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PRELIMINARY ANALYSIS OF POSTULATED
MAXIMUM ACCIDENTS FOR THE FFTF

November 1968

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PRELIMINARY REPORT

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PRELIMINARY ANALYSIS OF POSTULATED MAXIMUM
ACCIDENTS FOR THE FFTF

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Printed in the United States of America
Available from
Clearinghouse for Federal Scientific and Technical Information
National Bureau of Standards, U. S. Department of Commerce
Springfield, Virginia 22151
Price: Printed Copy \$3.00; Microfiche \$0.65

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ABSTRACT

Preliminary studies have been performed to coordinate characteristics and magnitude of a nuclear accident for the Fast Flux Test Facility (FFTF).

Although a mechanistic approach was used to determine the inception and propagation of possible major accidents, the degree of conservatism applied to the significant parameters involved clearly indicates that an event such as that evaluated should be considered a hypothetical accident.

The reactivity consequences of initial failure conditions were evaluated on the basis of the characteristics of the FFTF reference design. Since the backup vertical core design contains lower reactivity worth components, the same initial conditions imposed on that reactor would result in correspondingly lower ramp insertions.

Methods for analyzing the core nuclear and thermal response during power transients, up to core disassembly, were developed which consider the consequences of fuel melting with subsequent core compaction and also sodium voiding effects. Core responses were analyzed assuming the presence of sodium in the core and sodium out of the core during the power transient, although the sodium-in analyses are not fully developed. In recognition of the possibility

of molten fuel-sodium interactions, high sodium voiding rates were assumed to occur once the fuel reaches a specified failure threshold.

Excursion analyses, including core disassembly, were performed with a new computer program, MAX, similar to the Weak Explosion Program, but with added flexibility and the inclusion of movable mesh points.

The work energies capable of being produced in these nuclear excursions were evaluated assuming that the fuel vapor is the working fluid for sodium-out conditions and that sodium is the working fluid under sodium-in conditions. For the sodium-out cases, the fuel vapor was expanded isentropically to one atmosphere to evaluate the work energy. For the sodium-in cases, the sodium was assumed to be heated by complete mixing of the core sodium and fuel. The work energy was calculated for isentropic expansion without further heat transfer from the fuel, and also for expansion with continuous thermal equilibrium between fuel and sodium. The latter case represents an upper limit for work energy due to expansion of core sodium vapor.

It is concluded that a limit to the magnitude of a nuclear excursion in the FFTF would be in the range of 500 to 1500 MW-sec work energy release, where the energy is the work which could theoretically be done by expansion of vaporized core materials.

TABLE OF CONTENTS

ABSTRACT	iii
LIST OF FIGURES	vii
LIST OF TABLES	ix
1. INTRODUCTION	1
1.1 Objective	1
1.2 Outline of Approach	1
2. SUMMARY	4
2.1 Analyses.	4
2.2 Results	8
2.3 Conclusions	11
3. BACKGROUND	12
3.1 FFTF Concept.	12
3.2 Precedents for Maximum Accident Studies	14
3.3 APDA Studies	17
4. PHASE I - INITIATING MECHANISMS	26
4.1 Fault Tree	26
4.2 Reactivity Consequences of Potential Failure Modes	28
5. PHASE II - KINETICS ANALYSIS	33
5.1 Meltdown Model	36
5.2 Analysis of Operational Accidents	48
5.3 Analysis of Shutdown Accidents	64
5.4 Summary of Phase II	69
6. PHASE III - DISASSEMBLY ANALYSIS	71
6.1 Calculational Model	71
6.2 Parametric Studies	75
6.3 Summary of Phase III	97
7. PHASE IV - ENERGY PARTITIONING	101
8. CONCLUSION	104
ACKNOWLEDGMENTS	106
REFERENCES	107

APPENDIX A - MAXIMUM ACCIDENT DEFINITIONS FOR EBR-II, FERMI, FARET AND SEFOR	113
APPENDIX B - EQUATION OF STATE	118
APPENDIX C - WORK ENERGY CONSIDERATIONS	132
C.1 Techniques for Determining Work Energy	132
C.2 Experimental Bases for Work Energy Evaluation	138
C.3 Work Energy Due to Molten Fuel-Sodium Interaction	142
APPENDIX D - SODIUM EXPULSION MECHANISM	152

LIST OF FIGURES

1	APDA Parametric Results	23
2	Rod Expulsion Acceleration Rate	30
3	Fuel Drop-In Reactivity Insertion Rate	32
4	Meltdown Model	37
5	Radial Fuel Worth Curve Employed in the Meltdown Calculations	45
6	Axial Fuel Worth Curve Employed in the Meltdown Calculations	46
7	Relative Radial Power and Temperature Distributions Assumed for the Meltdown Calculations	47
8	Sodium Void Worth Curves Employed in Sodium Void Calculations	49
9	Reactivity Components of Full Power FTR with a 100\$/sec Initiating Ramp Rate and No Sodium Voiding	51
10	Melting Pattern in Full Power FTR with a 100\$/sec Initiating Ramp Rate and No Sodium Voiding	53
11	Reactivity Components of Full Power FTR with a 100\$/sec Initiating Ramp Rate and 1000 g Sodium Voiding	54
12	Voiding and Melting Patterns in Full Power FTR with a 100\$/sec Initiating Ramp Rate and 1000 g Sodium Voiding	56
13	Disassembly Ramp Rate as a Function of Sodium Voiding Rate	59
14	Sodium Ejection Curves Used in Loss-of-Coolant Study	62
15	Loss-of-Coolant Transient with Low Superheat Before Voiding	63
16	Reactivity Transients for Loss-of-Coolant Conditions	65
17	Disassembly Ramp Rates for Sodium-Out Shutdown FTR	67
18	Power Distribution Employed in the Disassembly Phase	77
19	Fuel Worth Distribution Employed in the Disassembly Phase	78
20	Power Trace for the Reference Accident	80
21	Reactivity Components During Disassembly of the Reference Accident	81
22	Effect of Fuel Worth on Energy Release	83

23	Effect of Temperature Peaking on Energy Release	84
24	Effect of Reactivity Ramp and Doppler Coefficient on Energy Release	89
25	Effect of Neutron Lifetime on Energy Release	90
26	Effect of the Heat of Fusion on Energy Release	91
27	Effect of Initial Temperature on Energy Release	93
28	Effect of Initial Power on Energy Release	94
29	Effect of Doppler Coefficient on Energy Release for Shutdown Accident	96
30	Energy Generation Rates in a Particular Disassembly Accident	102
B.1	Calculated and Measured Vapor Pressure Curves	121
B.2	Pressure-Energy Relationships	125
B.3	Maximum Work Energy from Fuel Expansion	127
B.4	Final Fuel Vapor Fraction After Isentropic Expansion Using Menzies' Critical Constants	129
C.1	Temperature-Entropy Diagram for Sodium	135
C.2	Heat Capacity of Saturated Sodium Liquid	145
C.3	Heat Gained or Lost by Sodium and Molten Fuel	146

LIST OF TABLES

I	FTR Core Design Parameters	13
II	APDA 1-D Fuel Comparison Studies	20
III	APDA 2-D Oxide Studies	21
IV	Core Parameters Used in the Meltdown Phase	44
V	Fuel Pin Parameters Used in the Meltdown Phase	44
VI	Disassembly Ramp Rates for Sodium-Out Full Power FTR	57
VII	Disassembly Ramp Rates for Sodium-In Full Power FTR	58
VIII	Disassembly Ramp Rates for Sodium-In Shutdown FTR	68
IX	Characteristics of APDA 2-Dimensional Case	73
X	Comparison of 1 and 2-Dimensional Models	73
XI	Characteristics of the Reference Accident	79
XII	Effect of Equation of State on Energy Release	86
XIII	Characteristics of Postulated Maximum Accidents for the FTR	100
XIV	Maximum Work Energy by Sodium Vapor Expansion to 1 Atm (MW-sec)	105
XV	Summary of Results	105
B.I	Critical Constants of UO_2	120
B.II	Parameters for Corresponding States Calculations	124
C.I	Typical Parameters for Evaluation of Work Energy by Sodium Expansion	137
C.II	Comparative Results: Sodium Expansion Analysis	138
C.III	Disassembly Mechanisms for Thermal Reactor Destruct Tests	140

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1. INTRODUCTION

1.1 Objective

This report presents the results of studies and analyses of postulated maximum accidents for the Fast Flux Test Facility. The type of accident postulated, is a nuclear excursion which is initiated by a hypothetical series of failures and is terminated by disassembly of the fueled core. The theoretical work energy resulting from expansion of the vaporized core material is analyzed and defined. Additional energy which could be released by possible chemical reactions, such as sodium fires following the nuclear excursion, is not considered within the scope of this report.

1.2 Outline of the Approach

A dual effort of maximum accident analysis for the FFTF was carried out during the conceptual design phase. The work was performed by Atomic Power Development Associates (APDA), under contract to the Pacific Northwest Laboratory (PNL), and by PNL itself.

- (a) Atomic Power Development Associates was retained to perform accident studies using the Weak Explosion Program⁽¹⁾ (WEP) and later the two-dimensional version of WEP, MARS.⁽²⁾ In these initial studies, the hypothetical core collapse model used in the FERMI⁽³⁾ analyses was employed to establish the conditions for initiation of the nuclear excursion.

- (b) The PNL effort used a more mechanistic approach. A series of system and component faults were investigated to evaluate the possible consequences of each failure sequence; the various consequences then provided the basis for the maximum accident conditions.

The APDA work began early in the conceptual design stage with an initial effort placed on a comparison of the consequences of accidents in cores fueled with four possible fuel types: mixed oxide ($\text{PuO}_2\text{-UO}_2$), mixed carbide (PuC-UC), cermet ($\text{PuO}_2\text{-SS}$), and metal alloy (PuZr-UZr). These comparisons were performed using the core collapse model and the one-dimensional Weak Explosion Program. Simultaneously with these studies, APDA developed the two-dimensional MARS Program and, subsequently, the mixed oxide accident studies were expanded to investigate effects of multi-zoned fuel loadings. The final APDA work for the conceptual design consisted of a parametric study to evaluate the sensitivity of the calculations to various factors independent of the core meltdown model. The parameters in these studies included core size, initial ramp reactivity rate, core zoning, and the Doppler coefficient.

The PNL effort was directed toward analysis of the course of postulated accidents on as plausible and realistic a basis as possible. The analysis was subdivided into four major areas:

- (a) Investigation of possible initiating faults through some logical means such as a Fault Tree.
- (b) Analysis of reactor response to these faults with consideration of reactivity feedback mechanisms and the time sequence of accident progression.
- (c) Analysis of nuclear excursions resulting in core disassembly, using as input the results of item (b).

- (d) Analysis of energy partitioning to determine work energies, considering interactions of the various core materials.

Although initial scoping studies were performed for comparison of the magnitude of the energy release for various types of fuel materials in the FTR, the studies were based primarily upon the use of mixed oxide fuel. This is the initial core driver fuel, and it presently appears that the potential energy release with cores of other fuel types will not significantly exceed that of the oxide core.

2. SUMMARY

2.1 Analyses

For any reactor facility, a maximum accident may be postulated which represents an upper bound to definable accidents to which the facility might be subject, with allowance for uncertainties in both accident definition and analysis. This accident, and the sequences of events leading to it, are of such low probability as to lend no credence to a finite possibility of occurrence.

As has been done for other fast reactors in this country, maximum accident studies for the FFTF consider an accident which results in disassembly of the fueled core in an explosive-like manner. Hypothesized sequences of events leading to disassembly result in rapid reactivity insertion which initiates the power transient. The transient is subsequently terminated by core disassembly after sufficient pressure is generated to overcome the inertia of the core materials.

Different paths leading to severe failure conditions during power operation and which have been considered are outlined as follows:

- (a) Control Rod Expulsion - Expulsion of a 4\$ reactivity worth control rod by sudden vaporization of sodium with 400 °F superheat could result in a maximum ramp reactivity insertion rate of ~50\$/second.
- (b) Single Channel Meltdown Without Scram - Meltdown of a fuel subassembly could result in a ramp reactivity insertion of nearly 1\$ at a rate approaching 4\$/second. Without scram, the feedback effect due to gross core melting and gravity collapse could increase the ramp to ~20\$/second.
- (c) Total Flow Blockage - Sudden flow blockage of the entire core without scram would result in sodium

vaporization; if the central sodium void coefficient is positive, this would result in a reactivity insertion. Assuming 400 °F superheat, a pessimistically large positive void coefficient, and simultaneous voiding of all channels with positive void coefficients, this could result in a ramp reactivity insertion of more than 1\$ at a rate up to ~70\$/second.

