculations and then thermodynamic plus "hydrodynamic" calculations. Taken together, these calculations are called "hydrocycles" in the code description. The calculations can be started in such a way that one may specify the coefficient α , the inverse power period in the exponent $e^{\alpha t}$, by which the power of the burst is to increase with time. The code will do a calculation by spherical SNG transport theory, changing all radii proportionally until the corresponding excess reactivity is reached, and then turn to the "hydrocycle" portion of the code. The energy generated per unit time is calculated for each space interval of the reactor. This information, as well as the material specific heat parameters specified, is used to determine the regional increase in temperature. The regional increase in temperature, together with the corresponding change in density as a result of any initially specified, or for later time steps of calculated velocities, is the source of the calculation of the regional pressures. The equation used for pressure is of the form:

$$\text{Pressure} = (A)(\text{density}) + (B)(\text{temperature}) + T.$$

Here A, B, and T are coefficients supplied by the physicist. If T is made negative, the code will set the pressure which it calculates to zero whenever T is larger in magnitude than the sum of the first two terms. This allows one to provide a threshold which must be reached before the disassembling pressure can begin to act.

The pressure for each region is used with Newton's Law of Motion to calculate the velocities induced for each radial segment of the reactor. Shock-wave propagation, after the wave is built up, is checked by the code for the "Courant stability criterion," which records the rate of propagation of the shock front and, by addition of a shock-smearing routine, which imitates the actual physical viscosity. The calculated velocities can, in turn, produce changes in density for the various regions. These changes, along with the further generation of fission energies, yield new internal energies. The number of hydrocycles of the above type, which the code performs before returning to the SNG calculation routine, is controlled partly by the physicist and largely by the design of the code. The next SNG calculation

determines the proper, probably changed, values for α and spatial power-generation rates. These revised values are then employed on another sequence of hydrocycles.

This process will continue until the calculation has followed the accident to the point where α has become negative as a result of the displacements which have occurred and the burst is essentially over. If α is negative and the rate of power generation is also small, the code continues only the hydrocycle calculations, making no more neutronic calculations. The printout record of the code gives, as a function of time and position in the core, calculated values of pressure, temperature, and material velocities. It also gives, for various time steps: α , the rate of power generation, and the integrated power to that time, as well as data on the progress of the code. This code was used to analyze the energy releases discussed in this Appendix.

2. Description of Reactor Meltdown Code (ANL-1891/RP)

The Reactor Meltdown Code was used to investigate the dynamic behavior of the two reference cores following a positive reactivity insertion, and the corresponding positive and/or negative reactivity feedbacks from temperature-dependent mechanisms, and the degree of core collapse after melting had started.

The program couples the dynamics of melting and collapsing of the core with the reactor kinetics equation. The models used for the various changes of reactivity are incorporated into three basic types of reactivity insertions or feedbacks.

First is the externally applied reactivity, R_X . Straight ramp insertions or ramp insertions up to a time t_0 may be allowed. The forms of the equations are:

$$R_X(t) = A_{X1} + A_{X2}t \quad t \leq t_0$$

$$R_X(t) = A_{X1} + A_{X2}t_0 \quad t > t_0$$

where A_{X1} and A_{X2} are arbitrary coefficients which define the ramp insertion rates.

The second type of reactivity change considered is that which is a function of core displacement, e.g., the collapsing of the core. This is labelled R_C . The equations are of the form:

$$R_C = A_{C1}(\Delta H) + A_{C2}(\Delta H)^2$$

where ΔH is the change in active core height, and A_{C1} and A_{C2} are collapse coefficients chosen to fit a predetermined curve.

The reactivity addition due to core collapse is determined from a series of static reactor calculations as a function of reactor height. The model used in these computations assumes that the melted segment of the fuel is uniformly distributed over the remaining core height, with an equal volume displacement of coolant.

Fig. 23. Reactivity of Collapse vs Change in Core Height

a single oxide fuel pin experiment in the TREAT reactor. (It has been shown however that this is a relatively minor term and could be neglected without significant effect on the analysis.)

The third reactivity insertion considered is dependent on the change of temperature, e.g., fuel and coolant and Doppler effects. The code at present includes only the Doppler effect, R_D . The temperature dependence of the Doppler reactivity is obtained from a series of static calculations involving Doppler-broadened cross sections for several fuel temperatures.

Figure 24 shows the temperature dependence of the Doppler reactivity for the two reference cores. A good fit was obtained with the relation:

$$R_D = A_{D1}[1 - \exp(-A_{D2}\Delta\theta)]$$

Figure 23 shows the reactivity of collapse as a function of change in core height for the two reference cores.

The time dependence of collapse is determined through a modified gravitational equation of motion:

$$\frac{d^2x}{dt^2} + \xi \frac{dx}{dt} = -g$$

which allows the collapse to occur at an acceleration less than g . The "dynamical friction" term, $\xi(dx/dt)$ is included to account for cores containing closely-packed fuel elements. An assemblage of fuel pins may behave quite differently from a molten isolated pin. For purposes of the FARET analysis, the coefficient was estimated to be 0.3. This estimate was based on

where $\Delta\theta$ is the averaged fuel temperature rise, and A_{D1} and A_{D2} are coefficients chosen to obtain a good fit.

Fig. 24. Doppler Reactivity vs Temperature Rise

The code computes power and temperature distributions at each time interval. The time dependence of the reactor power, with one group of delayed neutrons, is computed by the usual neutron kinetics equations. The power distribution is assumed to have a cosine shape in the axial direction, with a flat radial distribution. The core is divided axially into 40 segments of equal size. When a segment melts and collapse occurs, the code recalculates the temperature distribution over the new height. The power and temperature at each time are used to compute the reactivity feedback through the equations defined above. The reactivity insertions are then fed back into the neutron kinetics equations.

3. Small Core Excursion Analyses

A. Ramp Insertions

The meltdown program allows the study of a range of ramp insertions. The final result, however, insofar as input energy and inverse power period is concerned, is independent of the initiating events. For example, a ramp of \$20/sec could result from a variety of causes such as coolant voiding, dropping of a fuel subassembly and a control rod removal. The method of reactivity insertion is unimportant. Only the rate of ramp insertion related to the specific cause will determine the final results as computed by the code.

Meltdown studies have been made for a series of positive reactivity insertion rates ranging from \$3/sec to \$20/sec. The results for the small core are summarized in Table 18. The range of insertion rates was established on the basis of the following considerations:

(1) The control rod worth was estimated to be $0.004 \Delta k/\text{ft}$. By assuming a control rod removal rate of 6 ft/sec , a positive reactivity insertion rate of $0.0243 \Delta k/\text{sec}$ is obtained. With a β_{eff} of 0.00274 , the rate is equivalent to approximately $\$9/\text{sec}$.

(2) The reactivity insertion rate of $\$20/\text{sec}$ was obtained by using an average worth of $0.009 \Delta k/\text{ft}$ for the fuel subassembly, and an average free-fall velocity of 6 ft/sec through the core.

TABLE 18. Summary of Meltdown Studies--Reference Small Core

$$\beta_{\text{eff}} = 0.00274$$

$$L = 0.655 \times 10^{-6}$$

External Reactivity, $\$/\text{sec}$	Ramp Break, sec	Time Melting Starts, sec	Melting Time, sec		Rnet Melting Starts	Rnet Melting		$\Delta k_{\text{avg}}/\Delta T$ during Melting	α , μsec^{-1} at 87.5% Melt	Power Density, watts/cm^3 at 87.5% Melt	Energy Density, joules/cc at 87.5% Melt	Average Doppler Coefficient, $\Delta k/\Delta T$ (750-2500°K)
			50% Melt	87.5% Melt		50% Melt	87.5% Melt					
3	0.608	0.388200	0.006058	0.010010	0.002733	0.002965	0.003320	0.0586	1.0×10^{-3}	6.7×10^6	1.7×10^4	-0.22×10^{-6}
3	0.608	0.367235	0.002800	0.007634	0.002790	0.002844	0.003146	0.0466	7.0×10^{-4}	4.3×10^6	1.6×10^4	-0.11×10^{-6}
3	0.608	0.352648	0.001482	0.003200 ^(a)	0.002899	0.002926	0.003000 ^(a)	0.0315 ^(a)	$4.6 \times 10^{-4(a)}$	$3.1 \times 10^6(a)$	$1.5 \times 10^4(a)$	0
4	0.456	0.291318	0.004714	0.009382	0.002735	0.002872	0.003267	0.0567	9.2×10^{-4}	5.7×10^6	1.6×10^4	-0.22×10^{-6}
4	0.456	0.277518	0.002282	0.006534	0.002813	0.002853	0.003077	0.0404	5.7×10^{-4}	4.3×10^6	1.7×10^4	-0.11×10^{-6}
5	0.365	0.233250	0.003707	0.008042	0.002738	0.002837	0.003103	0.0454	7.1×10^{-4}	3.5×10^6	1.5×10^4	-0.22×10^{-6}
5	0.365	0.217272	0.000950	0.0017 ^(a)	0.002977	0.002994	0.003055 ^(a)	0.0170 ^(a)	$4.6 \times 10^{-4(a)}$	$4.3 \times 10^6(a)$	$1.3 \times 10^4(a)$	0
20	0.22	0.060778	0.000905 ^(a)	0.0017 ^(a)	0.003333	0.003382 ^(a)	0.003427 ^(a)	0.0549	$1.0 \times 10^{-3(a)}$	$1.9 \times 10^7(a)$	$2.1 \times 10^4(a)$	0

^(a) Extrapolated values.

Note: In all cases, $P_0 = 1.2 \times 10^3 \text{ watts}/\text{cm}^3$.

Core volume = 41.7 liters.

The results of the meltdown computation indicate (see Table 18) that for reactivity insertion rates below $\$5/\text{sec}$, the melting process will begin 200 to 400 msec after the start of the external insertion. The time for 50 and 87.5% of the core to melt ranges from 1 to 10 msec.

When the negative feedback of the Doppler coefficient is neglected, the time required to melt 87.5% of the core is 1.7 msec and 3.2 msec for reactivity insertion rates of $\$5/\text{sec}$ and $\$3/\text{sec}$, respectively. (See Table 19.) These are the shortest melting times involved, and the reactivities attained are the largest. For the case of an insertion rate of $\$5/\text{sec}$, the reactivity due to collapse is small, so that during the melting process the rate of reactivity associated with the $\$5/\text{sec}$ insertion is not altered. However, for the lower ramp insertion rate of $\$3/\text{sec}$, the reactivity due to collapse is effective in increasing the rate during the melting process to approximately $\$10/\text{sec}$. (See Table 18.) Therefore, it appears that the lower rates of external reactivity insertion could lead to higher rates of insertion during the melting and reassembly processes.

TABLE 19. Calculated Average Doppler Coefficient

Temperature Range, °K	Doppler Coefficient ($\Delta k_{\text{eff}}/\text{°K}$) $\times 10^{-6}$
<u>Small Core</u>	
300-750	-0.43
300-1500	-0.37
300-2500	-0.30
750-1500	-0.33
750-2500	-0.26
1500-2500	-0.22
<u>Zoned Core (Coefficient in test zone only)</u>	
300-750	-1.53
300-1500	-1.08
300-2500	-0.84
750-1500	-0.81
750-2500	-0.67
1500-2500	-0.56

Fig. 25. Inverse Power Period vs Time for Ramp Reactivity Input of $3\text{¢}/\text{sec}$ in Reference Small Core. (Curves terminate when 87.5% of core has melted.)

To study the latter effect, a series of computations was made with the introduction of negative Doppler coefficients having different magnitudes. The temperature-dependent negative feedback was introduced to offset the initial positive reactivity insertion. The resulting rate of insertion during the melting process confirms the conclusions stated for the no-Doppler case. That is, in the small core analysis, a negative feedback having these orders of magnitude could result in a greater reactivity contribution from the collapsing of the core. The Doppler effect can be seen in Fig. 25, where the inverse power period, α , is plotted as a function of time. The magnitude of the Doppler effect is not large enough to effect a reactor shutdown. In Fig. 26 are presented the collapse reactivity for varying degrees of melt in systems computed with and without the Doppler effect.

A ramp insertion rate of \$20/sec will cause the core to start melting 60 msec after the initial insertion. In about 2 msec, the core would be 87.5% molten. The short-time interval of melting would not allow any significant reactivity to be inserted by collapse.

The inverse power period is minimum for this case, and the average reactivity insertion rate during melt is the same as the initial input.

Fig. 26. Reactivity of Collapse vs Percent of Core Meltdown for Reference Small Core

entire 41.7-liter core. The energy required to sublime the oxide fuel in the core was computed to be 4×10^8 joules (1×10^8 cal), assuming a heat of sublimation of 2000 joules/g (Ref. 26). Therefore, when 87.5% of the core is in this highly superheated liquid or molten state, the energy input is of the same order of magnitude as the energy required to vaporize the core. Consequently, because of the power and temperature distributions and high pressures generated, it is expected that disassembly would take place some time before the core is 87.5% molten.