During very low power or shutdown conditions, the following failure paths have been considered:

- (a) Continuous Reactivity Insertion - Rods are continually withdrawn or fuel charged through critical and without scram, at a rate of up to 1\$/second.
- (b) Fuel Drop-In Accident - A fuel assembly is dropped into a core position when the core is inadvertently just barely subcritical. This results in a peak ramp reactivity insertion of ~60\$/sec with a free fall of 10 ft. For free fall in sodium from the top of the reactor vessel, a maximum velocity would be reached which would correspond to a peak ramp rate up to ~75\$/second.

The consideration of these accidents does not necessarily imply that they are "credible" in the sense that there exists a substantial chance of occurrence during the reactor lifetime. It is a characteristic of the mechanistic approach that any accident, once identified, can be eliminated or its likelihood minimized by design. Nevertheless, the postulated circumstances are definable conditions consistent with the laws of nature and the conceptual characteristics of the facility. Consideration of specific design features to prevent the occurrence of such accidents was beyond the scope of this study.

In order to bound the range of initial conditions defined, a ramp rate of 100\$/second was chosen as the initial reactivity ramp rate for analysis of accident magnitude.

The progression of the accident up to the beginning of core disassembly was analyzed with the MELT-I computer code. Several feedback reactivity effects interact during this phase of the accident, denoted the "meltdown phase", and are accounted for in the analyses. The primary feedback contributors are:

- The Doppler effect
- Reactivity addition due to core collapse
- Coolant void reactivity effects.

Cases were investigated for the defined reactivity insertion rate assuming both sodium-in and sodium-out of the core during the power transient. With the sodium-out cases, an equation of state was used which allowed considerable fuel expansion before buildup of significant pressures. During these runs, only the Doppler and collapse effects were applicable. (It was found that with an initial ramp rate of 100\$/sec, the additional effect of core collapse at disassembly is <5\$/sec, whereas with an initial ramp of 4\$/sec, the feedback effect of collapse is +16\$/second.) With the sodium-in cases, it was found that the void effect can produce a large additive reactivity ramp (+150\$/sec) by sudden fuel-sodium interactions just at initiation of disassembly - the magnitude depending upon the sodium acceleration upon expulsion (the analyses assumed accelerations up to 1000 g's). However, using a sodium-in equation of state in those regions which were not voiding resulted in an energy release of similar magnitude to the sodium-out cases.

Analyses of the core disassembly were performed using the MAX computer code⁽⁴⁾ a program similar to the Weak Explosion Program,⁽¹⁾ but with substantially enhanced flexibility. The

program allows a reasonably direct transfer of the output of the MELT-I analyses to input conditions for the disassembly (MAX) analyses. The MAX code also contains a flexible form for the fuel equation of state allowing an appropriate fit to predicted values at both low and high pressures.

Evaluation of the capability of the energy generated during the power transient to produce destructive work upon the surrounding media, and analysis of the post-transient equilibrium conditions, remain areas of significant uncertainty. It has been past practice to make a conversion of calculated work energy to equivalent pounds of chemical explosives to allow structural designers a means of correlation for containment responses to available experimental data developed with chemical explosives. Conversion of the nuclear excursion to an explosive equivalent was considered in this study; however, the differences between the characteristics of nuclear and chemical energy releases make it unlikely that a generally applicable equivalence exists. Thus, any determination of the chemical explosive equivalent for the postulated accidents is not within the scope of this report.

The work energy from expansion of the vapor products following the nuclear excursion can be determined from:

- (a) Expansion of the fuel vapor products.
- (b) Expansion of sodium vapor products after reaching a calculated equivalent temperature evaluated by mixing with all fuel which is at or above the melting point.
- (c) Expansion of the sodium vapor products as in (b), and assuming continued heat transfer from the fuel during the entire expansion process.

The work as determined by process (a) above is considered appropriate in analyzing the sodium-out case. With sodium-in, processes (b) and/or (c) are used which yield considerably more work energy. Process (c) is, of course, the most conservative and further studies will be required to show whether it is physically possible to have sufficient heat transfer between the sodium and fuel during the rapid expansion process to produce this effect. It was assumed that only sodium in the core region at the time of disassembly is available to do work. This assumption is considered valid at least for the sodium-in analyses, which yield the highest calculated work energies. The additional work energy from expansion of sodium vapor which may result from post-disassembly mixing of fuel and sodium outside the core region for the sodium-out conditions has not been calculated, but is expected to be no greater than the conservative results for the sodium-in conditions.

2.2 Results

For sodium-out conditions, the nuclear excursion resulting from a 100\$/sec reactivity insertion ramp occurring at full reactor power was calculated to have the following results:

Total energy generated in the nuclear excursion	6700 MW-sec
Energy deposited in fuel at temperatures ≥ 3000 °K	4000 MW-sec
Work by isentropic fuel vapor expansion to 1 atm	400 MW-sec
Peak mass average temperature of molten fuel	4850 °K

In expansion to a pressure of one atmosphere, approximately 15% of the core fuel mass would be vaporized.

A sensitivity study was performed for the sodium-out conditions to investigate the effects of significant parameters,

including the Doppler coefficient, the initial core average temperature, and the initial power. The energy of the excursion as a function of the Doppler coefficient and initial temperature show the characteristic oscillations demonstrated in other studies. The peak energy release over the ranges of interest occurred with a Doppler coefficient of ~ 0.003 and an initial average temperature of ~ 1500 °K.

A similar sensitivity study was not performed for the sodium-in conditions. The complexities added by the application of spatial variations in the equation of state, the void rate caused by sodium-fuel interactions, and sodium-fuel energy exchanges at disassembly, greatly increase the uncertainties of these analyses. However, the representative analysis employed is believed to be conservative. By increasing the 100\$/sec ramp rate to ~ 250 \$/sec at disassembly due to sodium expulsion upon fuel pin failure leading to molten fuel-sodium mixing, the following energy values were calculated:

Total energy generated in	
the nuclear excursion	4000 MW-sec
Energy deposited in fuel at	
temperatures ≥ 3000 °K	1580 MW-sec
Peak mass average temperature	
of molten fuel	3600 °K

The maximum work energy for the sodium-in conditions results from expansion of sodium as the working fluid rather than the fuel. The amount of work which the sodium can do depends on the energy which may be transferred to it from the molten fuel, and therefore is dependent upon the average temperature of the fuel at disassembly. The work energy was calculated for an average fuel temperature of 4000 °K which is higher than that calculated for any

sodium-in condition. After mixing, the equilibrium temperature of the mixture was 3430 °K. Three different methods of sodium expansion from this equilibrium temperature were considered, with the following results:

- (a) Isentropic expansion of the sodium to 1 atm 540 MW-sec
- (b) Expansion of the sodium assuming continual thermal equilibrium with the fuel to the fuel melting temperature and then isentropic expansion of the sodium to 1 atm 980 MW-sec
- (c) Expansion of the sodium to 1 atm assuming continual thermal equilibrium with the fuel. 1290 MW-sec

Process (a) assumes no heat transfer between the fuel and sodium once expansion begins. Process (c) is the most conservative since it assumes perfect contact between the sodium and fuel throughout the expansion process. Process (b) assumes no additional heat transfer after expansion to a temperature corresponding to the fuel melting point.

For transients initiated from shutdown conditions, results of analyses for the 100\$/sec ramp rate were:

Sodium-Out Conditions

Total energy generated in the nuclear excursion	8000 MW-sec
Energy deposited in fuel at temperatures ≥ 3000 °K	3700 MW-sec
Work by isentropic fuel vapor expansion to 1 atm	320 MW-sec
Peak mass average temperature of molten fuel	4650 °K

Sodium-In Conditions

Total energy generated in the nuclear excursion	5500 MW-sec
Energy deposited in fuel at temperatures ≥ 3000 °K	2000 MW-sec
Peak mass average temperature of molten fuel	3800 °K
Work by expansion of sodium vapor to 1 atm	490-1250 MW-sec ^a

a Energy range dependent upon the assumed expansion process as described in (a) to (c) above.

2.3 Conclusions

It is concluded, based upon these preliminary results, that the magnitude of any FFTF nuclear excursion will not exceed the release of 500 to 1500 MW-sec of work energy by expansion of vaporized core materials. Because of the conservative nature of the analyses, in particular the reactivity insertion rates and the sodium vapor expansion model, this figure is considered to represent a conservative upper bound to potential FFTF nuclear accidents.

It is emphasized that the upper value of 1500 MW-sec is based upon analyzed sodium-in conditions in which two major areas of uncertainty exist: (a) The rate of sodium expulsion initiated by molten fuel-sodium interactions which results in large reactivity insertion rates with the core near prompt critical and (b) The work energy by expansion of the sodium following the power transient. The assumed conditions which lead to this value are believed to be extremely conservative; thus, sufficient applicable data could eventually justify a lower design value.

3. BACKGROUND

3.1 FFTF Concept

The Fast Flux Test Facility is an AEC-sponsored project to design and construct a reactor facility to serve fuel and materials testing needs of the AEC Liquid Metal Fast Breeder Reactor Program and other national programs.

During the period of these studies, the definition of conceptual design requirements and characteristics was in progress. Design details were not firm, and it may be expected that substantive variations in design parameters may take place in the future. The allowance for uncertainties in the studies is large enough that the results are expected to be applicable for any design which may evolve from the current conceptual design.

The central feature of the FFTF is the Fast Test Reactor (FTR). The 400 MW_t power produced in the FTR is removed by a liquid sodium primary heat transport system and a liquid sodium secondary heat transport system which dissipates the heat to the air. The primary sodium systems are enclosed in concrete cells, the central one of which is the reactor cavity. A separate refueling cell is located above the reactor and includes spent fuel decay storage positions. The complex of cells is surrounded by a spherical steel containment vessel.

During the lifetime of the facility, it is possible that a number of different core designs and fuel types may be used in the FTR. The reference initial core, however, has a fueled volume of approximately 1000 ℓ , with a height of about 32 inches and an equivalent diameter of about 51 inches. The core is made up of approximately 90 subassemblies, of which about 70 are driver fuel and about 10 are control and safety rod assemblies; the remainder are available as test positions.

In addition, five closed test loops are located within a vertical plane bisecting the core. The core subassemblies, other than the closed test loops, are arranged in a divergent, nonvertical ("conical") array to provide separation of positions at the reactor cover. The initial core driver fuel is mixed $\text{PuO}_2\text{-UO}_2$, contained in fuel pins of about 0.250-in. diameter. Table I contains the principal core design parameters.

In addition to the reference initial core, a "backup" core design is being studied which incorporates a regular array of core subassemblies in a compact vertical arrangement. This design includes about 137 core positions, of which about 125 are driver fuel. The thermal and nuclear characteristics of the two cores are similar.

TABLE I. FTR Core Design Parameters

<u>Parameter</u>	<u>Units</u>	<u>Value</u>
Power	MW_t	400
Core Height	cm	85
Equivalent Radius	cm	65
Total Core Volume	liters	1150
Gap Volume	liters	150
^{239}Pu Mass	kg	650
Delayed Neutron Fraction, β	-	0.003
Neutron Flux (Rods Out)		
Central	$\text{n-cm}^{-2}\text{-sec}^{-1}$	5.46×10^{15}
Peak	$\text{n-cm}^{-2}\text{-sec}^{-1}$	6.00×10^{15}
% $> .1$ MeV	-	67
% > 1 MeV	-	14
Neutron Generation Time	sec	3.5×10^{-7}
Fuel Composition		
% ^{239}Pu	-	90
% ^{240}Pu	-	10
$^{238}\text{U}/^{239}\text{Pu}$ (Zone 1)	-	4.06

TABLE I. (contd)

<u>Parameter</u>	<u>Units</u>	<u>Value</u>
$^{238}\text{U}/^{239}\text{Pu}$ (Zone 2)	-	3.44
Fuel Burnup		
Breeding Ratio	-	0.50
Reactivity Cycle	days	75
Reactivity Loss/Cycle	δk	0.02
Average Burnup/Cycle	MWd/kg	10
Average Total Burnup	MWd/kg	40
Reactivity Requirements		
Fuel Burnup	δk	0.02
Cold to Hot	δk	0.01
Shutdown Margin	δk	0.01
Test Compensations	δk	0.02
Reactivity Coefficients		
Doppler	Tdk/dT	-0.004
Doppler (Na-out)	Tdk/dT	-0.002
Clad Axial Expansion	$\delta k/^{\circ}\text{C}$	-4.0×10^{-6}
Central Subassembly- ^{239}Pu Worth	$\delta k/\text{kg}$	1.18×10^{-3}
Central ^{239}Pu Worth	$\delta k/\text{kg}$	1.75×10^{-3}
Central Subassembly Na Worth	$\delta k/\text{kg}$	$+2.2 \times 10^{-5}$
Central Na Worth	$\delta k/\text{kg}$	-5.0×10^{-5}
Control Elements (in-core)		
Number of Safety Rods	-	3-4
Number of Shim Rods	-	6-7
Safety Rod Worth	δk	~ 0.01
Shim Rod Worth	δk	~ 0.01

3.2 Precedents for Maximum Accident Studies

Three terms have been commonly used in discussion of maximum accident definitions. These are:

- (a) Maximum credible accident (MCA)
- (b) Maximum hypothetical accident (MHA)
- (c) Design basis accident (DBA)

The maximum credible accident is that maximum accident assumed to result from some sequence of credible failures and which results in the maximum expected release of radio-nuclides to the environs. Typically, failures which are classified as credible include:

- . Loss of site power
- . Local flow blockage
- . Stuck reactivity-control rod
- . Primary system leak

Faults of this nature are expected to occur with some finite frequency, such that some implicit value of probability can be assigned to each type of failure. The MCA is then the worst of this class of accident.

No clear definition of the maximum hypothetical accident is available except that it places no significance upon the credibility for occurrence of the accident. One point of view is that an accident is classed as hypothetical if some credible accident occurs and the protective system fails to respond. Another point of view is that a hypothetical accident is used to describe a series of events of accident conditions without regard to physical restrictions or conditions specifically tied to any one design or set of circumstances. In either case, no significance is given to the specific credibility of the accident conditions.

The design basis accident is that accident which determines the design conditions for the containment system; it is chosen such that no accident is expected ever to reach this severity. Either the MCA or MHA approaches, or both, may be used in defining the DBA.

U. S. AEC safety review experience on fast reactors which is most pertinent to the FFTF includes:

- (a) Experimental Breeder Reactor No. 2 (EBR-II)^(5,6)

- (b) Enrico Fermi Fast Reactor (FERMI)⁽³⁾
- (c) Fast Reactor Test Facility (FARET)⁽⁷⁾
- (d) Southwest Experimental Fast Oxide Reactor (SEFOR)⁽⁸⁾

With the exception of FARET, the basis for containment design was an accident substantially more severe than any accident considered "credible". Okrent⁽⁹⁾ has summarized the precedent implied by the Enrico Fermi and EBR-II reactors:

"Examine the reactors for all plausible accidents and make sure no real public safety problem (or threat to the reactor investment, if possible) occurs from such sources, and then go on to look at those other physically credible accidents that are thought to be of much lower probability. Examine these realistically to see whether the reactor design is on the brink of difficulties of a new order of magnitude. If so, determine what new steps in design and what new safeguards can protect against this unlikely event so that the guarantee of safety becomes more nearly absolute." The SEFOR⁽⁸⁾ approach was consistent with that precedent.

The FARET safety analysis⁽⁷⁾ described a reactor containment based on a "maximum credible accident." Emphasis was placed on analysis of realistic sequences of events; the nature of the maximum accident, however, was the same as for the other reactors, that is, a prompt critical nuclear excursion leading to core disassembly. The FARET analysis is not an applicable precedent to the same extent as FERMI, EBR-II, and SEFOR, since the project was cancelled prior to authorization of construction; however, general acceptance of the safety analysis approach was believed to have been received during review of the PSAR.⁽⁷⁾

A summary of the maximum accident approaches for the above reactors is included for further information in Appendix A.

3.3 APDA Studies

Early studies to ascertain the maximum energy release in a major FFTF accident were performed by Atomic Power Development Associates, Inc. One of the first questions was whether the choice of driver fuel for the FTR would significantly influence the containment design. APDA-194⁽¹⁰⁾ contains a comparison of cermet and oxide cores. APDA-208⁽¹¹⁾ supplements this work to include metal and carbide cores. Later work to be published includes a more detailed analysis of the reference oxide core and a parametric study to investigate the effects of zoning, Doppler feedback, fuel specific heat, and reactivity insertion rates. The latter studies utilize a 2-dimensional disassembly code (MARS).

A detailed discussion of the methods used and the results obtained are given in APDA Reports 194 and 208. The 2D studies will appear in APDA-209 and the parametric studies are to be formally reported in the near future. The present discussion is intended only as a brief summary of the studies and some of the major conclusions.

3.3.1 Models

The initial APDA studies used essentially the same analytical techniques as developed for FERMI. The core was initially assumed to be completely molten (flat temperature distribution) and void of all sodium. Since total loss of sodium results in a negative reactivity effect, the molten core was then subcritical. The top plane of the core was thereafter assumed to fall freely under gravity, thus compacting the core and leading to secondary criticality. The rate of core compaction at criticality determined the reactivity ramp rate used in the disassembly calculations.

A maximum energy release was found by parametrically expelling fuel so that criticality, for the resulting smaller

but more dense cores, could be attained only after a greater degree of collapse - thus allowing more time for gravity acceleration to increase the rate of core reassembly. The APDA studies showed that the maximum energy release occurred with a volume of about 50% of the original core volume.

Two disassembly codes were used in the APDA analysis. The Weak Explosion Program,⁽¹⁾ WEP, is a 1-dimensional (spherical geometry) disassembly code which solves a complete set of neutronics and hydrodynamic equations. Worth and power profiles are read in as input and perturbation theory is assumed throughout. All mesh points remain stationary in time and local fuel density changes are disregarded. Doppler effects and delayed neutrons (up to six groups) can be included.

The equation of state is of the functional form of the vapor pressure curve; i.e.,

$$P = B \exp \left[\frac{-A}{E+E_0} \right]$$

where A and B are input fitting parameters, E represents the heat energy generated in the transient, and E₀ is the initial energy content. One of the major limitations of WEP is its intrinsic dependence upon this functional form; i.e., the equation of state over the entire range of interest cannot always be satisfactorily fitted by this form.

A 2-dimensional (R-Z) code,⁽²⁾ MARS, was used for all of the 2-D studies. This code is basically an extension of WEP to two dimensions, but does allow more flexibility in treating the equation of state in that the functional form shown above can be fitted in three separate energy (or temperature) domains.

The fuel equation of state (EOS) was estimated by the method of corresponding states. Because of the functional form of the EOS required by WEP, it became evident that the

calculated EOS could not be adequately fitted over the complete range of interest.

In retrospect, this limitation proved to be quite significant as evidenced by the rather significant changes in the energy release calculation of the oxide core as the APDA studies progressed. The differences in these results merit some explanation.

The 1-D oxide studies focused attention on an accurate fit to the EOS over the high pressure region near the excursion termination point. This procedure grossly overestimated the low pressure region (as bracketed by the Ackermann high and low temperature data - see Appendix B) and the energy releases were correspondingly low. The 2-D studies of APDA-209, in using a lower fit to the low pressure oxide data, resulted in higher energy releases. Finally, with the added EOS fitting capability of the MARS code, the recent parameter studies were conducted using a pessimistic fit to the low pressure oxide data and substantially larger energy releases were calculated. Although Section 6.2 of this report indicates energy release calculations to be rather insensitive to EOS variations, there are two primary differences between the APDA analysis and that of Section 6.2: (a) the APDA variations in the EOS used were very large - as much as a factor of 100 to 1000 over certain ranges, and (b) disassembly feedback was the prime negative feedback mechanism in the APDA studies because of the small Doppler feedback associated with considering cores initially at the molten condition.

Work energies, in all cases, were obtained as follows: The total energy generated (starting from the molten condition) during the nuclear excursion was tallied, and then an isentropic expansion of the molten and vaporized fuel was assumed until the fuel pressure decreased to one

atmosphere. The difference between the total energy generated and the energy content of the fuel at one atmosphere was labeled as work energy.

3.3.2 1-D Comparison Studies

The original APDA study in support of the FFTF was directed towards a comparison of the two fuel types under consideration at that time, namely cermet and oxide. A follow-on study included metal and carbide cores since it is possible that such fuel types might be used in future FFTF cores. All four cores were analyzed using the same ground rules, and the results are summarized in Table II.

TABLE II. APDA 1-D Fuel Comparison Studies

	<u>Oxide</u>	<u>Metal</u>	<u>Cermet</u>	<u>Carbide</u>
Volume (liters)	800	600	600	600
Relative Volume at Disassembly (V/V_0)	0.5	0.5	0.5	0.6
Moles of Fuel (initially)	~8200	9435	~1310	7956
Moles of Fuel (at V/V_0)	5399	6606	887	6052
Core Density (g/cm^3)	6.04	6.68	4.66	6.63
Fuel Density (g/cm^3)	4.05	4.70	0.89	4.65
Doppler Coefficient ($T \frac{dk}{dT}$)	-0.002	-0.00032	0.00027	-0.0021
Ramp Rate ($\frac{\delta k}{\delta t}$ /sec)	1.31 410	1.62 520	1.97 895	1.02 300
Total Energy Generated (MW-sec)	1560	5390	1111	1360
Work Energy (MW-sec)	770	2507	671	544

A significant result of this study was the difference in additional energy release between the metal core and the others. There are a number of possibly important differences between parameters of the metal fuel core and other cores, including the higher critical temperature of metal (thus requiring more energy deposition prior to achieving disassembly pressures),

the lack of a strong negative Doppler coefficient, and a slightly higher core inertial density. Subsequent studies indicate, however, that a relatively more conservative equation of state was used for the metal fuel. Further, as pointed out earlier, the low pressure range for the oxide fueled core was substantially over-estimated in the Weak Explosion Program, thereby leading to low energy releases.

3.3.3 2-D Oxide Studies

As the oxide core evolved as the reference core, APDA was asked to study this core in more depth - mainly to investigate the effects of zoning using the 2-D disassembly code MARS. Computationally, the zoned cases consisted of equal volume driver zones and the radial power profile was adjusted to match power peaks in the two driver zones. The salient features of this study are summarized in Table III.

TABLE III. APDA 2-D Oxide Studies

	Unzoned Core	Zoned Core	Zoned With Test Zone
Volume (liters)	800	800	800
Relative Volume at Disassembly (V/V ₀)	0.5	0.5	0.5
Moles of Fuel (initially)	8196	8200	8010
Moles of Fuel (at V/V ₀)	5211	5183	5094
Core Density (g/cm ³)	5.50	5.48	5.49
Fuel Density (g/cm ³)	3.52	3.50	3.51
Specific Heat (cal/mole °K)	26.	26.	26.
Doppler Coefficient ($T \frac{dk}{dT}$)	-0.0026	-0.0026	-0.0026
Ramp Rate ($\frac{\delta k}{\delta t}$)	1.18 393	1.13 377	1.18 393
Total Energy Generated (MW-sec)	2350	2020	2390
Work Energy (MW-sec)	1310	1060	1350

Difficulty was encountered in interpreting just how the interface between the two equal fuel zones should be handled, i.e., does the boundary move and, if so, should the worth change, etc. Basically, however, bracketing procedures (consisting mainly of a parametric study considering the above questions) were used to support the conclusion that zoning does not appreciably change the energy release, i.e., the differences in energy release between the zoned and unzoned cores are probably not as important as the uncertainties arising from some of the basic assumptions in the calculational model. The equation of state used for the oxide core in this study differed from the original study (in that lower pressures at low temperatures were used) and correspondingly higher energy releases were obtained.

3.3.4 Parametric Studies

The most recent study was made in an attempt to divorce the calculations from the original collapse model assumed. In addition, further investigation of the discrepancy between the oxide and the metal cores was desired.

Whereas the previous studies were based on the Ackermann high temperature data (see Appendix B) in determining critical constants required for the oxide equation of state, the later study adopted the Ackermann low temperature data because of the conservatism ensuing for the vapor pressure region. In conjunction with this, Miller's preferred constants were used to determine the departure point into the single phase region. Also in the parametric survey, the core volumes and geometry were held constant, with the reactivity ramp insertion treated as arbitrary input. As before, however, initial core temperatures were uniform at the melting point in the liquid state.