A plot of the inverse period as a function of total energy density is presented in Fig. 27. For all ramp cases considered, the inverse power period, α , reaches a maximum of approximately $1 \times 10^{-3}/\mu\text{sec}$.

To estimate the energy available to do work during core disassembly, the total energy yield as a function of α was computed from the AX-1 Code. The results are presented in Fig. 28. For the small core with an α of the order of 1×10^{-3} per μsec , the total energy yield is approximately 7×10^7 joules (1.67×10^7 cal).

The effect of the negative feedback is to increase the melting time and, hence, allow a greater degree of core collapse to occur. In all of the cases studied, the net reactivity did not exceed \$0.22 above prompt critical when 87.5% of the core was molten.

The shortest power period occurs with the maximum negative feedback. The inverse periods, α , at 87.5% melt for all the cases studied are included in Table 18.

The meltdown code computes the total energy input during the excursion. At 87.5% melt, the total energy input is of the order of 2×10^4 joules/cc of fuel (see Table 18), or 4.2×10^8 joules (1×10^8 cal) for the

Fig. 27. Inverse Power Period vs Energy Density for Reference Small Core

Fig. 28. Total Energy Available vs Inverse Power Period for Reference Cores

A computation was also performed in which the fuel temperature at which pressure would begin to rise in the core region was taken as 4400°C for comparison with those based on 6700°C. A temperature of 4400°C is approximately 1000 to 1200°C above the boiling temperature of the oxide fuel. The energy available to do work was reduced to 60% of that at the higher temperature (6700°C), whereas the maximum kinetic energy remained the same. This behavior appears reasonable, since reducing the temperature in the pressure equation allows the core to disassemble sooner, but the amount of total energy converted into kinetic energy remains the same. The lower temperature is equivalent to a slight increase in the core density in the equation of state used in the AX-1 computation. The density was increased from 5.2 to 6.5 g of fuel/cc of core.

Taking into account the temperature effect upon the calculated total energy yield, the energy available to do work for $\alpha = 0.001/\mu\text{sec}$ is now reduced from 7×10^6 joules to about 4×10^6 joules.

B. Small Reactivity Insertions

The effects of a slow meltdown were studied in order to evaluate the type of accident in which melting of the core occurs for a period prior to prompt critical and is followed by the collapse of the core. An accident of this type might be initiated by loss of coolant flowrate to the reactor, severe local perturbations in flow, or failure of experimental fuel subassemblies.

In order to estimate effects resulting from an accident of this type which increases the melting time, and thereby the extent of core

Fig. 29. Inverse Power Period, Power Density, and Energy Density vs Time for Reference Small Core. (Doppler effect excluded.)

collapse, a constant low inverse power period was introduced initially. The Doppler feedback was neglected. As shown in Fig. 29, the time required for 70 and 100% of the core to melt is about 15.5 msec and 17 msec, respectively. The inverse power period attained due to core collapse is about 2 to $3 \times 10^{-3} \mu\text{sec}$ at the input energy density of 2×10^4 joules/cc of fuel. The conditions for energy yields in this "slow meltdown" accident are quite similar to the ramp insertion cases. Work energy yields of about 6×10^6 joules can be expected.

In addition, if the initial power level is low, the inverse power period would increase by 10%, and the work energy would increase to about 7×10^6 joules. It should be noted that computations indicate that energy yield is not particularly sensitive to initial power level.

4. Zoned Core Analyses Excursion

A. Ramp Insertions

Meltdown studies were performed also for a typical zoned system. Positive reactivity ramp insertions of $\$2/\text{sec}$, $\$10/\text{sec}$, and $\$20/\text{sec}$ were assumed. The results, with and without Doppler feedback, are presented in Table 20.

The range of insertion rates was established on the basis of the following considerations:

(1) The control rod worth per linear foot of rod was estimated to be $0.00167 \Delta k/\text{ft}$. Again, using a control rod removal rate of 6 ft/sec results in a positive reactivity insertion rate of $0.01 \Delta k/\text{sec}$, or approximately $\$2/\text{sec}$, assuming a β_{eff} of 0.0066 .

(2) The range of reactivity insertion rates for the free fall of a fuel subassembly is from $\$6/\text{sec}$ to $\$20/\text{sec}$, with a more probable rate being somewhat lower. These values are based on the pessimistic assumption that a fuel subassembly of maximum worth would be involved in the accident.

TABLE 20. Summary of Meltdown Studies--Reference Zoned Core

$$\beta_{\text{eff}} = 0.0066$$

$$\lambda = 0.89 \times 10^{-6}$$

External Reactivity, \$/sec	Ramp Break, sec	Time Melting Starts, sec	Melting Time, sec		R _{net} Melting Starts	R _{net} Melting		$\Delta k_{\text{avg}}/\Delta T$ during Melting	α , μsec^{-1} at 87.5% Melt	Power Density, watts/cm ³ at 87.5% Melt	Energy Density, joules/cc at 87.5% Melt	Average Doppler Coefficient, $\Delta k/\Delta T$ (750-2500°K)
			50% Melt	87.5% Melt		50% Melt	87.5% Melt					
2	1.00	0.52648	0.00126	0.0024(a)	0.006924	0.006942	0.006958(a)	0.014	$4.3 \times 10^{-4}(a)$	5.8×10^6	$1.6 \times 10^4(a)$	0
2	1.00	0.58523	0.01203	0.02083	0.006608	0.006774	0.006803	0.00934	2.7×10^{-4}	3.1×10^6	2.0×10^4	-0.64×10^{-6}
10(b)	0.40	0.12398	0.00070(a)	0.0012	0.008156	0.008282(a)	0.008235(a)	0.066	$1.9 \times 10^{-3}(a)$	$4.6 \times 10^7(a)$	$2.8 \times 10^4(a)$	0
10	0.40	0.11332	0.00060	0.00081(a)	0.007454	0.007492	0.007507(a)	0.066	$1.2 \times 10^{-3}(a)$	$1.5 \times 10^7(a)$	$1.7 \times 10^4(a)$	0
10	0.40	0.11509	0.00383	0.01043	0.006533	0.006676	0.006764	0.022	2.2×10^{-4}	3.3×10^6	2.3×10^4	-0.64×10^{-6}
20(c)	0.152	0.056885	0.000456	0.00080(a)	0.007482	0.007542	0.007587(a)	0.132	$1.2 \times 10^{-3}(a)$	$5.9 \times 10^7(a)$	1.5×10^4	0
20	0.152	0.060	-	-	0.007900(a)	-	-	0.132(a)	2.0×10^{-3}	-	-	0
2(d)	1.00	0.51872	0.002366	0.00567	0.006823	0.006858	0.006922	0.0175	3.7×10^{-4}	7.7×10^5	3.1×10^3	0
10(d)	0.40	0.11121	0.000889	0.001755	0.007315	0.007378	0.007431	0.0661	9.7×10^{-4}	3.1×10^6	3.8×10^3	0
10(d)	0.40	0.11194	0.000853	0.002263	0.007069	0.007050	0.007030	-0.0172	5.2×10^{-4}	1.7×10^6	3.3×10^3	-0.64×10^{-6}

(a) Extrapolated values.

(b) $P_0 = 1.4 \times 10^{-4}$ watts/cm³; in all other cases, $P_0 = 1.4 \times 10^2$ watts/cm³.(c) $P_0 = 3.13 \times 10^3$ watts/cm³.

(d) Melting temperature of fuel 1150°C. Core volume = 460 liters.

(3) The spatial dependence of the sodium expansion coefficient for the zoned system is presented in Fig. 30. The estimated magnitude and sign indicates that a postulated loss of coolant in the test zone only could result in melting of the fuel. Assuming that the sodium could be expelled from the test zone in 600 msec to 30 msec, the average reactivity insertion rate would be in the range of \$1/sec to \$20/sec.

Fig. 30. Averaged Sodium Coefficient per Unit of Radial Area vs Radius for Reference Zoned Core

The magnitude of the Doppler coefficients used in the code computations was taken as 1/3 of the calculated values listed on the figures and tables. This reduction was made since (1) allowances were made for the uncertainty in the magnitude which could be associated with the actual experiment; and (2) the use of the full Doppler coefficient resulted in reducing the excursion power levels to values which could be interpreted as a reactor shutdown. For the ramp cases studied, the positive reactivities resulting from core collapse are negligible. However, in the case of \$2/sec ramp insertion, the contribution of collapse is greater by a factor of 3 to 4 times than in the \$10/sec case.

As shown in Table 20, the existence of the Doppler effect results in reducing the external input during the melting process. In the case of \$2/sec ramp insertion, the $\Delta k/\Delta T$ during melt becomes \$1.40/sec; for

the \$10/sec ramp, the rate becomes \$3/sec. The respective inverse power periods are lower in both cases than for the case where the Doppler effect is not considered.

In all cases when the Doppler effect was neglected, the rate of reactivity insertion during melting remained the same as the external input.

The overall conclusion which can be made for the zoned system is that for the external insertion rates studied, the fuel melts but negligible reactivity is introduced by the collapsing process. In comparison to the small core, the zoned core is of greater height, and the worth of collapse per unit height is of less importance. It should be noted that the maximum net reactivity insertion does not exceed \$0.25 above prompt critical.

The inverse power periods as a function of time are presented in Figs. 31 and 32. In the two cases where Doppler effect was included, the inverse power period became negative, but the magnitude was not large enough to reduce the power level to zero. In Fig. 32, the power versus time is included to observe the Doppler effect. The power is reduced somewhat, but then increases and the core melts.

Fig. 31. Inverse Power Period vs Time for Ramp Reactivity Input of \$10/sec in Reference Zoned Core. (Curves terminate when 87.5% of core has melted.)

Fig. 32. Inverse Power Period and Power Density vs Time for Ramp Reactivity Input of \$2/sec in Reference Zoned Core. (Curves terminate when 87.5% of core has melted.)

The inverse period for the \$20/sec ramp for two power levels was studied. When the reactivity insertion is initiated at high power densities, the inverse power period is decreased by a factor of 2.

The last three cases listed in Table 20 are the results of the meltdown code analyses which assumed that the severity of a nuclear accident is due primarily to the behavior of the metallic driver zone. For these cases, the melting temperature of the uranium metal (1150°C) was used. Hereafter, these cases are identified as metallic-fueled cores. In the former cases, the melting temperature used was that of the oxide-fueled cores (2750°C).

For the metallic-fueled cores, the fuel temperatures at an energy input of 2×10^4 joules/cc of fuel are usually low. In such cases, the inverse power period used is that when the average temperature of the fuel is approximately equal to the boiling temperature of the metal fuel (4100°C). For the oxide-fueled cores, boiling temperatures of $\sim 3300^{\circ}\text{C}$ were attained when the energy input of 2×10^4 joules/cc of fuel was reached. For the ramp insertions studied, the inverse power period was essentially insensitive to either energy input or average fuel temperature. For this preliminary analysis, the energy input is considered appropriate.

When the case for the $\$2/\text{sec}$ reactivity insertion rate is compared with $\$2/\text{sec}$ ramp associated with the oxide fuel, the metallic-fueled core starts to melt about 7 msec sooner. The net reactivity inserted at the start of melt is $\$0.035$ above prompt critical, as compared to $\$0.052$ above prompt critical for the oxide-fueled core. Therefore, the time required to attain the same degree of melting is longer for the case of the metallic-fueled core. The longer time interval allows a greater portion of the core to collapse and, hence, a larger positive reactivity insertion during the melting process. As shown in Table 20, the reactivity insertion rate during melt is the same as the externally applied rate of $\$2/\text{sec}$ for the oxide-fueled core, whereas in the metallic-fueled core, the average insertion rate has been increased to $\$2.60/\text{sec}$.

The power periods in both cases are approximately the same. The total energy input for the metallic-fueled core is lower by a factor of approximately 5 when 87.5% of the core is molten. Extrapolation to an energy input of 2×10^4 joules/cc of fuel gives an inverse power period of $6 \times 10^{-4} \mu\text{sec}$. This is a slight increase over the inverse period of $4.3 \times 10^{-4} \mu\text{sec}$ for the oxide-fueled core.

A similar study was performed for a $\$10/\text{sec}$ reactivity ramp insertion rate without Doppler effects. This study indicates that although the required melting time is increased, the initially applied reactivity insertion rate still predominates and core collapse is negligible. The total energy input is lower by a factor of 5 than that of the oxide-fueled core. Extrapolation to an energy input of 2×10^4 joules/cc of fuel gives an inverse power period of $1.2 \times 10^{-3} \mu\text{sec}$.

For comparison, the \$10/sec insertion rate cases were studied with the Doppler effect included. The results indicate that the net Doppler feedback contribution during the melting process is not sufficient to lower the net reactivity inserted as the core melts. As shown in Table 20, the net reactivity is above prompt critical and decreases slightly or is constant during the melting process. In the oxide-fueled core, the net reactivity is below prompt critical and increases slightly during the melting process. Extrapolation to an energy input of 2×10^4 joules/cc of fuel gives an inverse power period of approximately $7 \times 10^4/\mu\text{sec}$. In both cases, the reactivity added due to core collapse is negligible.