Figure I illustrates the results of the parametric study for a range of ramp rates up to 400 \$/sec. For the curves

FIGURE 1. APDA Parametric Results

shown, the oxide cores had a Doppler coefficient of -0.002 at a nominal fuel specific heat of $27 \text{ cal/mole-}^\circ\text{K}$. The metal cores had respective values of -0.0003 and $10.3 \text{ cal/mole-}^\circ\text{K}$. Parametrics on the Doppler coefficient revealed the energy release to be a rather insensitive function of the Doppler coefficient - a result which would be expected for the initially molten cores being analyzed. Energy release values were found to be nearly linearly proportional to the fuel specific heat used. This type of behavior might be expected in that the higher the specific heat, the larger the energy deposition before disassembly pressures can be generated.

One of the key conclusions drawn from this study is that containment designed for an oxide core can be expected to be adequate for a metal core (and, likewise, for cermet or carbide cores). The major reason for comparable energy releases for the two fuel types is the use of a more conservative equation of state for oxide than in the earlier studies.

3.3.5 Relationship to the PNL Approach

Initial conditions in the APDA studies outlined above were arbitrarily established and bear no defined relationship to physical conditions which can exist in a reactor. The studies are applicable for evaluation of the sensitivity of results to analytical assumptions, which was their purpose. Undoubtedly, they also could be used to establish theoretical upper limits for accident consequences, but there is no direct way to assess the magnitude of the pessimism which results from neglect of physical cause and effect relationships during progression of an accident prior to the conditions of core disassembly.

In the PNL studies which are described in the following section of this report, a mechanistic approach was used, as

outlined in Section 1.2. Physically possible initiating events were identified, and the resulting sequences of events were analyzed. Conservative assumptions were made in areas of unknown technology, and sensitivity studies were performed to assess the possible effects of uncertainties on the results. The accident established is considered more severe than any "credible" accident. It represents an upper bound for such accidents, with allowance for uncertainties in design parameters and physical phenomena.

4. PHASE I - INITIATING MECHANISMS

The definition of the postulated accidents is based upon analyses of various failure sequences and determining, with conservative assumptions, those sequences which yield the maximum work energy. A preliminary Fault Tree was developed, and this logic tool was used in evaluating the sequences which appear to be the most severe for the more detailed analysis. The analysis, however, is dependent upon failure thresholds and the modes of failure. These also are outlined below.

4.1 Fault Tree

The FFTF Fault Tree⁽¹²⁾ shows that subsequent core collapse and disassembly can be initiated by accidents leading to any one of the following:

- Large, rapid reactivity insertions.
- Slow reactivity insertions with a simultaneous failure of the protective system.
- Gross loss of sodium flow.

The large rapid reactivity insertion can occur either during power operation or during shutdown conditions assuming a pessimistic series of equipment failures and operator errors.

4.1.1 Operating Failure Modes

Three specific accident sequences are described with preliminary evaluation of the reactivity ramp rate associated with each. These accidents are:

- (a) Single control rod ejection.
- (b) Single subassembly collapse without protective action.
- (c) Gross coolant flow loss without protective action.

4.1.2 Shutdown Failure Modes

During shutdown, fuel handling operations will be performed with potentially large changes in the core reactivity. If failures and errors are compounded to the extent that the reactor is near a critical condition during insertion of a fuel subassembly, then a potentially hazardous situation exists.

Two specific accidents are identified which could follow such conditions:

- (a) A dropped fuel subassembly.
- (b) Gradual reactivity insertion beyond criticality by fuel addition or control rod removal.

4.1.3 Relationships to Safety Philosophy

The FFTF design safety philosophy calls for limiting the possible consequences of an accident to a degree consistent with the likelihood of the initiating circumstances and the multiplicity of coincident faults which must exist in order to initiate or permit the progression of the accident. Specific guides to the design are:⁽¹³⁾

- (a) The consequences of anticipated faults are limited to the level of an operational incident.
- (b) The consequences of a single unlikely fault are limited to the level of a minor accident.
- (c) The consequences of a single unlikely fault, together with an independent failure of a backup or protective device, are limited to the level of a major accident.
- (d) The consequences of two simultaneous and independent unlikely faults, or of a single such fault, together with failure of two backup or protective devices, will be limited to the level of a disruptive accident, less severe than the Design Basis Accident.

One objective of the Fault Tree has been to show that accidents which could lead to core disassembly require multiplicity of failures at least to the level of item (d). Consistent objectives are being applied in the development of design safety criteria for the FFTF.

4.2 Reactivity Consequences of Potential Failure Modes

Reactivity consequences have been analyzed for potential failure modes occurring during reactor power operations and shutdown operations. The dependency of the reactor response to initial thermal conditions along with the variable conditions of engineered safeguards emphasize the need for this distinction between operations.

4.2.1 Operating Failures

- (a) Single control rod ejection - The maximum allowable worth of any single control rod in the reference design is 3-4\$. This value is specified to limit the potential reactivity ramp rate in case of a highly improbable rod expulsion accident.

The following sequence of unlikely events is postulated to initiate the expulsion of a 4\$ control rod:

- (1) The scram release mechanism fails to latch on the full-in control rod and the operator and interlocks both fail to detect the failure.
- (2) The operator withdraws the extension rod to its full-out position and fails to notice:

An absence of reactivity response to the withdrawal.

Continued indication of rod full-in by the appropriate indicator.

- (3) Coolant blockage occurs at the control channel inlet and vaporization initiates just below the control rod after reaching 204 °C (400 °F) superheat.
- (4) The sodium pressure generated below the control rod provides a net upward force which, for an assumed weight, accelerates the rod at a rate of 3-1/2 g's.

A sodium vapor pressure of about 80 psi corresponds to 400 °F of superheat. The acceleration of the rod is shown in Figure 2 as a function of the magnitude of sodium superheat prior to flashing.

An acceleration of the control rod at 3-1/2 g's results in a peak reactivity ramp rate of 50\$/sec (the average ramp rate for 80% of the total rod worth is approximately 30\$/sec). An acceleration of more than 15 g's is required to cause a peak reactivity insertion rate of 100\$/second.

- (b) Single subassembly collapse without protective action - The model chosen to evaluate the reactivity attributed to the relocation of fuel within a single subassembly assumes collapse of the top 1/3 of the fueled assembly into the central 1/3. With a core subdivided into approximately 70 driver subassemblies, the reactivity increase would approach 1\$ and the ramp rate during collapse would be less than 4\$/second. With such a relatively low ramp rate, but only without protective action, the reactivity addition will affect the entire core and eventually result in core disassembly. The feedback effects are of predominant importance in an accident of this type.

FIGURE 2. Rod Expulsion Acceleration Rate

- (c) Gross coolant flow loss without protective action - Gross coolant loss without protective action can conceivably result in a power excursion to disassembly by the reactivity effects of sodium voiding and core collapse. The reactivity insertion rates are totally dependent upon these feedback effects.

4.2.2 Shutdown Failures

- (a) Fuel charging after criticality - If fuel charging operations continue after criticality is achieved and if all the scram systems fail to operate, the power excursion will begin with a ramp reactivity insertion dependent upon the charge velocity. The reactivity insertion rate is not particularly important since a major nuclear excursion will be initiated by feedback phenomena.
- (b) Fuel drop-in accident - If a fuel subassembly is dropped into the core when the core is inadvertently just barely subcritical, a power excursion will begin with a high initial ramp rate. Figure 3 shows the peak ramp reactivity insertion rates as a function of the drop distance to the core, where the peak ramp rate occurs as the fuel subassembly falls past the core center where the fuel worth is highest. The ramp rates shown in the figure assume a free fall unconstrained by fluid friction. For a free-drop in sodium from four feet above a sodium level at about 30 ft from the core, the velocity would be about 40 ft/sec and the corresponding peak reactivity insertion rate about 75\$/second. Lesser rates would result from dropping into a restricted sodium-filled space.

FIGURE 3. Fuel Drop-In Reactivity Insertion Rate

5. PHASE II - KINETICS ANALYSIS

The foregoing discussion has been concerned with the type of accidents to be considered in the preliminary maximum accident analysis of the FTR. The purpose of the present section is to analyze the consequences by considering, in some detail, the reactor response to the initiating mechanisms defined. Included is a determination of the degree to which reactivity feedback effects can influence the reactor prior to reaching disassembly conditions.

In attempting to follow the transient behavior of the reactor subsequent to such initiating mechanisms, the major phenomena which directly influence the net reactivity are:

- The Doppler coefficient
- Fuel axial compaction
- Sodium voiding
- Fuel dispersal effects

Thermal expansion of core components, particularly fuel, may have a significant negative feedback effect which would generally tend to reduce the accident severity. Under the conditions postulated, however, the reliability of thermal expansion is uncertain, and it is omitted from these analyses.

If the accident postulated is a reactivity accident (caused by moving fuel or control material), the only prompt reliable mechanism which provides a negative reactivity feedback is the Doppler effect. If this negative feedback is strong enough, either caused by a large Doppler coefficient or because of a large fuel temperature differential between the initial condition and the failure point, the accident might be automatically arrested with little or no damage occurring. Even with a rather weak Doppler effect, if the reactivity accident postulated has a low enough bound in total worth, similarly

minor consequences would transpire. Nearly all of the credible accidents leading to a power transient would fall in either of these categories, and for such accidents, the reactor is intrinsically safe.

It is possible, however, that for the maximum reactivity accidents postulated, the Doppler effect would not be strong enough to halt the power increase. Since total safety system failure is assumed to accompany such accidents (for the purpose of estimating the maximum damage resulting), two more major reactivity feedback phenomena could be effective - fuel compaction and sodium voiding. Certainly the core will begin to melt. What happens to the fuel under such conditions is not known, but it is possible that the fuel could compact and cause a positive reactivity feedback which would further aggravate the accident. Also, if the sodium is still in the core, molten fuel could come into contact with this sodium and produce violent sodium flashing. Here again, the events following such phenomena are not known. To take a pessimistic approach, one might envision these large sodium vapor pressures driving out the liquid sodium and causing a further reactivity feedback due to the sodium void effect. Unfortunately, under the conditions postulated, this could be a positive effect since the sodium void coefficient is usually positive in the hottest (central) regions of the core.

For slower power transients, the sodium may begin to boil prior to the point of gross fuel melting and collapse. Sodium superheat would tend to prolong the timing of such an occurrence, but this same superheat condition would probably result in much faster voiding patterns once flashing begins. The reactivity consequences of sodium voiding can be either positive or negative, again depending upon the region of the core in which this process occurs. For flow coastdown accidents,

the sodium would be hottest near the core outlet region (at the top of the core) and voiding in this vicinity would cause a negative reactivity effect. A power transient or gross flow blockage accident, however, could initiate voiding near the axial center of the reactor and this voiding would then produce large positive reactivity effects.

Finally, once the fuel temperatures rise above the melting point, fuel pressures become significant in moving molten fuel. This movement, the beginning of the disassembly process, can produce local positive effects for certain power, temperature, and worth distributions, but usually the effect is negative - at least macroscopically. Fuel need not move very far before the negative reactivity thus produced is sufficient to override all other reactivity components and the nuclear excursion is terminated.

Clearly, the possible interactions of the above effects are numerous and involved. The energy release is known to be quite sensitive to the effective reactivity ramp rate at the time disassembly starts,^(14,15) but it is also dependent upon the power level, precursor density, temperature and power distributions, and net reactivity. Furthermore, the presence or absence of sodium at this time directly influences the energy release because of the changes in the Doppler coefficient and effective equation of state which occur. To account for the possible permutations and combinations, even in a parametric way, is no small task.

As an approach toward this goal, the problem has been broken up into two parts, the first being a parametric analysis of the worst identified combinations of events prior to disassembly, and, second, the disassembly process itself - wherein

parametric studies are performed separately. This is not a completely satisfactory procedure and, in some cases, a direct merger of the two phases was performed as was done in an earlier report.⁽¹⁶⁾

5.1 Meltdown Model

Because of the possible interaction of the various reactivity mechanisms discussed above, a computer code capable of dynamically including such effects has been developed. Within the limitation of the models chosen, the instantaneous inclusion of fuel compaction and sodium voiding effects into the kinetics equation enables such feedbacks to directly influence further compaction and voiding patterns. The effect of this type of coupling is almost impossible to estimate from static calculations alone.