For all the cases studied, the maximum inverse power period is of the order of $2 \times 10^{-3}/\mu\text{sec}$. The average energy input is of the order of 5×10^9 joules (1×10^9 cal). The energy required to sublime the metal fuel in the driver zone and the oxide fuel in the test zone is about 3×10^9 joules (7.5×10^8 cal). This is based on a heat of vaporization of 2000 joules/g (Ref. 28). The total energy input into the fuel is of the same order of magnitude as the energy required to vaporize the core.

As in the case of the small core, the core disassembly should be expected to take place some time before the core is 87.5% molten, provided that the energy input is approximately 2×10^4 joules/cc of fuel. In Fig. 33, the inverse power period is plotted as a function of total energy input for the ramp insertions studied. For all cases, α is less than $2 \times 10^{-3}/\mu\text{sec}$.

Fig. 33. Inverse Power Period vs Energy Density for Reference Zoned Core

To estimate the energy yield during core disassembly, the same procedure was followed as was used in the small core studies. For an α in the range of $2 \times 10^{-3}/\mu\text{sec}$, the total energy yield is of the order of 7×10^8 joules (1.7×10^8 cal).

Assuming a 10% efficiency for converting heat energy into mechanical energy, and that the core begins to disassemble when the fuel vapor temperature is about 1000 to 1200°C above the boiling point, the energy available to do work is estimated to be about 5×10^7 joules.

B. Small Reactivity Insertions

In order to observe the effects of a "slower meltdown" and a higher degree of core collapse, a

constant inverse power period of $9 \times 10^{-6}/\mu\text{sec}$ was initially introduced into the zoned system. The melting temperature used was that of the metal

Fig. 34. Net Reactivity Insertion, Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core. (Initial power period = 0.11 sec.)

core resulted in an inverse power period of less than $2 \times 10^{-3}/\mu\text{sec}$ at an energy input of about 2×10^4 joules/cc of fuel.

Also considered was a "slow meltdown" incident in which the fuel in the test zone only was allowed to collapse. The melting temperature of the oxide fuel was used. The time required for complete melting of the fuel was about 55 msec. As evidenced in Fig. 37, the initial inverse period of $4 \times 10^{-6}/\mu\text{sec}$ was increased to about $1 \times 10^{-3}/\mu\text{sec}$ at an energy density input of 2×10^4 joules/cc of fuel. The estimated work energy in this case is about 4×10^7 joules. For low initial power levels, the estimate is 5×10^7 joules.

5. Other Core Analyses

The cases presented in Fig. 27 and 33 were for the purpose of indicating the general trend of ramp insertions with and without Doppler feedback. Other cases studied included \$3/sec, \$4/sec and \$5/sec with varying magnitude of the Doppler feedback. The results of these cases are shown on Fig. 38. Generally, the behavior and the conclusions drawn are essentially the same as for the reference cores.

As shown in Fig. 34, the time required for 100% of the core to melt is about 23 msec. The input energy density of 2×10^4 joules/cc of fuel was reached in 29 msec. The inverse power period at this time is approximately $1 \times 10^{-3}/\mu\text{sec}$.

However, extrapolation to a fuel temperature of about 4100°C could increase the inverse power period to about $2 \times 10^{-3}/\mu\text{sec}$. Using the higher of the two periods, the estimated energy yield becomes about 5×10^7 joules. In addition, if the initial power level was low, the inverse period could increase by about 70% and the work energy would increase to about 6×10^7 joules.

The results plotted in Figs. 35 and 36 pertain to cases where the initial power periods were increased to 0.55 sec and 1.7 sec, respectively. In both cases, the gravitational collapse of the molten

Fig. 35. Net Reactivity Insertion, Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core. (Initial power period = 0.55 sec.)

Fig. 36. Net Reactivity Insertion, Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core. (Initial power period = 1.7 sec.)

Fig. 37. Inverse Power Period, Power Density, and Energy Density vs Time for Reference Zoned Core. (Collapse of fuel in test zone only.)

Fig. 38. Small Core Meltdown.

For the range of ramp rates and the range of negative feedbacks studied, the inverse power period, α , at an input energy of 2×10^4 joules/cm³, could reach a maximum of about 2×10^{-3} /μsec. The energy yield to be computed should then correspond to this inverse power period.

It should be noted for the small core that as the ramp rate is increased, the Doppler becomes less operative in allowing an appreciable reactivity insertion by core collapse. For the zoned core the same phenomenon prevails with the added effect that the core collapse contribution is much lower because the large core (low neutron leakage) is less sensitive to fuel movement.

If ramp insertions terminate at a time such that the total reactivity inserted is less than prompt critical, and if the magnitude of the temperature feedbacks are those used, then shutdown could occur before melting takes place. For example a \$2/sec ramp was inserted for a time interval of about 0.4 sec. The fuel temperature rise at about a second later was only about 150°C. The inverse power period was decreasing rapidly indicating that the magnitude of the Doppler feedback was sufficient to reduce the power and eventually shutdown the reactor before melting occurred.

In general the studies indicate that a reactor would shutdown before fuel melting takes place if ramp insertions are terminated (i.e., the reactivity is not continuously added) and the net reactivity feedbacks before melting temperatures are reached are sufficient to overcome the net externally applied reactivity.

The maximum energy release corresponds to the maximum inverse power period ($\alpha = 2 \times 10^{-3}$ /μsec) when a total input energy to the fuel reached 2×10^4 joules/cc. The maximum energy yield occurs for the large zoned system. Several types of incidents might lead to a maximum inverse power period of 2×10^{-3} /μsec, e.g., the loss of coolant, or the dropping of a fuel subassembly.

The loss of coolant can be considered in two ways. First, the coolant may be expelled from the test zone in time intervals ranging from 600 msec to 30 msec. With a positive void coefficient the average reactivity insertion rate would then be in the range of \$1/sec to \$20/sec, respectively. This corresponds to the case studied for a \$20/sec ramp insertion. The inverse power period resulting from this insertion was slightly less than 2×10^{-3} /μsec and the energy yield corresponding to this period is about 5×10^7 joules.

Second, assume that a loss of coolant occurs in such a manner that the power rises very slowly. The fuel heats and eventually melts. The reactivity insertion now occurs only from the collapsing core. The most pessimistic situation is presented in Fig. 36. The reactivity insertion from

core collapsing results in an inverse power period of about $2 \times 10^{-3}/\mu\text{sec}$ at an input energy density of 2×10^4 joules/cc of fuel. The corresponding energy yield for this case is 5×10^7 joules.

6. General Comments

The calculated energy yields are believed to be pessimistic in view of the qualifications to the parameters used in the computations. For example, the only negative feedback employed was that due to the temperature-dependent Doppler effect. However, the time scales involved indicate that "delayed effects" could be operative and may, in most cases, mitigate the severity of the accidents. For example, the sodium coefficient is large and negative in the small core and in the driver section of the zoned core; this mechanism could terminate some possible excursions. However, it was not included in the meltdown code; that is, the expulsion or boiling-off of the sodium was not considered. In the zoned system, the metal driver has a reliable and significant negative fuel expansion coefficient. This effect also was not included in the meltdown code.

These additional effects will be investigated and the results will be included in the final safety analysis for FARET.

7. Sensitivity of AX-1 Results to Parameter Changes

Several equations of state were used for the mixed-oxide and metal fuels and estimating the energy yields for several core configurations.

In the AX-1 formulation, the equation of state has the following form:

$$p = \alpha\rho + \beta\theta + \tau$$

where θ is the temperature in keV, p is the pressure in megabars, ρ is the density in g/cm^3 . The constants α and β are related to the materials or mixture of materials. The constant τ is chosen to provide a threshold which must be reached in order for the pressure to start to disassemble the core.

The parameters used for the oxide fuel were initially estimated from thermodynamic considerations involving the compressibility, elastic modulus, velocity of sound and specific heat. These parameters were then compared with those obtained from the equations of state estimated by R. D. Johnson of ANL³⁰ and by R. A. Meyer and B. E. Wolfe.³¹ In these estimates the critical constants and their interrelationship are extended from known experimental data into regions of high temperatures and pressures. The parameters obtained were different.

A study in which the various parameters were used indicates that the resulting total energy yield was somewhat insensitive within this range of variation. A partial listing of the energy yields and the corresponding parameters for a maximum inverse power period of $2 \times 10^{-3}/\mu\text{sec}$ is presented below.

α	β	τ	Total Energy Yield MW-sec
0.0164	144	-0.1720	~80
0.035	131	-0.2600	~80
0.0124	46	-0.0923	~120 from Ref. 1
0.0042	16	-0.0313	~160 from Ref. 2

A similar conclusion can be stated for the zoned system. The constants used for the uranium metal fuel were those recommended by Stratton³² and those estimated from thermodynamics. In this case the energy yields from the different equations of state for the metal fuel (driver zone) were evaluated for a variety of combinations with the oxide fuel parameters. For a maximum inverse power period of $2 \times 10^{-3}/\mu\text{sec}$ the total energy yield ranged from 500 to 1000 MW-sec. A 10% efficiency factor would then give a range of energy to do work from 50 to 100 MW-sec.

The energy yields computed for the preliminary FARET core loading³³ were found to be within the range, as predicted, of the small core and zoned system. For the inverse power period of $2 \times 10^{-3}/\mu\text{sec}$ to $3 \times 10^{-3}/\mu\text{sec}$ the estimated energy yield is 330 MW-sec. Using a 10% conversion factor, the energy available for mechanical work becomes about 33 MW-sec. This would correspond to a loss of coolant incident.

The average neutron speed or speeds, corresponding to a one group or a multigroup analysis, respectively, is a required input parameter for the AX-1 Code. The effect on the total yield was found to be about a 20% decrease in yield for a factor of two increase in the average speed. The maximum speeds used in the analysis are considered to be underestimates so that the computed yields are on the conservative side.

APPENDIX D

Criteria Related to Safety

The complete FARET design criteria appears in Volume I of this report. The purpose of this Appendix is to provide only the safety criteria. Almost all criteria could somehow be thought of in terms of safety. However, some criteria is primarily related to safety. Such criteria has been segregated herein.

1. Seismic

a. The FARET Facility is located in a region designated as Zone 2 of the Uniform Building Code. Structures, systems, and components important to safety will be designed such that the plant will maintain its capability of shutting down, removing decay heat, and be safely contained for seismic requirements twice that of Zone 2 in direct combination with other applicable loadings, such as operating loads, live and dead loads, and loads due to design and transient plant conditions. The effects of seismic and accident loading shall not be considered simultaneously, except as noted in Item 2.e. of the containment structure criteria.

b. Specifications for the primary system components including the reactor vessel, heat exchangers, and pumps require that the fundamental period of vibration for those components lie outside the range of 0.03 to 1.0 sec, which is the range generally associated with maximum response to earthquake motions.

c. Dynamic analyses of critical sodium piping will be performed in order to assure that deflections and stresses are appropriately considered.

d. The structures and equipment important to safety, i.e., required to remain functional during and following an earthquake, will be reviewed by a recognized seismic expert to determine the need for a dynamic analysis for seismic conditions.

e. The applicability of the FARET seismic criteria will be evaluated by a recognized seismic expert.

f. The piping is supported in a conventional manner, utilizing constant load hangers for the major piping. In the event the detailed seismic analysis of the piping results in values of combined stresses higher than those permitted by code, consideration will be given to the use of hydraulic snubbers to obtain a more favorable stress distribution in the piping loops. Consideration will also be given to the use of additional supports and/or relocation of existing supports to reduce seismic stresses, if necessary.

g. The piping systems utilized in FARET are all designed for low stresses due to pressure, weight and other sustained steady-state loads. In these piping systems the thermal stresses predominate the design, with the result that an increase of about 33% in allowable stresses (for combined steady-state and transient external loads) readily permits tolerating high seismic factors.

h. The primary system components--reactor vessel, heat exchangers, and pumps--will be designed and supported to withstand seismic forces due to horizontal accelerations of 0.2 g. The specifications for this equipment require that the fundamental period of vibration be calculated for the components, and further require that this period of vibration lie outside the range of 0.03 sec to 1 sec, which range is generally associated with maximum response to earthquake ground motions. The criteria established by the seismic consultants will be utilized in the design of the primary system components, if such use results in design coefficients greater than 0.2 g.

2. Containment Structure

a. The containment structure will be housed within a conventional metal prefabricated building. During reactor operations, reactor fuel handling operations, movement of irradiated fuel, or whenever forced sodium coolant circulation is required, the pressure in this building will be maintained subatmospheric in order to prevent ground release of any radioactivity which may have been released from the containment structure. The air from this building will be monitored and discharged through a 200 ft stack via the building exhaust system. In the event of high radioactivity the air will be routed through filters via the emergency exhaust system. These filters will be of the highest efficiencies obtainable and will be tested periodically.

b. Except for sodium sampling stations, argon system, etc., the entire reactor primary system together with all auxiliary systems which may contain radioactive process fluids will be enclosed within a containment structure to provide a barrier to radioactivity that might be released from the reactor primary system. The containment structure will include the cell, cell annex, the reactor and wet fuel storage tank cavities, and the equipment vault. Atmospheric seals are provided between cell and cavity, cell and vault, and cell and cell annex to separate the gas atmospheres of the three regions during normal operation.

c. The containment structure will consist of an inner steel containment liner surrounded by several feet of reinforced concrete. In the area of the cell and cell annex, a continuous steel plate will be located on the outer surface of the cell wall, connected by web plates to the inner liner plate to form a series of continuous steel frames. The concrete

between the two steel liners will act principally as biological shield. The inner steel liner functions as a leak-tight membrane throughout the containment.

d. The containment will be designed to accommodate missiles and blast effects resulting from a 40 lb TNT explosion at the center of the reactor core.

e. The containment and those systems and components associated with maintenance of containment will be designed to withstand the temperature and pressure following the maximum postulated accident (including the results from the expulsion of sodium from the reactor vessel) coincident with the seismic design loadings defined in Item 1.a. The steel liner will be designed to withstand a sodium spill and maintain its leak-tight integrity.

f. The containment will be closed and the atmosphere will be inerted during reactor operations, reactor fuel handling operations, movement of irradiated fuel or whenever forced sodium coolant circulation is required. The containment is "open" only when the reactor is "shutdown" and secured.

g. The containment will be initially tested to full (30 psig) design pressure and subsequently at lower pressures. The maximum leakage rate will be 30 weight percent of the contained volume or less per day at 30 psig. The containment penetrations (except the MI cables imbedded in the cell concrete) will be designed so that the seals can be periodically tested at full design pressure to demonstrate leak tightness.

h. From an ultimate safety standpoint none of the equipment within the containment complex is required to operate after the maximum credible accident.

i. Air will only be used within the containment structure when the reactor, in addition to being shutdown is also "secured." The reactor will be defined as secured when all of the following conditions are satisfied:

(1) The reactor is shutdown (subcritical).

(2) A condition or tasks that could lead towards criticality will not be permitted.

(3) Decay heat is small so that forced sodium circulation for cooling is not required.

(4) There will be no movement of fuel within or into the core. The control rods will be delatched and handling of control rods will not be permitted at any time that fuel is in the core.

j. At times when the reactor is not secured in accordance with the above definition, the following criteria will be applied to the gas systems:

(1) Cell

The cell will have an argon atmosphere. During normal reactor operation and during fuel and/or control rod handling during shut-down, the following conditions will prevail.

(a) Normal Reactor Operation

Oxygen and water vapor concentrations during reactor operation will be controlled at a maximum of 1 w/o and 0.1 w/o, respectively.

(b) Fuel and/or Control Rod Handling

During fuel and/or control rod handling operations, oxygen and water vapor concentrations will be kept at a maximum of 0.01 w/o and 0.005 w/o, respectively. This reduction in allowable concentrations is related to the scavenging ability of the sodium in the reactor vessel which would be available to getter oxygen and water vapor from the cell atmosphere.

(2) Vault and Cell Annex

These regions will normally contain argon with oxygen and water vapor concentrations of about 1 w/o and 0.1 w/o, respectively.

a. Monitoring of the inert containment atmosphere for oxygen content will provide a surveillance of the containment leak tightness.

b. The pipes which penetrate the containment envelope and which could provide a normally open leakage path are relatively small (8 in. pipes or less). These lines will be provided with isolation valves which will be actuated by suitable signals in the event an accident condition develops. Provisions for periodic leak testing will be made.

c. The larger pipes that penetrate the containment structure are associated with the secondary sodium and argon cooling systems. By virtue of the nature of the design these systems are closed systems during reactor operation.

3. General Reactor Facility

A. Reference Cores

The reference cores in the preliminary safety analysis are intended to represent the extremes of the spectrum of cores which are

currently visualized for FARET. To be specific, the reference zoned core is the largest core foreseen and the reference small core is the smallest. These reference cores do not necessarily describe a specific core to be used in FARET although some similarities will undoubtedly exist. The first core will be described and the safety analysis results reported in the Final Safety Analysis Report. The safety of each succeeding core configuration will be thoroughly considered in light of existing standards.

B. Planned Reactivity Excursions

The future experimental program for FARET does not include any planned reactivity excursions of appreciable magnitude. Of course, the reactor will be oscillated in the usual manner to determine stability characteristics, and power levels will be changed, in the usual fashion.

No detailed information is available concerning either fuel bowing and movement studies or vented fuel experiments. As indicated previously, each proposed experiment will always be considered individually from a safety standpoint. Moreover, experiments will usually be conducted on a small scale in the early stages. For example, existing cores may be perturbed slightly. As experience is gained, the experiments will be extended.

C. Reactivity Limitations

Reactivity effects will be limited in the following (or equivalent) manner:

The nominal 0.030 in. gap between subassemblies will be reduced at one or more elevations to about 0.002 in. by the use of buttons on the outer faces of the subassemblies. Fuel elements within the subassemblies will be separated from each other and from the subassembly walls by spiral wires. Helical springs will probably be used to restrict the rising of fuel within each fuel element. The subassemblies will be held down by virtue of a combination of gravity and hydraulic forces.

Basically, these are the same techniques used in EBR-II. Accordingly, since EBR-II has been oscillated at over one-half power with no signs of instability, there is reason to believe that these methods can also be successfully employed in FARET.

The densification of ceramic type fuels during operation is a factor that could cause an increase in reactivity. Although all the mechanisms that may be involved cannot be specifically identified or evaluated in detail, it is believed that the effects would be small and of a random nature occurring over an extended period of operation.

D. Excess Reactivity

The limit on the operating excess reactivity will be based essentially on two main factors: (a) fuel burnup, and (b) temperature dependent reactivity effect.

(1) Zoned Core

(a) In the case of a zoned system, fuel burnups for any single phase of the experimental program is expected to be small. The current estimate is less than 1% atom burnup.

(b) The major criteria will be based on the absolute reactivity temperature coefficients with a temperature range of room temperature to the melting point of the fuel in the test zone, a 1000°C temperature rise in the buffer region and a 500°C temperature rise in the driver zone. The limit on the operating excess reactivity will be established when the coefficients are measured in the ZPR-III critical experiments.

(2) Small Core

In the case of the small core, the limits will be determined in the same manner as for the zoned core, except that atom burnup must be accommodated up to 10%.

E. Reactivity Shutdown Margin

The reactor shutdown margin will include the operating excess reactivity. In addition, an amount of reactivity will be designed into the total control system such that for the case of free falling control rods the insertion rate will be as great as the maximum credible insertion rate considered in the safety analysis.

Shutdown margin is not less than 2% $\Delta k/k$ at the reactor temperature of 150°C and exceeds the worth of the most reactive subassembly.

With a single control rod malfunction shutdown margin is at least 1% $\Delta k/k$ at the reactor temperature of 150°C.

Maximum control rod withdrawal rate is 6 in./min or 5¢/sec. Two rods cannot be driven out simultaneously.

Scram accelerations are 1 to 2 g's within the upper 16 in. of control stroke. (Approximately 200 to 300 milliseconds for 16 in. travel.)

F. Core

(1) The overall total prompt reactivity coefficient will be negative under all conditions.

(2) The reactor vessel and containment will contain deflection devices as required to protect against the possibility of recriticality following a major accident.

G. Reactor Vessel

(1) The reactor vessel will be a double-walled vessel constructed of 304 stainless steel and will be designed for an internal pressure of 15 psig and a temperature of 1200°F to the ASME Boiler and Pressure Vessel Code, Section III, as a Class A vessel. The reactor vessel will also be designed to meet the requirements of Section VIII of the ASME Code for an external pressure of 15 psig with an internal temperature of 1200°F.

(2) The reactor vessel, reactor vessel support assembly, extension piece and internals, will be designed for a minimum life of 15 years at mixed mean vessel outlet temperature conditions as outlined in Volume I of this report. In addition, the vessel will be designed for the expected operating transients (50°F/hr) and a limited number of emergency shutdown transients as defined therein.

(3) The annular volume between the inner and outer reactor vessel walls will contain NaK with provisions for monitoring the NaK annulus system for indications of sodium leakage. The outer vessel wall will be designed such that in the event of an inner vessel wall failure it will contain the sodium.

(4) All nozzles and penetrations in the reactor vessel will be above the core and the system will be designed such that draining of the reactor vessel to a level below the core is not possible as a result of valve manipulation or equipment malfunction.

(5) The reactor vessel cover complex will include mechanisms and instrumentation to insure that all control subassemblies are disengaged and fully inserted within the core prior to removal of the vessel cover.

(6) The operating liquid level in the reactor vessel is maintained by a continuous makeup-overflow system.

H. Control Rod Drives

(1) The reactor will be controlled by vertical adjustment of in-reactor control rods.

(2) The control drive mechanisms will be designed so that only one rod at a time can be moved to increase core reactivity.

(3) Maximum reactivity insertion rate will be no greater than 5¢/sec.

(4) Rod scram will be by gravity assisted by pneumatic pressure.

(5) The rod drive system will be designed such that no single malfunction or operating accident (including the effects of sodium spills) can cause movement of more than one control rod in a manner which would insert reactivity.

(6) The control system will be designed to shut down the reactor from its most reactive condition (at any point in an operating cycle) with a minimum shutdown margin of 1% $\Delta k/k$ assuming that any one control rod is stuck in its full-out position.

(7) The minimum shutdown margin of reactivity will not be less than the maximum worth of a fuel subassembly.

I. Coolant System

(1) Those portions of the reactor coolant system in contact with sodium will be constructed of Type 304-stainless steel to applicable ASME and ASA Codes, except where design practice calls for a different material.

(2) The reactor will be cooled by two sodium systems: a main system capable of removing 50 megawatts thermal (MWt), and an auxiliary system capable of removing reactor decay heat and normally operating at a low flow, standby condition. Each system will contain a separate primary and secondary loop isolated from each other by an intermediate heat exchanger.

(3) The main primary loop will contain an additional E-M pump with a capability of delivering 6% of main loop rated flow. This pump will be energized from a battery source with sufficient life (~15 min) such that the core will be cooled and remain covered (to prevent fuel cladding failure due to overheating).

(4) The auxiliary system will serve as an operational standby for the removal of reactor decay heat when the main system is out of service. The auxiliary primary system is energized from the 480-volt diesel-generator powered emergency bus. The auxiliary secondary, including the air-cooled heat exchanger has adequate natural convection flow.

(5) The coolant system will be designed such that the core will be cooled and remain covered in the event of (1) a single wall failure of any portion of the primary coolant system, or (2) the loss of pumping power.

(6) The main system will be designed to provide the capability of removing reactor decay heat through natural circulation in the event that all forced cooling is lost.

(7) A makeup and overflow system for the reactor vessel will be provided to insure a constant sodium level. Continuous makeup will be provided in the event of coolant system leaks to the degree sodium is available in the primary dump tank.

(8) The core will remain covered and shutdown cooling is provided by the auxiliary system if failure occurs in any portion of the unguarded main system piping.

J. Control and Instrumentation

(1) General criteria for control and safety instrumentation include the following:

(a) Instruments should be commercially available wherever practicable.

(b) Critical instruments should be of a design proven in actual installations.

(c) Logic circuits should be simple, using a minimum number of components.

(d) Critical components should be conservatively rated.

(e) Redundancy will be used where deemed necessary in the interest of continuity of operation.

(f) Solid state components will be used when they exhibit superiority in the application.

(2) Safety trip values will be established for parameters vital to reactor safety including high reactor power level, short period, low detector voltage, low scram assist pressure, low reactor coolant flow, high rate of change of reactor coolant flow, high temperature in the reactor vessel outlet coolant, power interruption to the primary or secondary pump motors, low primary system sodium level, high reactor blanket gas pressure, and seismic activity.

(3) All trip functions in the safety system derived from reactor sodium level, flow and temperature instruments will include redundancy and whenever feasible equipment will be designed to fail in a safe mode. A failed channel, or a channel which is being tested, will indicate a trip on that channel such that the previous 2 out of 3 logic will become a 1 out of 2 logic.

(4) The safety system will be designed so that no single component or wiring failure can compromise the safety system effectiveness.

(5) The safety system instrumentation will be tested to demonstrate proper operation at extremes of rated environmental conditions. In addition, the safety system instrumentation located in the control room will be tested in situ at the extremes of anticipated operating environmental conditions.

(a) All safety system instrumentation will be specified for operation in ambient temperatures between 5°C and 50°C and a maximum humidity of 90%. The supplier will be required to furnish certified results of tests that verify the proper operation of representative samples of all units at these extremes of environmental conditions.