5.1.1 Description

The computer code developed for the transient analysis up to the time of disassembly is currently designated as MELT-I. The model, as sketched in Figure 4, allows the core to be divided into a maximum of 10 radial channels (rings) and 40 axial segments. Within each channel, temperature and power density are calculated for one fuel pin which is considered to be characteristic of all fuel pins within the channel. The fuel region within the pin may be divided into a maximum of eight radial nodes, and the cladding and coolant are each assigned a single node. After the fuel melting temperature is reached, the fuel segment temperature remains fixed at its melting temperature until sufficient energy has been generated to provide the latent heat of fusion. The Doppler feedback during the transient is determined from a volume weighted core temperature.

FIGURE 4. Meltdown Model

For the meltdown and fuel compaction analysis, it has been assumed throughout that gross fuel movement cannot occur until cladding failure occurs. Presently, the code assumes cladding failure whenever any one of the following three mechanisms occurs:

- (a) The cladding reaches its melting point.
- (b) Fuel in the node adjacent to the cladding melts, (and, hence, comes into direct contact with the cladding).
- (c) Pressure in the center of the fuel pin, either caused by fission gases or fuel vapor pressure, becomes large enough to rupture the cladding.

Once the cladding has failed, all fuel at an elevation above that point is allowed to slump under the force of gravity (a viscous term is also contained in the equation of motion, although this has been found to be a second order effect). Below the failure point, however, local cladding failure must occur before the fuel is allowed to slump. Whenever fuel motion takes place, the reactivity feedback caused by collapse is determined by the total displacement along a fuel reactivity worth curve. Since the worth curves (input) are assumed to be stationary in space throughout the transient, the model is no longer valid after a sufficiently large change in the core configuration develops. Early results indicate, however, that fuel temperatures characteristic of an explosive core are generated before sizeable fuel movement has occurred.

For the analysis of sodium voiding effects, a simple piston model has been used. Once boiling begins at a particular point of a core channel, the vapor-liquid interface is assumed to propagate in both axial directions from that point according to a predetermined equation of motion. Application

of a read-in sodium worth curve then allows a straight-forward perturbation calculation of the reactivity feedback due to voiding. The location of the initial boiling can be determined by the local coolant temperature or by local failure of the cladding.

The present fitting forms of the equations of motion for sodium void propagation are as follows:

$$X_{up} = X_0 + A_1 + A_2(t-t_{A2}) + A_3(t-t_{A3})^2 + A_4(t-t_{A4})^3$$

$$X_{down} = X_0 - B_1 - B_2(t-t_{B2}) - B_3(t-t_{B3})^2 - B_4(t-t_{B4})^3$$

where X_0 is the axial location of initial voiding. The constants are input parameters selected to approximate the type of voiding scheme envisioned. As in the fuel compaction model, the material movement in one channel is affected by movements in other channels in that the local time base and position X_0 can be influenced by the overall progression of the reactor transient.

To obtain accurately the 4000 nodal temperature and power densities for this model by doing a complete heat balance at each node would be a major programming effort and, furthermore, would probably require large amounts of computer time. Hence, the method used in MELT-I is to compute the nodal temperature accurately at only the average point in the reactor by the K-III⁽¹⁷⁾ method and then obtain the remaining spatial distributions by a simple projection scheme. Spatial power and temperature variations (determined from static calculations) are assumed to remain fixed in time during the transient.

Because of this simplified method for computing core temperatures, several comparison runs were made with the NUTIGER coupled heat transfer kinetics code (an adaptation of the TIGER series of codes⁽¹⁸⁾). NUTIGER is capable of calculating transient temperatures in a single fuel pin quite accurately; however, it cannot continue the calculation beyond the point of core geometric change and, even more importantly, is limited to a treatment of about 400 nodes. Hence, it is not suited for the meltdown calculations desired, but it is ideal for assessing the accuracy of the MELT-I code up to the time of melting.

The referenced study⁽¹⁶⁾ was performed for the Nuclear Proof Test Facility, which is neutronically equivalent to a low power (shutdown) version of the FTR. For the axial cosine power and temperature distributions which exist for such a condition, the MELT-I code is probably adequate. Similar comparisons for the full power case have not been carried out to the same extent. A brief comparison indicated reasonable predictions of transient fuel temperatures, at least good enough for the first iteration in estimating the reactivity effects of meltdown. The prediction of coolant temperatures, however, is not adequate. Hence, the treatment of sodium voiding effects in the loss of coolant accident has been done by a bracketing procedure as outlined in Section 5.2.2.

An important item to consider in developing a meltdown analysis is the decision of when to transfer to a disassembly code. The current technique is to continue the melting program until core temperatures are sufficiently high to produce a significant amount of vapor pressure (such that a negative reactivity component due to disassembly should be included). By iteratively starting the disassembly code at various times after melting starts, the net reactivity transition can be made quite smooth. Although in this report the merger of the

meltdown phase with the disassembly phase is not usually done, it is still important to know when the core is nearing a disassembly condition in order to determine a reasonable termination point for the meltdown phase.

5.1.2 Correlation with Other Models

Perhaps it is worthwhile to comment briefly on the validity of the kinetics model chosen. Certainly the best test of any model is good experimental data. There exists, however, very little data which has direct bearing on the type of accidents under discussion. The onset of fuel melting and sodium boiling can and has been studied,⁽¹⁹⁾ and more elaborate experiments are planned,^(20,21) but normally the number of parameters which exist under actual accident conditions greatly exceed those which have been handled in experiments to date. Eventually, by integrating the results of many good fundamental experimental studies, more realistic analytical models can be developed. For the present, however, about the best one can do is to strive for realism and simply choose the conservative path whenever a significant unknown exists.

The approaches initiated in previous fast reactor studies are outlined in Appendix A. A few comments, however, may presently be appropriate regarding the methods used to establish the input condition for the disassembly calculations - especially for FARET and SEFOR (since they more closely parallel our present approach).

The EBR-II and FERMI analyses used a threshold reactivity model, i.e., some mechanism was envisioned to produce a subcritical reactivity and then secondary criticality was reached by gross core compaction. No analysis was attempted to define how such initial conditions could be reached.

SEFOR studies utilized the output of a 2-dimensional coupled neutronics-heat transfer code (FORE)⁽²²⁾ to determine the time at which fuel collapse could start in various radial channels. Static calculations were then performed to determine the ramp rates achievable due to collapsing cylindrical shells of fuel. These ramp rates served as input to the disassembly calculations.

The analysis most similar to our present approach is probably that performed for FARET.⁽²³⁾ The meltdown code developed there calculated the temperature distribution in a single pin quite accurately. An axial division of 40 segments was used and fuel was allowed to compact under gravity. The compaction model, however, assumed the collapsed fuel to be redistributed uniformly over the entire reactor, rather than to concentrate near the point of collapse as assumed in the present studies. Further, no radial projection scheme was used and so only cores with a flat radial temperature distribution could be studied.

Because of the continuous reactivity model employed in the FARET analysis, a decision had to be made as to when to stop the meltdown phase and begin the disassembly phase. The approach used was to wait until the total energy input reached a value roughly equal to the energy required to vaporize the fuel. For the present studies, disassembly pressures have been noticed for total energy depositions of roughly one half this value, and the transfer to the disassembly phase is accordingly started earlier.

Sodium voiding models, substantially more elaborate than the one herein selected, have been developed and reported in the literature.^(24,25,26,27) However, there is wide disagreement as to the effects of superheat, the propagation of voiding,

and little is known about molten fuel-sodium interactions. Hence, the voiding model presently used, simple as it may be, is considered adequate for this stage of the studies. The feature of direct dynamic coupling of sodium voiding effects to the reactivity balance is particularly useful.

5.1.3 Reactor Constants Used in the Kinetics Analysis

Listed in this section are the pertinent constants which were used in the kinetics analysis prior to disassembly.

Table IV contains the core constants used in the calculations and Table V lists the relevant fuel pin parameters.

Reduction from three to two dimensional geometry is somewhat uncertain in the FTR reference core because of the split core design. Hence, the worth and power curves for the radial direction, obtained by weighting techniques, are approximations only. Volumes for the test zone and the inner and outer fuel zones do not correspond to actual values due to the problems in going to 2D geometry. The relatively cold test zone, for example, was greatly reduced in volume in order to achieve a reasonable power and worth profile in the radial direction. Axial power and worth curves are believed to be more accurate - the axial power following the normal chopped cosine distribution. The temperature distribution, assumed to follow the power distribution, is known to be a poor approximation for coolant temperatures, but has been found to be reasonable for fuel temperatures. As noted earlier, coolant temperatures are never used, per se, and, as will be shown in Section 5.2.2, the loss of coolant accident has been studied using a bracketing procedure. As can be seen from the power and worth curves used, namely, Figures 5, 6, and 7, the core was divided into 10 radial channels and 18 axial segments for this study (a sensitivity study was reported in Ref. (16) in which it was concluded that an axial division of 18 segments was adequate for these studies).

TABLE IV. Core Parameters Used in the Meltdown Phase

Core Radius	= 66 cm
Core Height	= 85 cm
Core Volume	= 1163 liters
Central Test Zone	= 11 liters
Inner Fuel Zone	= 630 liters
Outer Fuel Zone	= 522 liters
Total ^{239}Pu Inventory	= 600 kg
Neutron Lifetime	= 3.5×10^{-7} sec
Initial Average Power	<div> <div>Shutdown</div> <div>Full Power</div> </div> <div> <div>= 0.004375 W/cm³ fuel (~2.5 kW total)</div> <div>= 1300 W/cm³ fuel (400 MW total)</div> </div>
Initial Average Fuel Temperature	<div> <div>Shutdown</div> <div>Full Power</div> </div> <div> <div>= 293 °K^(a)</div> <div>= 1350 °K</div> </div>
Radial Peak-to-Average Power	= 1.15
Axial Peak-to-Average Power	= 1.25

(a) A few shutdown cases starting from a more realistic refueling condition of 500 °K were performed. No major differences were found.

TABLE V. Fuel Pin Parameters Used in the Meltdown Phase

	<u>Fuel</u>	<u>Cladding</u>	<u>Na</u>
Radius (cm)	0.27	0.32	0.37
ρC_p (J/cm ³ -°K)	3.93	4.18	1.30
k (J/cm-sec-°K)	0.021	0.208	
h (J/cm ² -sec-°K)	0.85	17.0	
ρ (g/cm ³)	10.5	2.00	0.90
T _{melt} (°K)	3000	1623	371
T _{boil} (°K)			1153
Heat of Fusion (J/g)	280	110	115

FIGURE 5. Radial Fuel Worth Curve Employed in the Meltdown Calculations

FIGURE 6. Axial Fuel Worth Curve Employed in the Meltdown Calculations

FIGURE 7. *Relative Radial Power and Temperature Distributions Assumed for the Meltdown Calculations*

The sodium void worth curves, as shown in Figure 8, were estimated by a 2D perturbation theory code⁽²⁸⁾ and are, therefore, quite approximate. There simply does not exist at the present time an accurate way of obtaining absolute worths, although the spatial variation of the curves, as presented, are believed to be reasonable. The values used, which include a maximum possible void reactivity of $\sim 2\%$, and a maximum void worth for a single subassembly of $\sim 5\%$, are believed to be conservative, i.e., more positive than will actually exist in the reactor.

5.2 Analysis of Operational Accidents

There are basically two types of accidents which could occur during full power operations. These include power transients, due to reactivity changes, and loss of flow accidents.

The purpose of the present section is to investigate the consequences of the major accidents and attempt to determine whether core compaction and sodium voiding, either separately or in combination, could aggravate the postulated accidents prior to disassembly.

5.2.1 Reactivity Insertion

The most severe reactivity accident arising during full power conditions was identified as a control rod ejection. For a maximum control rod reactivity worth of 4% and an assumed ejection rate of $3\frac{1}{2}$ g's, the maximum ramp rate was calculated to be $50\%/second$. Although this is believed to be an upper limit for initiating ramp rates which could be achieved at full power, a range of ramp rates from $4\%/sec$ to $100\%/sec$ was studied.

The analysis of these power transients was done in two ways. First, the core was assumed to be free of sodium (or at least the consequences of sodium flashing were not considered) and, second, the core was assumed to contain its normal sodium inventory.