(b) The control room temperature for this test will be less than the 122°F (50°C) specified for the instruments, and is expected to be about 110°F.

(6) These tests will include, but are not necessarily limited to:

(a) Tests of individual systems to affirm that the proper response occurs with variations of input function.

(b) Test of each remote instrument transmitter, etc., to affirm that the correct signal appears at the correct input terminals at the related control panel.

(c) Test of each remote actuator, etc., to affirm that the proper action follows a given signal at the control panel terminals.

(d) Test, insofar as practicable, of the integrated system in which an input variable is simulated, or actually caused at the various transmitters.

(e) Individual chassis tests for sensitivity to high and low voltage, etc.

K. Auxiliary Systems

(1) An argon cooling system, a sodium service and purification system, and portions of the radioactive waste gas system will be included within the containment enclosure.

(2) Wherever possible the auxiliary system fluids within the containment will be compatible with the reactor coolant system fluids. Where an incompatibility does occur, the auxiliary system will be designed and protected such that contact between the incompatible fluids does not result in conditions that would violate the containment.

L. Fuel Handling and Storage

(1) Windows and/or viewing devices will be provided in the cell to permit visual observation of refueling and other principal cell operations.

(2) Temporary storage capacity for up to 151 irradiated fuel assemblies immersed in sodium will be provided in a double walled wet fuel storage tank located in the cell. All vessel penetrations, leading into the sodium part of the system, located above the fuel region with anti-siphon provisions.

(3) The annulus encompassing the wet fuel storage tank will be filled with NaK. A NaK coolant loop will provide for normal cooling of the sodium tank. Emergency cooling will be provided by use of the sodium purification system. This tank will be so located that its integrity will not be affected by any reactor incident.

(4) The design of the irradiated fuel storage array will be such that criticality will not be possible regardless of the number and type of subassemblies stored.

(5) New fuel subassemblies will be stored in an array such that they will not be critical under any conditions.

(6) During the handling of "irradiated" fuel, the emergency power source for the fuel handling system is operative.

(7) The reactor vessel cover is up, held by the support ring, and inoperative during fuel handling over the reactor vessel.

(8) Operation of the wet fuel storage tank cover will not be possible during the movement of the fuel handling machine over the wet fuel storage tank.

(9) The reactor vessel coolant temperature is between 225°F and 350°F during fuel handling to and from the reactor vessel.

(10) The release of the gripper holding a subassembly will not be possible during fuel handling when the subassembly is partly or fully withdrawn in the fuel handling machine.

(11) It will be possible to disengage the machine barrel and trolley assembly, by means of the overhead in-cell crane, and lower it partly into the disassembly pit.

(12) It will be possible to remove the bridge and trolley assembly from the cell through the ceiling hatch for major modifications or repair.

(13) The trolley and the machine barrel will also be removable from the cell through the equipment transfer port.

(14) Transfer of fuel, into or out of the cell, made while the reactor is at power will be accomplished in a manner that will not violate containment criteria.

M. Power

(1) Normal plant power will be furnished via a single 13.8 kV overhead transmission line from EBR-II, through transformers to a single 2.4 kV bus and two separate 480 V buses.

(2) All components whose operation is necessary to prevent or significantly reduce the consequences of a major accident will be provided with at least two backup power supplies or their equivalent. Each of these backups shall be independently capable of operating the components for as long as is required.

(3) Emergency power to the plant will be supplied by a single, automatically started, diesel-generator unit which is automatically connected to the two 480 volt plant load centers and is capable of delivering power to critical plant loads (approximately 625 kVA) within 45 seconds after a plant outage. At least twenty-four hours supply of fuel for the unit will be on hand.

(4) Station battery systems will be provided to assure uninterrupted power to the safety systems and other critical plant loads in the event of a loss of normal power supply, and to assure a power supply to selected loads for a limited time in the event that the emergency power diesel-generator unit does not start automatically.

(a) A 60-cell battery system, continuously operated in a float charge condition, will supply emergency power to the safety system and other critical loads (including portions of the fuel handling systems, and the emergency lighting). The battery system will be capable of supplying power for at least 30 minutes of operation.

(b) A separate float-charged 125 V battery system will be used solely to assure uninterrupted power to the main primary sodium shutdown pump for a period of at least 15 minutes.

(5) A separate emergency power supply will be provided for the emergency exhaust system and will be in operation during reactor operations, reactor fuel handling operations, movement of irradiated fuel or whenever forced sodium coolant circulation is required. Sufficient fuel reserve will be provided to insure an operating time of 30 days or the duration of the maximum postulated accident, whichever is less.

N. Ventilation System

(1) Pressures within the building will be controlled such that air flow is always from clean areas toward areas of potential contamination in order to protect operating personnel and prevent the spread of activity.

(2) Pressure in the reactor building will be maintained sub-atmospheric in order to preclude the possibility of out-leakage and thus prevent a ground release of radioactivity.

(3) Discharge of radioactive gases or particulate matter from the 200 ft high ventilation stack must not exceed allowable levels.

(4) The following pressures have been selected for the various building areas in order to ensure air flows in the proper direction.

(a) Front cell area--0.15 in. W.G. negative (with respect to atmosphere).

(b) Rear cell area--0.35 in. W.G. negative.

(c) Cell--2 in. W.G. negative to 4 in. W.G. negative.

(d) Cell annex-- $1\frac{1}{2}$ in. W.G. negative to $3\frac{1}{2}$ in. W.G. negative (annex pressure is controlled to be $1\frac{1}{2}$ in. W.G. positive with respect to the cell.)

(e) Vault Cavity-- $1\frac{1}{2}$ in. positive to 4 in. positive (with respect to atmosphere).

APPENDIX E

Site Data

The FARET Facility is planned for construction in the East Area of the National Reactor Testing Station. The station is about 30 miles due west of Idaho Falls, Idaho. The East Area, which includes the EBR-II, and TREAT reactors, is about 11 miles west of the NRTS east boundary, and 4 miles north of Highway 20. It is planned to erect the FARET Facility approximately 4,000 ft southeast of the EBR-II. A new asphaltic roadway, about 3,400 ft long, will connect the FARET Site to existing Taylor Boulevard.

The FARET Site will cover an area of about $2\frac{1}{2}$ acres. It was selected on the basis of satisfactory subsoil information obtained previously for the planned, but later abandoned, Argonne Boiling Reactor (ARBOR) Facility. See Volume 1 of this report for site details.

1. HydrologyA. Soil and Subsurface Characteristics

The NRTS is located in southeastern Idaho on a level plain which has an average elevation of 4,865 ft above sea level. The completed FARET Site will have an average elevation of about 5,170 ft above sea level.

The surface of much of the plain is covered by waterborne and windborne silty loam, top soil, and includes some gravel. Lava rock may be seen to outcrop occasionally and is found to extend below grade to a considerable depth, ranging at least to the water table. The top soil usually covers the lava to a depth of 2 to 10 ft. The lava rock is honeycombed with openings of about 1/8-in. diameter. Larger openings occur frequently, and these range upwards to the size of tunnels, tubes, and caves.

B. Drainage

The NRTS overlays a natural underground reservoir of water having an estimated lateral flow of not less than 500 ft³/sec (323,136,000 gal/day). The water table is approximately 650 ft below grade. The main sources of water for this reservoir are the streams that originate in the mountains to the north and disappear into the porous soils of the NRTS area. These streams include Big Lost River, Little Lost River, and Birch Creek.

The manner in which the surface water percolates to the reservoir is unknown. However, it is expected that drainage would be very rapid through the gravel overburden, and less rapid through the lava rock but still very high as compared to flow through sands or clays. The flow would be

around, rather than through, the clay beds. Therefore, in case of a major accident with loss of a large volume of liquid wastes, the reservoir probably would become contaminated in a very short time.

The estimated lateral flow of the main body of water through the lava is approximately one-half mile per year. Based on this estimate, discharged contaminated water would reach the Snake River Canyon Springs and enter the Snake River in about 120 to 140 years, depending upon the exact origin of discharge within the Testing Station Area.

Surface drainage, which is always quite low in the arid region of the NRTS, is generally towards the northeast.

2. Seismology

The NRTS Site is located in a region designated as a Zone 2 area on the "Map of the United States Showing Zones of Approximately Equal Seismic Probability." This map is included as a basic reference in the Uniform Building Code (1961).

The NRTS area is known to be traversed by a series of geological lineaments or distinctly linear surface features which are oriented either parallel or at right or obtuse angles to the major subsoil structure. They are believed to be faults (or fractures) in the rock material along which movement has taken place, one rock mass sliding past the other. One of these lineaments (a proven fault) is located approximately 0.7 miles east of the FARET Site and extends for several miles in a north-northeasterly direction. (See Fig. 39.)

The following information is quoted from a report on the earthquake risk at a site near Arco, Idaho.³⁴

"Earthquake risk at this site is appreciable, but not great. Since isoseismal maps for principal earthquakes have been drawn, beginning in 1925, the isoseismals of only one earthquake reached Cerro Grande. Prior to this time several earthquakes recorded for surrounding areas may have been felt at Cerro Grande. There is no record of a major earthquake originating close to Cerro Grande.

"However, Cerro Grande is surrounded by areas of comparatively high seismic activity. Furthermore, it lies in a region of geologically young faults, any one of which must be considered potentially active.

"Cerro Grande is situated within one hundred and fifty miles of several areas of pronounced earthquake activity. Any one of these might produce a shock stronger than it has yet produced with a

correspondingly greater intensity at Cerro Grande. The earthquake history of one hundred years for this area is very short, from the geological point of view.

"An earthquake might occur any day that would alter substantially our ideas of the distribution of seismic activity in the area about the Snake River Plains.

"Earthquake risk in any area is relative to the type of structures to be built. Reinforced concrete buildings well constructed in every way with high factors of safety and incorporating features recommended by engineers acquainted with earthquake-proof design, stand less risk of being damaged. Such buildings, set on the lava bedrock at Cerro Grande, certainly would be reasonably safe from earthquake damage.

"No traces of recent faults are known by the writer to cross the Snake River Plains. The chances, then, of displacement in the ground that would cut water supplies are small enough to be eliminated from consideration.

"In spite of the fact that a Zone 3 area exists both North and South of the Arco area, the distances are so great that Zone 2 has been considered completely safe."

This report was published in 1950. On August 17, 1959, an earthquake was felt at NRTS. The official description is as follows:

"August 17, 1959 23:37 Main shock of Hebgen Lake, Montana series; epicenter 44° 55'N., 111° 5'W. (north of West Yellowstone, Montana). This earthquake was perceptible over an area of approximately 600,000 square miles in the U. S. and Canada. Pasadena Magnitude 7.1. Maximum intensity in epicentral region was X. In Idaho, intensity VII was reported from the Big Spring, Henry's Lake, and Island Park areas. Intensity VI at Arco, Dubois, Howe, Rexburg, Rigby, Rupert, and St. Anthony. Intensity V at Aberdeen, American Falls, Blackfoot, Burley, Carey, Idaho Falls, Richfield, Shelly, and Shoshone."

3. Meteorology³⁴⁻³⁶

The NRTS covers about 894 square miles of the Upper Snake River Plain. The most important topographical features are the northeast-southwest orientation of the Plain, and the Continental Divide to the north and east. The Divide prevents the invasion of many of the cold, continental air masses in winter. All air masses must first cross a mountain barrier regardless of the direction from which they enter the Plain. Because they are subject to lifting, these masses usually precipitate moisture over the mountains and enter the Plain sufficiently dry to give the region its desert-like characteristics.

Fig. 39. NRTS East Area Seismic Faults and Lineaments

The dryness and the infrequency of low clouds permit intense solar heating of the ground during the day, and rapid radiative cooling at night. The average temperature range is about 37°F in summer, and about 23°F in winter. Temperature inversions based at the surface are present for at least a few hours almost every day of the year; with snow cover they may persist for several consecutive days. Temperature inversions through the lower 250 ft of the atmosphere are present for about one-half the total time for the year. While temperature measurements have not been made to the top of the inversions at the times of maximum depths, the average maximum depth is estimated to be approximately 2,000 ft.

There is a strong tendency for up-valley flow (southwest wind) during the day, and down-valley flow (northeast wind) at night. Frequently, local effects dominate the wind flow near the ground in the face of strongly opposing pressure forces. For example, part of the NRTS will experience northerly winds, while another part, southerly winds. This situation arises at times from drainage effects and, at other times, from a topographically-induced eddy.

Wind rose diagrams for the general NRTS area are presented in Figs. 40 and 41. More detailed wind rose diagrams for the Central Facilities area (about 16 miles west of the FARET Site) are given in Figs. 42 and 43.

Destructive winds have not been a problem. Tornadoes have never been reported in the region although funnel-shaped clouds have been sighted on several occasions. Structures designed to withstand average winds of 60 mph and peak gusts to 80-85 mph are not likely to be damaged.