FIGURE 8. Sodium Void Worth Curves Employed in Sodium Void Calculations

If the presence of any sodium is disregarded, fuel compaction is the only mechanism which has been identified which could aggravate the accident via a positive reactivity feedback. On the other hand, the collapse model assumes that once cladding failure occurs, molten fuel is free to spew out into the coolant channel. Hence, if sodium is present, it is possible that an intimate mixture of molten fuel and sodium could flash the liquid sodium and produce high local pressures. Certainly, the degree of turbulence would depend on fuel particle size and the rate of mixing, but, at the present, the possibility of local sodium voiding due to such a phenomenon cannot be ruled out. According to the arguments presented in Appendix D, pressures of several thousand psi could occur, and if one does a simple force balance for the amount of sodium in the FTR coolant tubes, sodium acceleration rates of the order of 1000 g's might be achieved. The most disturbing elements of such a phenomenon are:

- (a) Such voiding would initially occur near the hottest region of the core where the local sodium void coefficient has its maximum positive value, and
- (b) Such voiding would occur when the reactor is near prompt criticality so that even a small positive reactivity feedback could be quite influential.

Two illustrations will help to clarify the types of interactions involved. Figure 9 represents the transient which ensues when a 100\$/sec ramp rate is imposed on the FTR core operating at 400 MW (for this case, the Doppler coefficient is assumed to be -0.003). In this example, no molten fuel-sodium interaction is assumed; i.e., the only positive feedback mechanism is that due to fuel collapse. Prior to the times shown on the graph, the reactor becomes prompt critical and the subsequent rise in fuel temperature causes Doppler

FIGURE 9. Reactivity Components of Full Power FTR with a 100\$/sec Initiating Ramp Rate and No Sodium Voiding

feedback which approximately compensates the external input so that the net reactivity characteristically fluctuates around one dollar (prompt critical). At slightly under 15 msec after the accident is initiated, cladding in the hottest region of the reactor fails and collapse begins. For such a large external ramp rate, however, the accident proceeds so fast that fuel compaction is rather unimportant; i.e., the time scale is simply too short to allow significant compaction prior to the time the core is in a disassembly condition. Within about 2 msec after collapse begins, the core average temperature has risen to the point where some fuel vapor pressures, in the hot region of the core, become significant and a negative disassembly feedback term should be included. The ramp rate due to collapse at this point in time is only about 2\$/sec so that the total ramp rate at the time of disassembly has risen from 100\$/sec to only 102\$/sec.

Figure 10 illustrates the melting pattern which occurs for this case. This is an R-Z cross-section of the core (where the conical effect is not considered) and the region within the heavy black lines represents the portion of the core melted (actually, it represents the boundary corresponding to outer fuel node temperatures equal to or exceeding 3000 °K). Times given represent the time lapse after collapse begins. The two hot spots on either side of the core center correspond to the power peak of Figure 7. As pointed out above, however, disassembly effects become appreciable after about 2 msec, so the patterns shown beyond that time are not too meaningful.

Figure 11 is a transient representation of the same case as illustrated in Figure 9, except that in this instance, violent sodium voiding is assumed to accompany fuel collapse at the point of cladding failure; i.e., whenever in any one of the 10 radial channels the clad fails (and collapse starts),

External Ramp Rate = 100 \$/sec
 Doppler = -0.003
 (Fuel Compaction Only)

FIGURE 10. Melting Pattern in Full Power FTR
 with a 100\$/sec Initiating Ramp
 Rate and No Sodium Voiding

FIGURE 11. Reactivity Components of Full Power FTR with a 100\$/sec Initiating Ramp Rate and 1000 g Sodium Voiding

the voiding routine is also started. For this case, the vapor-liquid interface was assumed to propagate above and below the point of cladding failure at the rate of 1000 g's. As can be seen from Figure 11, the positive sodium void feedback is quite substantial, and even though the total void feedback is only about 12 cents at the time of disassembly, the void ramp rate is about 150\$/sec so that the total ramp rate at the time of disassembly has risen to approximately 250\$/sec. Figure 12 represents the case where the inner propagation pattern is that of sodium voiding. The outer pattern represents the portion of the core molten, as in Figure 10. However, the influence of the positive sodium void feedback has caused a substantially accelerated melting pattern, as can be seen by comparing the melting propagation between Figures 10 and 12. As in the previous case, however, disassembly effects should be included beyond approximately 2 msec so that the patterns beyond that point are not really descriptive.

For this latter case, in which a sodium ejection rate of 1000 g's was assumed, it should be emphasized that the time between the beginning of voiding and the start of core disassembly is only a few milliseconds. In most of the cases considered, the disassembly process itself is over within approximately 5 msec. But in order to get the 1000 g voiding rates, the molten fuel-sodium interaction was assumed to produce "equilibrium" sodium pressures instantaneously. Although this is certainly conservative from the standpoint of achieving maximum voiding patterns, any transient delay during the mixing process could sizably lower the sodium vapor pressure and, correspondingly, the voiding rate. A transient mixing time of even a few msec could, therefore, be significant enough to reduce drastically the reactivity ramp rate due to voiding at the time of disassembly.

External Ramp Rate = 100 \$/sec
 Doppler = -0.003

FIGURE 12. Voiding and Melting Patterns in Full Power FTR with a 100\$/sec Initiating Ramp Rate and 1000 g Sodium Voiding

Actually, both cases described above are extreme cases in that they were assumed to be caused by an external unbounded ramp rate of 100\$/sec. The maximum control rod ejection ramp rate was calculated to be 50\$/sec and much lower ramp rates are probably all that are really credible. Hence, initial ramp rates of 4 and 40\$/sec were analyzed by the same procedure as outlined above. If sodium is not in the core, or if the molten fuel-sodium interaction is not significant, the results are those shown in Table VI. Here it is seen that for low initiating ramp rates the collapse effect becomes important; i.e., an initiating ramp rate of 4\$/sec is augmented to around 18\$/sec due to fuel compaction. As the initiating ramp rate increases, less time is available for core collapse prior to reactor disassembly conditions and collapse effects become less important.

*TABLE VI. Disassembly Ramp Rates for Sodium-Out
Full Power FTR*

	<u>Reactivity Rate, \$/sec</u>		
Initiating Ramp Rate, \$/sec	4	40	100
Feedback Ramp Rate due to Core Collapse, \$/sec	14	2	<2
Total Ramp Rate, \$/sec	18	42	102

Doppler coefficient = -0.003

An item that has not been explicitly discussed as yet is the magnitude of the Doppler coefficient. As shown in Ref. (16), this parameter can significantly affect the melt-down phase, as well as the disassembly phase. If positive reactivity feedback effects are considered during the course of the accident, a large negative Doppler coefficient actually enhances the effects of such mechanisms, since it holds the power and temperature down longer and provides more time for

these positive feedback mechanisms to manifest themselves. Parametric studies of the Doppler effect are not included in the meltdown phase of this study, although the magnitude of the coefficients selected are considered reasonable.

Table VII contains the results of inserting ramp rates of 2\$/sec up to 100\$/sec into a full power FTR core where, in this case, the effects of a violent molten fuel-sodium interaction are included. All cases in this table assume a 1000 g void pattern to begin at the point of cladding failure. For low ramp rates, the compaction effect becomes more pronounced than for the high ramp rates, but in all cases the extremely fast sodium void pattern provides the dominant reactivity feedback effect.

TABLE VII. *Disassembly Ramp Rates for Sodium-In Full Power FTR*

	<u>Reactivity Rate, \$/sec</u>			
Initial Ramp Rate	4	8	40	100
Feedback due to Collapse	4	4	2	2
Feedback due to Na Voiding	235	236	153	148
Total Ramp Rate	243	248	195	250

Doppler Coefficient = -0.003

As discussed above, the molten fuel-sodium interaction is conceivably severe. However, even if large local pressures could rapidly be generated from very efficient mixtures, the sustaining of such pressure, the neglect of viscous effects, etc., is perhaps such that 1000 g voiding rates are far too conservative. Figure 13 represents a parametric study wherein the voiding rate is varied from 0 g's up to 1000 g's and, in one case, up to 1500 g's. All cases shown were done for a Doppler coefficient of -0.003. The magnitude of the voiding

FIGURE 13. Disassembly Ramp Rate as a Function of Sodium Voiding Rate

feedback would be expected to increase for high (larger negative) Doppler coefficients and be diminished for lower Doppler coefficients. For the three initiating ramp rates chosen, fuel compaction is included, even though such effects are small for voiding rates in excess of about 10 g's. Possibly the most significant feature of the curve is that low initiating ramp rates can lead to very large ramp rates at the start of disassembly due to the increased time available for sodium ejection to take place. In fact, for the particular cases shown, the 4\$/sec initiating ramp rate leads to larger disassembly ramp rates than the 40\$/sec case.

5.2.2 Loss of Coolant

Since core coolant temperatures are highest near the top of the core during normal operating conditions, it is suspected that sodium voiding during a loss-of-coolant accident would usually begin in this vicinity. The reactivity consequences of such voiding are expected to be small due to the negative sodium void coefficient near the core periphery. It is conceivable, however, for a case of complete flow blockage, that voiding could originate from a point near the axial center of the core. The consequences of such an occurrence have been analyzed briefly by a bracketing procedure. As pointed out in the description of the MELT-I code, accurate coolant temperatures cannot presently be calculated for flowing sodium systems. Hence, no attempt has yet been made to start channel voiding from axial locations which might occur under actual accident conditions. Rather, the worst conceivable voiding patterns have been studied to see how bad a most severe loss of flow accident might be.

The rate of sodium ejection for loss of flow accidents depends very much on the degree of liquid superheat - quite a different phenomenon from that of a molten fuel-sodium

interaction. Little information currently exists on this topic, especially for flowing sodium situations. This study utilized the recent German work of Pepler, et al.,⁽²⁹⁾ mainly because of the simplicity of the piston model therein assumed. In Figure 14, which contains the ejection patterns used in the present exercise, Curves 1 and 2 are taken directly from the German work.⁽²⁹⁾ Curve 1 corresponds to a low* liquid superheat condition coupled with a fairly high temperature differential between fuel and sodium. Curve 2 represents a high* superheat condition where, as suggested in Ref. (29), the fuel-sodium temperature differential is unimportant. Curves 3 and 4 represent simple symmetric 20 g and 10 g acceleration rates, respectively. For a 220 °C superheat condition and a 20-ft column of sodium, the 10 g acceleration curve should provide an upper limit for loss of flow ejection patterns in the FTR core.

An extreme flow blockage case is presented in Figure 15 where voiding has been assumed to start at the axial center of the inner seven radial channels - those channels for which the estimated sodium void coefficient is positive at the core midplane. For the case illustrated, Curve 1 of Figure 14 was used to describe the fluid equation of motion. As noted from Figure 15, the positive feedback due to sodium voiding is sufficient to cause core melting and collapse. The total ramp rate at the time the core enters disassembly condition (fuel vapor pressures become significant) is about 55\$/sec, of which about 53\$/sec is the result of voiding and 2\$/sec is the result of fuel collapse.

* "Low" and "high" correspond to 20 °C at 220 °C superheat, respectively, for the German core. Because of the differences between the height of their sodium column and that of the FTR, however, a 220 °C superheat condition in the FTR corresponds to an acceleration rate of about 10 g, i.e., about one half the German value.

FIGURE 14. Sodium Ejection Curves Used in Loss-of-Coolant Study

FIGURE 15. *Loss-of-Coolant Transient with Low Superheat Before Voiding*

Figure 16 contains the reactivity feedback due to voiding for five other cases. Curve 1 duplicates that of Figure 15 and is included as a point of reference. Curves 2, 3 and 4 represent the high superheat condition, 20 g and 10 g voiding rates, respectively, and Curves 5 and 6 represent cases where the voiding rates were the same as in Curves 1 and 2, but voiding was assumed to start simultaneously from all channels in the core (including those peripheral channels where the sodium void coefficient is negative throughout). The ramp rate near disassembly reached nearly 100\$/sec for Cases 2 and 3, and about 65\$/sec for Case 4, whereas in the last two cases, the negative voiding feedback overwhelmed the positive contribution before significant core melting began and, thus, the nuclear transient was terminated.

Based on these studies, it would appear that ramp rates of the order of 50 to 70\$/sec might be obtained as a result of sodium voiding during a severe loss of flow accident. Compounding the pessimistic assumptions of high liquid superheat with the most extreme voiding patterns, ramp rates of the order of 100\$/sec have been calculated. The conditions necessary for such transients to occur, however, are so remote that the latter values are suspected to provide nothing more than a most conservative upper limit.

5.3 Analysis of Shutdown Accidents

Two major accidents pertinent to the shutdown mode have been identified - fuel drop and reactivity addition after criticality. A total loss of coolant would eventually result in core melting (primarily caused by gamma heating, but also because of alpha decay and spontaneous fission of plutonium) and possible compaction, but the time scale involved for such

FIGURE 16. Reactivity Transients for Loss-of-Coolant Conditions

an occurrence would greatly exceed that normally considered for loss of flow accidents. Furthermore, if such an event did occur, the consequences would certainly not be more severe than those considered in the foregoing Section 5.2.2.

5.3.1 Fuel Drop

A maximum ramp rate of $<75\$/\text{sec}$ has been estimated for a case where the most reactive fuel rod (4\$) is dropped into the core; however, the rate is dependent upon the height of the drop and the resistance to free fall. The more credible situation (if any fuel drop is credible, in the sense that the reactor could be critical at the time such an element is dropped) would result in lower ramp rates. Hence, a range of initiating ramp rates from $1\$/\text{sec}$ to $100\$/\text{sec}$ has been investigated.

As in the case of the power transient considered for the full power core, the shutdown transients have been analyzed in two ways--sodium-in and sodium-out. If any molten fuel-sodium interaction is disregarded, the accident analysis follows much the same pattern as in Ref. (16). Figure 17 contains the results of this study and demonstrate that collapse effects are significant for low initiating ramp rates, but are almost negligible for initiating ramp rates exceeding about $50\$/\text{sec}$ because of the short time lapse to disassembly conditions.

The sodium-in analysis was divided into two ramp rate domains. For initiating ramp rates in excess of about $4\$/\text{sec}$, the cladding fails because of molten fuel impingement and the molten fuel-sodium interaction could, along with fuel compaction, seriously aggravate the accident. For slower ramp rates, however, calculations indicate that for conditions of zero coolant flow, sodium could boil and void the channel prior to clad failure.

Two cases below $4\$/\text{sec}$, namely $1\$/\text{sec}$ and $2\$/\text{sec}$, were run where coolant voiding did occur prior to cladding failure.

FIGURE 17. Disassembly Ramp Rates for Sodium-Out Shutdown FTR

Void propagation rates of 10 g's (representative of 220 °C superheat conditions) were then assumed to occur in the channels affected. The reactivity consequences of this voiding quickly raised the power level and cladding failure and subsequent collapse then occurred. Total ramp rates, however, are still well below 50\$/sec for these cases. One might ask whether the onset of clad failure could then cause a molten fuel-sodium interaction of the type considered earlier. For the cases considered, however, the sodium in the vicinity of cladding failure was already voided so as to preclude such a reaction. Patterns may exist, however, wherein original sodium voiding due to normal flashing could be followed by cladding failure which, in turn, could be followed by a molten fuel-sodium interaction. Such patterns have not as yet been investigated.

For higher initiating ramp rates, the large disassembly ramp rates shown in Table VIII are primarily caused by the molten fuel-sodium interaction (1000 g sodium acceleration rates were assumed).

TABLE VIII. *Disassembly Ramp Rates for Sodium-In Shutdown FTR*

	<u>Reactivity Rate, \$/sec</u>				
Initiating Ramp Rate	1	2	4	40	100
Feedback caused by Collapse	7	8	4	3	2
Feedback caused by Sodium					
Voiding	24	17	240	277	203
Total Ramp Rate	32	27	248	320	305

Doppler Coefficient = -0.004

5.3.2 Fuel Charging After Criticality

A continual mechanical insertion of fuel into a critical core could eventually lead to core disassembly. For ramp rates less than approximately 4\$/sec the total ramp rate near the time of disassembly was shown in the foregoing to be small,

since voiding ejection problems, caused by rather large degrees of superheat, are not too severe. Collapse effects, although quite appreciable, are not strong enough to aggravate the initiating ramp rate beyond around 30\$/sec.

5.4 Summary of Phase II

The purpose of the present section has been to assess the consequences arising from the types of accidents defined in Section 4. Although many factors influence the magnitude of the disassembly phase, probably the single most important parameter is the ramp rate. For both shutdown and full power conditions, the calculated ramp rates obtained, if the influence of sodium is disregarded, are seen to be about 52\$/sec for the control rod ejection accident, 65\$/sec for a gross flow blockage accident, and <75\$/sec for a fuel drop accident. If a very violent molten fuel-sodium interaction is considered, the ramp rates associated with the power excursion are seen to be of the order of 250\$/sec.

In considering the large initiating ramp rate cases for the sodium-out situation, the aggravating effects of core collapse are small. Therefore, it would seem reasonable to bound these most extreme cases in the disassembly phase by considering a maximum ramp rate of 100\$/sec. Such analyses can then be done completely with the MAX disassembly code.

For the sodium-in cases, however, it was shown that a molten fuel-sodium interaction could possibly give rise to large ramp rates at the time of disassembly (≈ 250 \$/sec). The analysis of these cases, as will be shown in Section 6.2, can best be handled by coupling the meltdown and disassembly codes at the time the voiding effects become pronounced. Furthermore, the utilization of a sodium-in equation of state and assumption of partial credit for sodium pressures in moving fuel will be shown to mitigate the consequences of such situations substantially.

It should be pointed out that all initiating ramp rates were assumed to be totally unbounded during Phase II. Actually, an upper bound exists to the total worth in any reactivity accident, and further work should account for this. The main justification for the procedure to date is that for all power transients considered, core collapse began when at most 3\$ to 4\$ of reactivity had been inserted. Since the peak fuel elements and control rods in the reference core have worths of this magnitude, the assumption of an unbounded ramp is realistic. For some of the shutdown accidents studied, the low starting temperature allowed a total Doppler feedback of 2\$ to 3\$ which, although not enough to arrest an accident of the types considered, still might be strong enough to curtail any accident which would actually occur.

Finally, as pointed out earlier, the worth of sodium voiding is not well defined at the present but the worth curves used in this study are believed to be quite conservative. A change in the sodium worth would have direct bearing in the loss-of-coolant accident and in those power transients in which molten fuel-sodium reactions are considered.

6. PHASE III - DISASSEMBLY ANALYSIS

6.1 Calculational Model

The calculational model chosen to obtain the energy release in the FTR postulated accidents is that described in BNWL-611.⁽¹⁶⁾ This overall method employs two computer codes, MELT-I and MAX⁽⁴⁾ to calculate the feedbacks caused by core collapse and sodium voiding, and core dispersion, respectively. A brief description of the MELT-I code is contained in Section 5.1.1. MAX is a further modification of the original Bethe and Tait⁽³⁰⁾ model employing all of the features normally included in computer codes of this type. In addition, it allows complete versatility in specifying the core conditions at the start of the disassembly calculation.

The major difference between the MAX code and others of the same generation^(1,31) is that the reactivity feedback caused by core dispersion is calculated by actually moving the mesh intervals. This allows for the local variations in fuel density which can have direct bearing on the magnitude of disassembly pressures. Very high pressures for small incremental changes in energy density can occur if the liquid fuel has expanded enough to fill the space available to the fuel. The MAX code allows these high pressure regions to expand, increasing the volume available, and thus reducing the pressures to those characteristic of a liquid and its vapor in equilibrium. This expansion will not take place, of course, if the pressure gradient is small (i.e., at the center of a uniform core where the power distribution is smooth and continuous). In a zoned core such as the FTR, however, such expansions should be considered probable since large pressure discontinuities exist. Allowing the high pressures to be relieved could cause an increase in the time required for disassembly.

A consequence of allowing fuel to move is that the resulting computer code formulation is not easily extended to two dimensions. Other investigators^(31,32) using 2-dimensional codes have shown that for homogeneous cores, a 1-dimensional (spherical) model is quite adequate. In order to check these findings in the MAX code, a 2-dimensional case supplied by APDA was reduced to one dimension and recalculated in MAX. The characteristics of the reactor considered are shown in Table IX. The 1-dimensional power and worth curves used were not characteristic of a spherical reactor. They were instead estimated from similar curves for the 2-dimensional model in much the same manner as the power and worth curves in BNWL-611. As in BNWL-611, the estimated power curve was adjusted as much as possible to duplicate the peak to average temperature distribution of the 2-dimensional model. The APDA⁽³²⁾ equation of state of the fuel material was used in the calculations and is described in Appendix B. The results of these comparison calculations are shown in Table X.

The equivalent 1-dimensional MAX calculation estimates the energy in the molten fuel to be ~6% less than the APDA MARS⁽²⁾ calculation. A better measure of the adequacy of the model, however, would be the change in the destructive work performed during an adiabatic expansion of the fuel to one atmosphere. APDA computes this destructive work by entering the curve shown in Figure B.3 (Appendix B) with the core averaged energy density. Using this method, ~640 MW-sec could do destructive work in the cylindrical calculation. MAX, using the same procedure, would estimate this value to be ~19% less. Thus, employing a spherical model for the disassembly calculation of a homogeneous core would yield energy releases ~6 to 19% lower than those which would be obtained had a more realistic cylindrical model been used.

TABLE IX. *Characteristics of APDA 2-Dimensional Case*

Core Volume	1000 l
Fuel Density	3.32 g/cm ³
Inertial Density	5.14 g/cm ³
Reactivity Ramp	50\$/sec
Neutron Lifetime	6.9×10^{-7} sec
Delayed Neutron Fraction	0.003
Central Fuel Worth	2.83×10^{-7} Δk/g of fuel
Initial Temperatures	2970 °K (molten)
Initial Power	100 MW
Doppler Coefficient	-0.002 ($T \frac{dk}{dT}$)
Specific Heat	0.42 J/g °K

TABLE X. *Comparison of 1- and 2-Dimensional Models*

	Cylindrical (2D) case	MAX 1D Equivalent case
Peak Pressure, atm	11280	786
Peak Temperature, °K	8200	7790
Energy in Molten Fuel, MW-sec (E = 0 at 2970 °K in solid phase)	5310	4980
Amount of Molten Fuel, kg	3320	3320
Average Temp. of Molten Fuel, °K	6130	5880

The peak pressure in the cylindrical calculation is significantly higher than that in the spherical calculation because the liquid has expanded to fill the voids in the former case (this occurs at ~ 7850 °K).

In Section 5.2 it was found that, for the accidents analyzed, the effective ramp rate had an upper bound of 100\$/sec. For ramps this large, it was shown that insufficient time is available for the feedback caused by core collapse to aggravate the accident appreciably. Therefore, in those cases in which the feedback caused by sodium expulsion is ignored, it is possible to compute the energy releases entirely in the MAX code. There is some question, however, as to the adequacy of this method since the power curve used in MAX is only approximate and thus the core average temperatures (from which the Doppler feedback was computed) may be in error. In order to assess the magnitude of this effect, an accident occurring in a core with a Doppler coefficient of -0.003 and an initial core average temperature of 1300 °K was calculated in the MELT-I code until noticeable pressures were generated. At this time the calculation was transferred to the MAX code. This resulted in a calculated work energy of 210 MW/sec. A similar calculation in which the accident was computed entirely in the MAX code yielded very similar results, and it was therefore concluded that MELT-I could be by-passed if no feedback terms other than the Doppler effect were treated.

The disassembly calculations require that the pressures in the core and the temperatures of the fuel be known as a function of the energy generated in the transient. These relationships provide the necessary information for calculating the reactivity feedbacks caused by core dispersion and the Doppler temperature effect. It is usually assumed that the transient proceeds so rapidly that all of the energy generated remains in the fuel and thus the required relationships are properties of the fuel material only. Such properties comprise

the equation of state of the fuel. (In some instances, other core constituents may appreciably augment the pressures due to fuel vapor. For example, a "sodium-in" equation of state was used in some analyses.)

Of the energy generated in the accident, only a fraction can do destructive work on the surroundings of the reactor. If it is assumed that the hot fuel existing at the end of the nuclear excursion cannot come into intimate contact with other materials, an upper bound on this destructive energy can be obtained by isentropically expanding the fuel to a pressure of one atmosphere (if the sodium is allowed to come into contact with the fuel, the sodium-fuel mixture must be expanded. See Section 7). Thus, an additional relationship, maximum potential work energy vs total energy, is also required for the fuel material.

The required relationships used in this study were estimated using the principle of "corresponding states"⁽³³⁾ and are described in Appendix B.

6.2 Parametric Studies

The energy generated in a nuclear excursion accident is, of course, a function of all of the parameters of the reactor. In the absence of any temperature reactivity feedback, the direction of the change in the energy release caused by a change in a parameter can usually be determined intuitively. If the prompt Doppler temperature coefficient has an appreciable reactivity effect, however, oscillations in the reactor power can occur. Meyer⁽³¹⁾ has shown that these power oscillations can cause oscillations in both the magnitude and direction of the effects on energy release of a given parameter. Furthermore, the interaction of one parameter change with that of another can be quite obscure.