Dust-entraining winds (gusts of 20 mph or more) occur on the average for 6 hr during 13 windy days per month. Dust concentrations have varied from a low of 0.014 mg/m^3 over snow cover, to a high of 0.77 mg/m^3 on a day when dust devils were present near the sampling station. Concentrations vary widely from place to place, depending on human activity. During the warmer months, daytime concentrations average 0.07 mg/m^3 . Visibility is not significantly reduced except briefly and infrequently during dry thunderstorms.

Four to six thunderstorms can be expected during each summer month; some produce hail that can be damaging to crops. Lightning protection is required on all man-made structures, since the lack of comparable natural targets makes them particularly vulnerable.

Average annual precipitation at the site is 7.7 in. The heaviest precipitation occurs as scattered showers during the warmer months. Although two or three months may pass without rain, a single passing shower may, in a matter of minutes, exceed the normal total for the month in which it occurs. One inch of rainfall in one hour is about the maximum intensity to be expected, and a total of two inches in 24 hr is not likely to be exceeded. Despite this extreme variability, annual totals have not varied greatly.

Supercooled fog or low stratus clouds occur occasionally in the winter together with a snow cover and a persistent high-pressure system. These conditions may last for several consecutive days. Maximum snow depth has been extremely variable during the period of record. The accumulation of considerable rime icing on power and telephone lines is commonplace. Destructive glaze icing has not occurred during the period of record.

Weathering of concrete and hard-surface roads and streets is a very serious problem, mainly due to the freezing-thawing cycles which occur during all but summer months. When concrete is poured, precautions must be taken against adverse effects of excessive cold in winter and extreme dryness in summer.

Fig. 40. NRTS Annual Average Wind Roses

Fig. 41. NRTS Central and TAN Annual Wind Roses

Fig. 42. NRTS Central Wind Rosco at 20 ft

Fig. 43. NRTS Central Wind Roses at 250 ft

APPENDIX F

Miscellaneous Subjects

1. Reactor Vessel

During one meeting with the ACRS Subcommittee, a question was raised regarding possible difficulties in having the reactor vessel fabricated to the necessary exacting standards. In response to this question the method of reactor vessel procurement was generally described, including the steps taken for pre-qualification of vendors. It was noted that the plants of prospective vendors were visited to inspect their facilities. It was stated that the vessel manufacturer selection process would place considerable emphasis on the quality control practices of the organizations. Finally, it was concluded that it had been established that at least a few firms could meet the stringent requirements. It was pointed out that considerable "weeding out" had resulted in the past few years but that a few well-qualified firms which had invested heavily in upgrading their facilities, especially in the area of quality control, were well qualified to fabricate the FARET reactor vessel.

2. Critical Experiments

The critical experiments being planned for the FARET cores have two main functions. The first is to establish the configuration and composition of the FARET cores. The second objective is to determine as closely as possible the magnitude of the reactivity effects to be expected in the FARET cores. The experimental program planned for ZPR-3 is as follows:

- a) Doppler coefficient determination
- b) Fuel expansion effect
- c) Sodium expansion effect
- d) Material distribution worths (fissile, fertile and structural)
- e) Control rod worths (single and multiple rod)
- f) Flux traverse out to the sodium region in reactor vessel for shielding and heating rate studies
- g) Reactor rate traverses

3. Materials

During the AEC-FARET reviews related to safety the question of materials selection arose several times. Particular interest was focused on the application of Type 304 stainless steel to the most critical features of the design. The Commission was advised in part as follows:

"Type 304 is considered more desirable than Type 316 chiefly because of its lesser tendency than that of Type 316 to

develop sigma phase on long term exposure at FARET temperatures with resultant possible embrittlement. Otherwise Type 316 appears to be equally acceptable. You will also note that numerous other candidate materials were considered, including the relatively new superalloys. These new alloys have some desirable characteristics such as high strength, but unfortunately their technology is not sufficiently developed for our application.

Your attention is directed to the very limited information available concerning the effects of high temperature, sodium environment and irradiation upon reactor vessel materials. This information is greatly needed for the design of future reactor installations."

Because of the considerable interest expressed in this subject, and because of the implicit safety-design relationships, excerpts from a topical report on the subject written by L. R. Kelman, and R. J. Dunworth are included in this Appendix. This report has been published as ANL-6939 "Selection of the Structural Material for the FARET Liquid Metal Systems" by the same authors.

METALLURGICAL CONSIDERATIONS IN THE SELECTION OF THE MAIN MATERIAL FOR CONSTRUCTING COMPONENTS OF THE FARET LIQUID METAL SYSTEMS

INTRODUCTION

The combined high temperature sodium-radiation environment proposed for the 15-year plant life of FARET is an extrapolation beyond present experience and knowledge for available structural materials. Selecting the optimum material required close cooperation between the designers and those most knowledgeable in materials properties and technology. To that end the following were asked early in 1963 to serve on a FARET Materials Selection Committee.

T. L. Kettles (R.E.) - Chairman
N. Balai (R.E.) - Materials and Fabrication
L. R. Kelman (Met.) - Physical and Mechanical Properties
M. A. Pugacz (Met.) - Welding and Radiation Damage
S. Greenberg (Met.) - Corrosion

The first job of the Committee was to advise W. R. Simmons, FARET Project Manager, in the selection of a material for the core structure and primary vessel and piping. The Committee first reviewed ground rules and pertinent FARET conditions with members of the Reactor Engineering Division to determine the materials requirements and probable problem areas. Through our discussions and reviews of the literature we realized that most candidate materials did not meet the requirements.

In the course of our deliberations we consulted with many materials experts from ANL, other research laboratories and industry. Some of the well-known authorities in the high-temperature structural material field that were consulted included

J . Chirigos	Bettis Laboratory, Westinghouse
M. A. Cordovi	International Nickel Co., Inc.
J . W. Freeman	The University of Michigan
D. R. Johnson	Bechtel Corp.
W. Leyda	Babcock and Wilcox Research Center
R. A. Lula	Allegheny Ludlum Steel Corp.
J . J . Moran	International Nickel Co., Inc.
P. Shahinian	Naval Research Lab.
E. N. Skinner	International Nickel Co., Inc.
C. R. Sutton	Nuclear Utility Services, Inc.
R. C. Werner	MSA Research Corp.
High Temperature Materials Specialists from U. S. Steel Corp.	
High Temperature Materials Specialists from Huntington Alloy Products, INCO	

We also arranged for a symposium on "Materials for Sodium-Cooled Reactors" which was cosponsored by the AIME and the ANS and which was held at the 1963 Winter meeting of the ANS. We invited reactor engineers and metallurgists from all organizations that have designed, built, or operated sodium-cooled reactors. Thus we were able to collect in one place³⁷ materials information that was widely disseminated through the literature and some that was not yet reported. Most important to our immediate problem, the symposium permitted us to exchange views with knowledgeable and interested persons from other organizations and especially those who are involved in materials problems similar to ours.

FARET REQUIREMENTS

The FARET vessel is proposed to serve for 15 years as a safe container for engineering quality sodium at high temperatures while undergoing a total integrated fast neutron irradiation of 10^{21} nvt.* Failure of the vessel by brittle fracture is a most serious potential hazard. Therefore, the material selected must retain adequate ductility and impact resistance to withstand the stresses and thermal shocks that the vessel will see. It is anticipated that the system will operate at 1100-1200°F for 10,000 hours, 1000-1100°F for 40,000 hours, 600-1000°F for 40,000 hours, and <600°F for the remainder of its 15-year life.

*Ed. Note: This quantity is for an unshielded vessel. The FARET design provides sufficient shielding internally to reduce this value by two or three orders of magnitude.

Since there was not to be a materials development or proof program, the material selected for FARET had to have a reasonably well-developed and adequate technology. Not only must it have adequate mechanical properties under FARET conditions, but the material must be readily available in the necessary sizes and shapes as well as amenable to fabrication and welding into the final assembly. Ultimately, of course, it is the fabricated assembly that must satisfy FARET conditions.

MATERIALS CONSIDERED

A wide range of materials was examined some of which looked very promising. However, most of them were developed too recently to satisfy the "existing technology" requirement. Others were eliminated because they did not have adequate properties.

Consideration of the long-time load-carrying ability at 650°C (1200°F) of candidate materials in Table 21 narrows the field to the austenitic chromium-nickel stainless steels and the new superalloys. The low strength at 650°C (1200°F) of the ferritic steels and the precipitation hardenable stainless steels eliminates them from consideration for this application.

TABLE 21. Allowable Stresses for Ferritic and Austenitic Steels⁴⁴

Type	Material	Grade	Allowable Stress at 1200°F (ksi)
Ferritic	2.25 Cr, 1 Mo	P22	2.0
	5 Cr, 0.5 Mo	P5	1.5
	9 Cr, 1 Mo	P9	1.5
	13 Cr	410	1.0
	17 Cr	430	1.75
Austenitic	19 Cr, 10 Ni	304	4.5
	17 Cr, 12 Ni, 2.5 Mo	316	6.8
	18 Cr, 10 Ni, Ti	321	5.0
	18 Cr, 11 Ni, Cb	347	5.0

As can be seen from Table 22, the relatively new superalloys are potentially far superior to the austenitic stainless steels in long-time, high-temperature strength. Superalloys high in cobalt were included in spite of the general belief that cobalt would cause radiation problems. Rossin advised that the radioactivity contribution of the fast neutron-induced isotope of cobalt having a 5.3 year half-life would be relatively insignificant compared with the 71 and 314 day half-life of like amounts of iron and nickel.³⁸ Most of these alloys were developed to solve the shortcomings of the austenitic stainless steels for short-time, high-temperature applications and only

recently have been considered for long-time use. Incoloy-800 is an example of a high nickel-chromium-iron alloy that was considered a promising candidate by the Committee. However, it has an inadequately developed technology and its compatibility with sodium is not well enough known.^{39,40} It has very recently been approved by the ASME Board of Codes and Standards for use under Sections I and VIII of the Code.⁴¹

TABLE 22. Estimated Allowable Stresses for Superstrength Alloys at 1200°F

I. Fe-Cr-Ni-Mo Alloys	Stress (ksi)	III. Ni base Alloys	Stress (ksi)
	6 ⁽⁵²⁾	Inconel	6 ⁽⁴¹⁾
15-15N	11 ⁽⁵²⁾	Inconel-X	23 ⁽⁵²⁾
17-14CuMo	12 ⁽⁵²⁾	Inconel-102	18 ⁽⁴¹⁾
16-25-6	9 ⁽⁵²⁾	Hastelloy-X	11 ⁽⁵⁴⁾
A286	11 ⁽⁵³⁾	Rene 41	24 ⁽⁵⁴⁾
Incoloy-800	7 ⁽⁴¹⁾		
II. Fe-Cr-Ni-Co Alloys		IV. Cobalt base Alloys	
N155	10 ⁽⁵²⁾	HS25	18 ⁽⁵²⁾
S590	14 ⁽⁵²⁾	HS31	12 ⁽⁵²⁾
		S816	14 ⁽⁵²⁾

Unfortunately, most of the superalloys are still laboratory curiosities and none are commercially available in the desired sizes and shapes. In addition there is very little long-time test data and practically no service history or developed technology to permit their selection for FARET. The results of a series of high-temperature radiation studies of superalloys has been reported.^{42,43} Hastelloy-X has been irradiated to 10^{19} nvt at 650°C (1200°F) for testing times of approximately 1000 hours with only slight lowering of rupture strength and ductility. In the same study, the rupture strength and ductility of two of the promising precipitation hardenable alloys, A286 and Rene 41 were significantly reduced.

SUMMARY AND CONCLUSIONS

The following summarizes the conclusions that were reached by the FARET Materials Selection Committee regarding selection of a material for the components of the liquid metal systems (especially the pressure vessel and piping).

1. The FARET conditions of 650°C (1200°F) sodium in an intense neutron environment are an extrapolation from our current knowledge necessitating some element of risk and speculation in our selection of a material.

2. AISI Type 304 stainless steel comes closest to meeting FARET conditions and material selection requirements.

(a) It has been used extensively in liquid sodium systems to 538°C (1000°F), and there is some experience to 650°C (1200°F). It has been used extensively in the petrochemical and the chemical-processing industries to 650°C (1200°F) but with a different environment (usually extremely corrosive and for shorter times) than anticipated in FARET.

(b) Type 304 SS has been selected by other organizations for proposed applications similar to those in FARET.

(c) Allowable stresses and the basis for establishing allowable stress values for service temperatures of 650°C (1200°F) are available in the ASME Boiler and Pressure Vessel Code.

(d) Long-time data based on currently commercial Type 304 stainless steel shows that the Code values may be too low. The Code values are based on unwarranted extrapolations from a relatively limited amount of old, short-time data. The 650°C (1200°F) creep strength of Type 304 SS is similar to that of Type 316 SS if the optimum composition is specified and verified.

(e) Retention of some ductility and impact resistance over a long time under FARET conditions is necessary to avoid catastrophic brittle failure. The excellent low temperature ductility of Type 304 SS is somewhat degraded at FARET temperatures by carbide precipitation. Type 304 SS gives no sigma problem if its composition is controlled. It retains a greater degree of its ductility under these temperature conditions than do most other candidate materials.

(f) There exists no service experience or convincing experimental data on the effect of long exposure to neutron radiation of Type 304 SS (or any other candidate material) under FARET's conditions of stress and temperature. This is the area of greatest extrapolation in the selection of a structural material for FARET.

(g) The selection of Type 304 SS for FARET is based on a reasonable extrapolation from our existing knowledge of sodium compatibility and the effect of sodium on mechanical properties.

(h) Type 304 SS has a very well-developed technology. It can be obtained readily through normal channels in a wide variety of shapes and sizes with reasonable assurance that specifications and delivery schedules will be met.

(i) There is a wealth of experience that indicates that Type 304 SS is one of the most readily welded of the austenitic stainless steels.

3. Type 316 SS was the second choice for the FARET application. It was considered less desirable than Type 304 SS only because of its tendency to develop sigma phase on long-time exposure at FARET temperatures with resulting embrittlement. It is possible that neutron irradiation will accelerate sigma formation especially if the material is under stress. Except for this, it comes as close as Type 304 SS to satisfying FARET conditions and materials selection requirements.

4. Types 347, 321 SS and the high chromium austenitic stainless steels were eliminated from consideration because of welding problems and because they are subject to embrittlement from sigma formation.

5. The superalloys were eliminated from consideration because none of them have an adequately developed technology and they have very limited service history.

4. Seismic Analysis

The Laboratory planned to request seismic consultants, J. A. Blume & Associates to review the following structures and equipment to determine the adequacy of the design approach that has been used and whether a dynamic analysis may also be required.

1. Reactor Vessel including Internals and Vessel Support Structure
2. Control Rods and Associated Drive Mechanisms
3. Auxiliary Primary Coolant System
 - a. Piping (including the double-walled portion of main primary coolant inlet pipe)
 - b. Auxiliary Intermediate Heat Exchanger
 - c. Auxiliary Primary E-M Pump
4. Auxiliary Secondary Coolant System
 - a. Piping
 - b. Auxiliary Secondary E-M Pump
 - c. Reactor Flange Cooling Heat Exchanger
 - d. Auxiliary Air-Cooled Heat Exchanger
 - e. Auxiliary Secondary Expansion Tank and its Support Structure

5. Emergency Diesel Generator (including power wiring to both primary and secondary auxiliary pumps)
6. Containment Structure
 - a. The physical (basic) structure of the cell, cavity and vault
 - b. Physical extensions of containment--penetrations, hatches, pipes (up to and including closure devices); and any other appendages to the basic containment structure which must also function as the containment barrier
7. Emergency Exhaust System
 - a. Emergency exhaust fans (2 at 5000 cfm each) (including power supply)
 - b. Absolute filter assemblies
 - c. Interconnecting ductwork, building exhaust fan dampers, emergency exhaust fan discharge damper and absolute filter inlet valve
 - d. 200 ft stack and support foundation
 - e. Reactor building structure

5. Sodium Chemical Reactions

The following analysis was made for the FARET containment, before the cell annex was a main feature. Its relevance to the current design is somewhat academic because there are no plans to operate with air in the cell except when the reactor is secured. However, it is included in this report because it represents a significant contribution to safety considerations of FARET Type Containment. Also, it may be possible to operate FARET with air in the containment under certain circumstances in the future. It should be stressed that there is no current intention to operate in this manner and such a change would require prior AEC approval presumably based on submittal of a satisfactory safety analysis at that time.

It has been shown⁴⁵ that sodium will not burn in a low-moisture atmosphere with an oxygen content of less than about 5%. Since the oxygen content of the inert gas in the cell will normally be less than 5%, sodium ejection into the cell at that time should not result in a fire.

However, if sufficient air is present in the cell a violent ejection of sodium into the cell would cause a sodium fire. The resulting temperatures and pressures were calculated with the aid of Code 1922/RE written expressly for this purpose.

A. Chemical Reactions

The sodium-oxygen reaction considered is:

The reaction between sodium and water was assumed to be:

The heat of reaction, Q , may be found by considering the reactions:

and

Subtracting Eq. (4) from Eq. (3),

From Reference 16, Q_1 and Q_2 are found to be 102 kcal and 68 kcal, respectively. Hence, Q in Eq. (2) is $102 - 68 = 34$ kcal.

B. Sodium Fire Code (1922/RE)

The sodium fire code, 1922/RE, for the IBM-704 is used to calculate the peak temperature attained in an enclosure containing air with any moisture content, into which a sodium stream is sprayed, followed by burning at an infinite rate. Any mole ratio of sodium to oxygen may be considered. The peak temperature of the constituents is calculated by equating the heat release to the available heat capacities of all reactants. Specific heats of reactants are variable with the temperature where applicable, and are derived by using information in Reference 17. The maximum container pressure is calculated using the Perfect Gas Law and calculated gas temperatures.

The code then calculates cell pressure versus time, assuming that heat is transferred from the gaseous constituents to the cell liner and 3 in. of the wall and ceiling concrete only by natural convection and/or radiation. The calculation ends when the gas, liner, and 3 in. layer of concrete have reached equilibrium temperature.

C. Cell Pressure as a Function of Time after a Sodium-Air Reaction

The experimental investigations of Humphreys⁴ show that theoretical peak pressures resulting from sodium-air reactions are virtually impossible to develop. These investigations were carried out with a reaction vessel consisting of a steel tank 3 ft in diameter and 10 ft high, with a wall thickness of 1/4 in. The sodium was ejected into the reactor vessel

from various 4-in.-diameter reservoirs located concentrically in the lower head of the vessel. The manner, quantity, and energy of sodium ejection were studied for their effect upon peak sodium pressures and temperatures. The results are shown in Fig. 44.

NOTES:

1. CURVES INDICATE MAXIMUM POSSIBLE PEAK PRESSURES AND TEMPERATURES BASED UPON:
 - (a) 100% REACTION OF SODIUM OR OXYGEN, WHICHEVER IS LIMITING.
 - (b) INSTANTANEOUS REACTION (WHICH IMPLIES NO HEAT LOSS; OR, PERFECT DISTRIBUTION, INFINITE PARTICLE VELOCITY, AND INFINITESIMAL PARTICLE SIZE OF SODIUM).
 - (c) THE FOLLOWING AVERAGE SPECIFIC HEATS ARE ASSUMED:

$$C_v(N_2) = 4.82 + 3.3 \times 10^{-4}t - 4.7 \times 10^{-8}t^2 \text{ cal/(mole)(}^\circ\text{C)}$$

$$C_p(Na_2O_2) = 0.31 \text{ cal/(mole)(}^\circ\text{C)}$$

$$C_p(Na_2O) = 0.31 \text{ cal/(mole)(}^\circ\text{C)}$$

$$C_p(Na) = 0.31 \text{ cal/(mole)(}^\circ\text{C)}$$

2. EXPERIMENTAL DATA ARE INDICATED AS FOLLOWS:

(A) = PEAK PRESSURE (MEASURED), RUN A.

[A] = PEAK TEMPERATURE (CALCULATED FROM MEASURED PEAK PRESSURE), RUN A.

Fig. 44. Peak Cell Pressure vs Sodium-to-Oxygen Mole Ratios

Humphreys' investigations were performed under near-ideal conditions. In particular, the volume of the reaction vessel traversed directly by the ejected sodium was undoubtedly a very large percentage of the total volume of the vessel. While the shape or trajectory of the ejected

sodium is not known, it can be conjectured that it assumed the shape of an inverted frustum of a solid having a hyperbolic- or parabolic-like surface of revolution. For simplification consider an inverted frustum of a cone. In this case, it is readily seen that solid angles as small as 15 to 30° with the vertical would permit the ejected sodium to pass through a substantial percentage--60 to 85%, respectively--of the vessel space, thereby providing good mixing of the sodium with the vessel atmosphere. This very favorable geometry would promote high peak experimental pressures and temperatures. However, even under these conditions experimental peak pressures rarely exceeded 60% of theoretical values and, in many cases, were as low as 40%.

In FARET, the situation is considerably different. First, the reactor vessel always will be covered when the cell atmosphere is air. The cover should serve to decrease the efficiency of ejection. Secondly, the shape of the upper portion of the vessel flares inward. Humphreys reported that, for the same ejection energy, outwardly-flared ejection reservoirs appeared to promote higher peak pressures than a nonflared reservoir. This was attributed to a wider dispersion cone which, in turn, permitted better initial mixing in the immediate reaction zone directly over the reservoir. Thirdly, the percentage of the reaction chamber that is encompassed in the dispersion cone is expected to be considerably less in the FARET cell than in Humphreys' reaction vessel. For example, for ideal dispersion cones having solid angles of 15 to 30°, the percentage of cell volume encompassed by these conical shapes is 14 and 32%, respectively, in FARET, as compared to 60 and 85%, respectively, in the experimental vessel. It is expected that an additional decrease of 1/4 to 1/3 in peak pressure could be attributed to this factor.

The ejection energy per unit weight of ejected material in the experimental work covered an ejection energy rate of from 5.3 cal/g to 31.7 cal/g. In the case of FARET, ejection energies of about 0.5 cal/g to 17 cal/g might prevail, based upon a total energy to eject sodium upward of 0.3×10^7 cal.* The higher ejection energy per unit mass of sodium results from the assumption that a quantity of sodium equal to that within the core support structure above the core mid-plane is ejected into the cell. This amounts to about 400 lb (181,000 g) of sodium. Further, the range of the quantity of sodium ejected and the range of ejection energy employed in Humphreys' investigations, when scaled up to the size of the FARET cell, correspond to explosive ejection of from 260 to 2100 lb of sodium with ejection energies of from 0.37×10^4 kcal to 1.3×10^4 kcal (equivalent to 7.4-26 lb of TNT).

It is believed that peak pressures achievable by sodium-air reactions within the FARET cell should not approach the maximum values

*See Appendix B for general discussion of energy partition.

obtained experimentally. The ejection into the cell atmosphere of large quantities of sodium of the most ideal size and shape for efficient reaction is improbable. The large, rectangular shape of the cell does not contribute to the favorable dispersion of finely-divided sodium throughout its volume. Furthermore, the proximity of the reactor vessel to the cell walls and to other equipment within the cell probably would cause a significant quantity of sodium to impinge upon these surfaces and thus to suppress complete reaction with the air. All of these effects would tend to reduce the peak pressure that obtains.

Based upon these considerations, it is believed improbable that the peak pressure in the FARET cell will exceed $1/4$ to $1/3$ of the values obtained experimentally under near-ideal conditions. As mentioned earlier, the experimental values rarely exceeded about 60% of theoretical. The theoretical peak value for FARET is calculated as 97 psig. Accordingly, peak pressures in excess of about 20 psig are considered improbable.

The case of a sodium-oxygen reaction having a peak pressure of about 20 psig was studied. Figure 45 shows the cell gas pressure vs time for the case of convection and conduction cooling only. The inclusion of a radiative mode of heat transfer would cause the gas pressure to fall more rapidly with time than with convective cooling. Although the high gas temperatures indicate that the radiative heat transfer undoubtedly exists, it was not included in these preliminary analyses.

Fig. 45. Cell Gas Pressure vs Time after Ejection of 15,000 lb of Sodium into Cell (Based upon cell volume only)

6. Sodium Pool Burning in the Cell

The pressure-time history in the cell due to pool burning of an exposed sodium surface was also calculated. This situation could result if the reactor vessel and/or the wet fuel storage tank were inadvertently uncovered at a time when the cell was filled with air instead of the usual argon. The other assumptions used in this calculation were:

- (1) Cell air temperature = 100°F.
- (2) Sodium temperature = 300°F.
- (3) Water vapor content = 0.0108 lb/lb dry air.
- (4) Sodium pool burning rate = 1.3 lb/(hr)(ft²). (From Ref. 7.)
- (5) Reactor vessel sodium exposed surface area = 68.4 ft²
(D = 112 ft).
- (6) Wet fuel storage tank sodium exposed surface area = 113 ft²
(D = 12 ft).
- (7) The chemical reactions used are described in the previous paragraphs under "Sodium Chemical Reactions."
- (8) The sodium-water vapor reaction proceeds at a rate such that all water vapor reacts in one hour.
- (9) The heat release to the cell is constant at the rate defined by assumptions (4) and (8).
- (10) Various heat transfer coefficients were assumed or calculated.
- (11) Perfect Gas Law was valid.

Other cases were considered wherein the reactor vessel and the wet fuel storage tank were uncovered individually and simultaneously. These cases and their respective temperature distributions are listed in Table 23.

TABLE 23. Summary of Results for Pool Burning of Sodium

Case	Description	Cell Gas Temperature Distribution
A	Reactor Vessel Open	$T = 614 - 52 e^{-0.1t} - 462 e^{-1.73t}$
B	Wet Fuel Storage Tank Open	$T = 877 - 135 e^{-0.0915t} - 642 e^{-1.884t}$
C	Both Open	$T = 1289 - 321 e^{-0.08t} - 868 e^{-2.13t}$

The letter T is the temperature (in degrees Fahrenheit) of the cell gas at t-hours after the start of the fire. The cell pressure is found by substituting the appropriate temperature T into the equation:

$$P = \left(\frac{T + 460}{560} - 1 \right)_{12.2}$$

where P is the cell pressure in psig. The results are plotted in Fig. 46 for each of the three cases. The maximum temperature for each case was calculated from the temperature distribution of Table 23. The peak pressures were then found to be 11.2, 16.9 and 25.9 psig for cases A, B, and C, respectively. All of these values are below the cell design pressure.

Fig. 46. Pressure Rise in Cell due to Pool Burning of Sodium

Figure 46 was prepared without taking into account the fact that at about 2 psi in the cell the barriers between the cell, vault, and cavity will rupture. This will result in more than doubling the relief volume for the same pool surface. As a result, all pressures in Fig. 46 can be halved. Also, the cell annex volume was not accounted for and this further significantly reduces the calculated pressures from pool burning. Subsequent to this analysis FARET design changes occurred which in effect made the results still more con-

servative. The following two additional problems were then evaluated. Because of the certainty of the design changes it was reasonable to use revised criteria related to the changed vessel and storage tank surface areas and the addition of the cell annex.

A. Burning Rate 5.0 lb/hr-ft² for Case C, Table 24

The results of this calculation include a reduction in the diameter of the fuel storage tank to account for the new vessel size. The new FST area is 31 ft². Using this new area and the same assumptions as listed previously the temperature distribution in the cell is

$$T = 2257 - 1810 e^{-2.566t} - 347 e^{-0.095t}.$$

This gives a peak pressure of 47 psig and compares to the values of Fig. 46. This is a very conservative value because assumption (9) was for a constant

and continuous heat release to the cell. When the effect of oxygen depletion is included a smaller peak pressure certainly obtains. Taking credit for the fact that the oxygen supply is limited, the actual pressure should not exceed 14.4 psi. This is still without taking into account the cell annex or the vault-cavity volume.

B. Burning Rate 5.0 lb/hr-ft² and Burning Area 1200 ft²

For this case, the temperature distribution is

$$T = 25900 - 19120 e^{-0.1425} - 6680 e^{-7.866t}$$

This results in a peak pressure of 271 psig. Based upon the same considerations as discussed in (a) above, the maximum possible pressure is 115 psig. This situation is considered to be quite unrealistic.

7. Filter Protection

The absolute filters are located about 50 to 60 ft from the cell and are connected to the cell by a vent line of about two inches diameter. The vent line will be provided with double valving and with instrumentation to isolate the filters from the cell in event of an increase in radioactivity in the vent gas.

The deposition of sodium oxides following the sodium-air reaction in the cell is a somewhat abstract phenomenon. Several investigators have performed liquid metal burning experiments to establish the efficiencies of various filters to remove particulates generated (in smoke form) from such reactions. In some tests the investigators attempted to establish particle size measurements of the smoke constituents. However, these efforts have not been successful, since the oxides have a great affinity for water. Oxide particles undergo size changes quite rapidly and continuously when exposed to air containing moisture and the resulting sodium hydroxide particles rapidly agglomerate. Further, sodium hydroxide is quite tacky and tends to adhere to surfaces.

Oxides formed in the cell would, upon leakage into the building, rapidly combine with the moisture in the building atmosphere to form the hydroxides. The hydroxides would tend to agglomerate, settle to the floor and/or adhere to other surfaces within the building.

It is considered very unlikely that significant amounts of the sodium hydroxide particles would reach the absolute filters because of the low velocity of air movement in the building at that time (10,000 cfm exhaust and a building volume of about 1,000,000 cu ft). Moreover, because of the long distances involved (50-60 ft) between the cell and the filter bank, the gas temperatures at the filters are not considered to be a problem.

8. U-Pu Separation Effects

Recent Doppler effect measurements^{47,48} have indicated that fertile isotope U^{238} has a negative Doppler coefficient as theoretically predicted. A similar measurement on the fissile isotope of Pu^{239} has also yielded a loss in reactivity, but very small. This has not as yet been established as being due solely to the Doppler effect. However, regardless of the sign, the analysis thus far does indicate that the Doppler effect in the Pu^{239} is very much smaller than has been theoretically predicted. A positive but smaller than theoretically predicted effect has also been measured for U^{235} . Therefore, the computed Doppler coefficients (overall value of -0.63×10^{-6} for U and $<0.1 \times 10^{-6}$ for Pu, both at 750°K) are to be considered as conservative estimates. The computations are based on Breit-Wigner, nonoverlapping line shapes. This always results in computing higher reaction rates.

The possible segregation of the plutonium and uranium oxides^{49,50} (or possibly carbides) in a mixture could introduce a small-time delay in the transfer of heat by conduction from the plutonium to the uranium oxide. Heat energy transferred by radiation and fission fragment energy, however, would be prompt. Therefore, the overall effect on the time constant for a mixture, where the plutonium oxide is the heat source, is expected to be small.

The segregation or migration of mixed fuels is not considered a problem in the fuel compositions being anticipated for the FARET Core studies. The fuel element designs and compositions in FARET Core I involve enriched uranium so that the uranium component in the fuel mixture is a heat source.

9. Ground Level Activity Release

Up to this point it has been assumed that all activity was released to the atmosphere via the ventilation stack. This assumption is valid because the design is based on a guaranteed stack release. This is accomplished by provisions which assure that the building ventilation exhaust blower will operate continuously. Under this condition it is unlikely that the building pressure will rise significantly above ambient. However, in the extremely unlikely event of system failure, the building pressure is still not expected to rise much above ambient, since the total volume of released fission products and argon which escapes to the building is small when compared to the total building volume of about 10^6 ft^3 .

Nonetheless, the result of an activity release from the containment and building at ground level was investigated to provide a general appraisal of the effects of this highly unlikely situation. For this case, the maximum off-site concentrations would occur at the EBR-II site. For a ground release,

$$\left(\frac{X}{Q}\right)_{\max} = \frac{2}{\pi C_y C_z \bar{u}(x)^{2-n}}$$

where x = distance from FARET to EBR-II = 1219 m

$$C_y = C_z = 0.035 \text{ for ground release}^{21}$$

which gives

$$\left(\frac{X}{Q}\right)_{\max} = 6.1 \times 10^{-3}$$

Finally,

$$TID_i = 3.1 \times 10^{-6} D_i Q_{i,0} L_B$$

and

$$TID_{p_i} = 3.1 \times 10^{-6} Q_{p_i} L_B$$

For leakage of solid fission products and plutonium, the term L_B was increased by a factor of 1000 to account for the lack of absolute filters in the leakage path. Further, the building structure was assumed to have no effect on the leakage rate therefrom, even though recent investigations indicate that a conventional building structure may indeed have a significant effect.⁵¹

10. Summary of Facility Description in Volume I

The site for the FARET Facility is in the East Area of the National Reactor Testing Station, Idaho.

The facility comprises a single building of conventional construction and miscellaneous structures. The building will house a reinforced-concrete structure consisting of three integrated enclosures: cell, cavity, and vault. These enclosures provide containment and biological shielding for the reactor, all radioactive components of the main and auxiliary sodium heat-transport system, and the fuel handling, fuel disassembly, and spent-fuel storage systems.

The reactor vessel, vessel supports, control-rod drives, fuel-handling equipment, and spent-fuel storage facilities are all located within the cell and cavity. The vessel is supported at the bottom of the cavity and is guided laterally near the top. Within the vessel is located a removable

core support cylinder which positions and supports the in-core components. The reference-design fuel subassemblies are similar to those used in the EBR-II except that each FARET subassembly includes a 5-ft shielding section which extends above the coolant outlet region within the vessel.

Control rods and drives. The control rods, containing neutron-absorbing materials, are driven individually by individual overhead drive mechanisms mounted atop the reactor vessel cover. The cover and rod drives protrude up into the cell above the cavity. All eight drives are removable as a unit to provide access to the reactor vessel. Safety devices on the vessel cover ensure that all control rods disengage from the drives before the cover can be lifted appreciably. The cell also contains the remote-controlled fuel-handling equipment and the fuel storage facilities.

Cooling systems. The reactor is cooled by two sodium heat-transport systems in series. The heat is ultimately dissipated to the atmosphere. The main coolant system comprises primary and secondary sodium heat-transport loops, including one full-capacity pump in each loop, an intermediate sodium-to-sodium heat exchanger, and in the secondary system a sodium-to-air heat exchanger. The system is designed to remove 50 MWe of heat generated in the core, with a 240°F coolant temperature rise through the core, a coolant inlet temperature ranging from 600°F to about 960°F, and a maximum coolant outlet temperature capability of about 1200°F.

A similar but smaller auxiliary coolant system removes shutdown decay heat. Designed to remove about 3 MWe of heat by forced circulation, this system operates in parallel with the main coolant system so that, in case of main-system failure, the reactor will be cooled adequately. The secondary loop of this system uses NaK rather than sodium. In the event of a main power failure, the pumps, NaK-air heat-exchanger blower, and other essential equipment are automatically switched to an emergency power supply.

Both the main and auxiliary primary-coolant loops are in the vault. Both the main and auxiliary secondary loops are located in the rear cell area. The liquid metal-air heat exchangers are outside the building. To ensure adequate natural convection of the coolant for long-term reactor shutdown cooling requirements, the main system heat exchangers are placed higher than the reactor.

Special precautions are taken to eliminate the possibility of inadvertent loss of sodium from the core region. The reactor vessel and essential piping leading from the cavity to the process systems in the vault are of

double-walled construction. The built-in drain piping for the reactor vessel is designed so that it requires a vacuum or pressure assist, in addition to valve operation, to drain the vessel.

The design excludes water or appreciable quantities of hydrocarbons inside the containment structure. Inert-gas atmospheres in these enclosures provide space cooling, permit fuel handling with exposed sodium and suppress possible sodium chemical reactions. The water-cooled inert-gas systems are equipped with water-leak detectors. They are designed to preclude significant addition of water to the containment zones in the event of system failures. In addition, sodium leak detection devices will be installed in appropriate building and containment areas.

Access. The cell, which is designed to permit personnel entry for loading special instrumented subassemblies, maintenance, or equipment adjustments, has two personnel locks. One lock is for normal access; the other is for emergency escape. Other openings are provided for personnel access to the vault and for removal of large components and/or irradiated fuel. All penetrations are designed to maintain the integrity of the containment structure.

Equipment. The cell contains a fuel-handling machine, manipulator, overhead crane, and other equipment to facilitate remote-controlled operations. These include removal of the vessel cover, subassembly unloading, transporting, cooling, storage, and partial disassembly.

Many safety features are incorporated into the fuel-handling machine to prevent inadvertent release of a fuel subassembly during remote-controlled fuel-exchange operations. These features include: (1) system interlocks to ensure conformance to the prescribed sequence in which these operations are to be performed, (2) rate-limited motion for withdrawal or insertion of a subassembly, (3) devices to limit the load which the machine can impose upon the subassembly, and (4) positive-locking mechanisms to retain the subassembly during other than normal engagement or release operations.

Normally, each irradiated fuel subassembly is removed from the reactor vessel to the wet fuel storage tank located in the cell. After a prescribed decay period, the subassembly can be partially disassembled so that the active fuel and the shield and reflector sections are separated and placed in individual shipping containers within the cell. From the cell, the containers are transported in a shielded cask-carriage assembly through a tunnel leading to a loading dock outside the cell.

Building. The building external to the containment structure is serviced by a conventional ventilation system. The atmospheric pressures in various zones of the building can be regulated to ensure air flow from

zones of least contamination suspect to those of higher suspect. Effluent air is monitored, filtered, and discharged to the atmosphere through an exhaust stack.

Radiation detectors are installed throughout the building and containment structure. A self-contained, portable, air-monitoring unit is available for routine gas and particulate analysis. If the detectors monitor radiation intensities above present levels, visual and audible alarms are energized in the FARET control room and at the local security guard station.

Smoke detectors for sensing sodium fires are installed in appropriate areas of the building and containment structure. Manual and automatic fire alarm systems are interconnected with the NRTS fire alarm grid and the nearby EBR-II fire department.

Oral communication systems include: (1) a conventional telephone system operating through the EBR-II switchboard, (2) a local paging system integral with the telephone system, (3) a sound-powered telephone system interconnecting the control room and other areas of the building, and (4) a separate communication system between the cell interior and the control room.

The control room is located in the support wing of the reactor building. It contains the control console and principal control panels. On these panels are displayed and recorded all parameters vital to the operation and safety of the reactor, major process equipment, and the proposed experimental program.

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