The FTR has an appreciable Doppler coefficient and thus the oscillations described above should be quite evident in

maximum accident calculations. In order to determine to what extent these oscillations could affect the calculations, a sensitivity analysis was performed. A reference accident was chosen and energy releases were obtained for variations of each of several reactor parameters while holding all others fixed. No attempt was made to vary all parameters simultaneously to obtain the maximum possible energy release. Even with the constraints made possible by the relatively well-known accuracy of some reactor characteristics, such an optimization study would be quite difficult due to the nonlinearity of the effects of the parameters on the calculated results.

The reference accident chosen for these parametric calculations was an at-power accident initiating a reactivity ramp of 100\$/sec. The neutron lifetime and Doppler values were characteristic of a core with the sodium coolant still in. However, the equation of state conservatively assumed that the core was void of sodium. Power and worth curves representative of the zoned FTR were estimated from the corresponding curves used in the MELT-I code in BNWL-611.⁽¹⁶⁾ These same curves were used in this study and are shown in Figures 18 and 19. A complete description of the initial conditions of the accident is shown in Table XI.

At the time the power transient was terminated in this case, the energy content of the fuel (energy = 0 at the melting point in the solid phase) was 3500 MW-sec and 237 MW-sec of energy could do destructive work if the fuel isentropically expands to 1 atmosphere. Figure 20 shows the power level during the transient and Figure 21 shows the components of the net reactivity just before and during disassembly. Note that for graphical convenience both the input ramp and the Doppler feedback were renormalized to zero at 0.018 sec after the start of the accident. The total reactivity effect of each, of course was much higher (i.e., after 0.018 sec the external ramp had added +1.80\$. The Doppler effect was such

FIGURE 18. Power Distribution Employed in the Disassembly Phase

FIGURE 19. Fuel Worth Distribution Employed in the Dissassembly Phase

TABLE XI. *Characteristics of the Reference Accident*

Initial Power Level	400 MW
Initial Temperature	1300 °K (flat)
Reactivity Ramp	100\$/sec
Doppler Coefficient ($T \frac{dk}{dT}$)	-0.0035
Neutron Lifetime	3.5×10^{-7} sec
Melting Temperature of Fuel	3000 °K
Heat of Fusion	280 J/g
Specific heat at constant volume	0.42 J/g °K
Equation of State	Menzies' Constants
Volume fraction occupied by non-fuel materials	0.25 (sodium out)
Work Energy Relationship	Menzies' Constants
Core Volume	1163 ℓ
Total Fuel Inventory	3802 kg
Zone Thicknesses (spherical shells)	
Zone I (Test)	14 cm
Zone II (Inner Driver Fuel)	35 cm
Zone III (Outer Driver Fuel)	16.2 cm
Smeared Fuel Density	
Zone I	1.64 g/cm ³
Zone II	3.29 g/cm ³
Zone III	3.29 g/cm ³

FIGURE 20. Power Trace for the Reference Accident

FIGURE 21. Reactivity Components During Disassembly of the Reference Accident

that the net reactivity was 1.03\$ as shown on the figure). The anticipated power oscillations are evident in Figure 20 and, thus, their effect must be taken into account in FTR studies.

6.2.1 Sensitivity Studies - Variations about the Reference Accident

- (a) Variations in Fuel Worth - The net reactivity in the reference case is below prompt critical and the power level is dropping at the time disassembly occurs (Figure 21). This would indicate that variations in a parameter which only change the magnitude of the disassembly feedback would not yield large changes in the energy release. This is indeed the case for the worth of fuel material as shown in Figure 22. Even if power oscillations occur, increasing fuel worth will always reduce the energy release. The sensitivity of this parameter, however, will depend on the power level and reactivity at the time of disassembly.
- (b) Variations in the Peak-to-Average Temperature Ratio - Another parameter whose effect can be anticipated is the initial temperature distribution. At a core averaged temperature of 1300 °K, increasing the peak-to-average temperature ratio from a value of 1.0 decreases the energy released in the accident as shown in Figure 23. The Doppler effect should not be a strong function of the initial temperature distribution since it is based on a core averaged value for these calculations. Thus, the power trace prior to disassembly is not affected. For peak-to-average >1.0 however, disassembly occurs sooner since pressures are generated earlier (near the peak

FIGURE 22. Effect of Fuel Worth on Energy Release

FIGURE 23. Effect of Temperature Peaking on Energy Release

temperature) and the disassembly driving forces (pressure gradients) are enhanced. As in the case of fuel worth, increasing the initial peak-to-average temperature ratio always decreases the energy release and the magnitude of the effect depends on the reactor conditions near the start of disassembly.

- (c) Variations in the Equation of State - The effect of the equation of state used during the transient is shown in Table XII. For the reference case the density dependence (point of departure into pure liquid phase) had no effect. If fuel movement is disregarded (as in Meyer⁽³¹⁾), the specific volume of the fuel, including all voids, would always be $\sim 60 \text{ cm}^3/\text{mole}$ (reduced volume $V_r = \sim 0.68$). The maximum energy density generated, however, would only have caused departure from the vapor pressure curve for $V_r < \sim 0.38$. Thus, the result is not surprising. Departure was observed at several points in the MAX calculation since some mesh intervals compressed to $V_r < 0.38$. However, these did not have an appreciable effect on the overall calculation.

The equations of state obtained from both the Menzies (base case) and Miller critical constants yielded almost identical energy releases. From Figure B.2 (Appendix B), it is evident that the pressures obtained from Miller's constants are a factor of ~ 2.4 lower than those obtained from Menzies' values for a given fuel energy density. The specific heat estimated from the corresponding states calculation for Miller's constants, however, was $\sim 0.38 \text{ J/g } ^\circ\text{K}$ (versus $0.42 \text{ J/g } ^\circ\text{K}$ for the reference case). Thus, the lower pressures were compensated for by a larger Doppler effect.

TABLE XII. Effect of Equation of State on Energy Release^(a)

Equation of State	Peak Pressure, atm	Peak Temperature, °K	Energy in Molten Fuel, MW-sec (E = 0 at 2970 °K in solid phase)	Average Temperature of Molten Fuel, °K	Work Energy from Fuel Expansion, MW-sec
Reference EOS (Menzies' Constants ⁽³⁴⁾)	135	5700	3500	4530	237
Reference EOS with no density dependence (all vapor pressure curve)	135	5700	3500	4530	237
Reference EOS with pressures/10	22	6030	3900	4780	330
Reference EOS with $P_{\text{threshold}} = 100 \text{ atm}$ ^(b)	213	6010	3890	4770	326
Reference EOS with Sodium in	1310	4040	1470	-	2
Miller's Constants ⁽³⁵⁾	55	5950	3500	4700	230
APDA ⁽³²⁾	77	5790	3610	4600	259
BNWL-611 ⁽¹⁶⁾	181	5610	3400	4460	259

a. See Appendix B for description of the equations of state

b. Calculated pressures ignored until they equal 100 atm

The APDA⁽³²⁾ equation of state yielded only slightly higher energy release values than the reference case, even though the APDA pressures were a factor of ~ 2 lower (Figure B.2) than those of the reference case over most of the energy density range covered. The higher APDA pressures below an energy density of 300 J/g may have been important enough, however, to significantly affect the calculated results.

Arbitrary changes to the reference equation of state produced noticeable effects on the energy release. Reducing the calculated pressures by a factor of 10 increased the time required for disassembly, and employing a pressure threshold of 100 atmospheres (pressures not allowed to cause fuel movement until they reach 100 atm) delayed the start of disassembly. Both caused increases in the work due to fuel expansion of $\sim 40\%$.

A "sodium-in" equation of state (although ignoring the pressures caused by the sodium) was constructed by specifying that the volume fraction of the core occupied by non-fuel material was 0.647. This would cause departure from the vapor pressure curve to occur at an energy density of ~ 220 J/g (3500 °K) if fuel density changes are disregarded. The transient will continue on past this point since shutdown is hindered by the inertia of the core material. There is, however, a significant reduction in the calculated energy release.

The equation of state used in BNWL-611 employing both higher pressures and a lower specific heat yielded only slightly optimistic energy release values.

The effects of the equation of state cannot be isolated as completely as was done for the worth of

fuel since, in some instances, the specific heat (which directly affects the Doppler effect and thus the power oscillations) is changed. If, however, only the pressures during the disassembly phase are varied, the effects on energy release can be predicted.

- (d) Variations in the Reactivity Ramp Rate and Doppler Coefficient - The effect of the reactivity ramp on energy release is shown in Figure 24 as a function of the Doppler coefficient. These curves have the same general shapes observed by Meyer⁽³¹⁾ except that the oscillations occur at lower values of the Doppler coefficient. This difference is probably accountable to the longer neutron lifetime in the reactor analyzed by Meyer. The same general conclusions reached by Meyer, however, still apply in that the energy release is sensitive to the Doppler coefficient and the power oscillations resulting from Doppler feedback can have a pronounced influence on the results of varying other core parameters.
- (e) Variations in Neutron Lifetime - The neutron lifetime can appreciably affect the calculated energy release as shown in Figure 25. Decreasing the lifetime increases the sensitivity of the system to small changes in reactivity around prompt critical, and thus, in the presence of a strong Doppler coefficient, the number of power pulses should increase. Indeed, for the cases considered, the number of pulses varied from one to four as the lifetime decreased. The peaks in this figure occur when disassembly occurs near the peak in the last power pulse.
- (f) Variations in the Heat of Fusion - The sensitivity of the results to the heat of fusion is shown in Figure 26. Increasing this parameter delays both the

FIGURE 24. Effect of Reactivity Ramp and Doppler Coefficient on Energy Release

FIGURE 25. *Effect of Neutron Lifetime on Energy Release*

FIGURE 26. *Effect of the Heat of Fusion on Energy Release*

Doppler effect and the pressure buildup and increases the time during which the external ramp can add reactivity to the system. More refined measurements of this fuel property, however, are not expected to reveal a marked variation from the 280 J/g assumed.

- (g) Variations in Initial Temperature - The most pronounced temperature effect occurs when the initial temperature is varied. As seen in Figure 27, assuming the entire core is initially at the melting point can greatly increase the energy release. Increasing the temperature reduces the Doppler effect and decreases the pressure gradients across the core (thus decreasing the disassembly driving force). In the presence of power oscillations, however, there may be reversals in the general trend. In reality, of course, if the core average temperature were high, there would be a significant spatial peaking effect. As previously shown, this would tend to lower the values shown in Figure 27 for the higher (>1300 °K) temperatures.
- (h) Variations in Initial Power - In the absence of an appreciable Doppler coefficient, it would be expected that lowering the initial power level would increase the energy release, since both the power level at the time of disassembly and the reactivity added by the external ramp would be increased. The peak observed in Figure 28 is caused by the power oscillations resulting from the Doppler effect. In the 0.1 MW case for instance, disassembly starts before the second power peak reaches its maximum.

It was assumed in Figure 28 that the reactor had operated at the power levels shown for a sufficiently long period prior to the accident so that asymptotic delayed neutron precursors were established. Power

FIGURE 27. Effect of Initial Temperature on Energy Release

FIGURE 28. Effect of Initial Power on Energy Release

levels below the normal operating conditions would thus be inconsistent with a core average temperature of 1300 °K. In order to estimate the energy released from an accident initiated in the shutdown condition, several cases were run in which both the initial temperature and initial power levels were changed (to 300 °K and 2 kW, respectively). The results as a function of the Doppler coefficient are shown in Figure 29. As expected, the oscillations occur at lower values of the Doppler than for the at-power cases. (This is a direct result of a larger effective Doppler feedback caused by the larger temperature differential.) The energy release values, however, are quite comparable to those obtained for the at-power situations, at least for Doppler coefficients near -0.003.

6.2.2 Modifications for Sodium-In Conditions

In Section 5.2 it was found that expelled sodium from the core caused by molten fuel-sodium interaction may increase the effective reactivity ramp at the time of disassembly to ~250\$/sec. Two cases were considered in which these reactivity consequences were treated. The first assumed that the core was at the normal operating condition (400 MW, 1300 °K) prior to the start of the accident. The second started at the shutdown condition (2 kW, 300 °K). The reactor parameters were the same as those of the reference case except that the Doppler coefficient was -0.003 and an equation of state characteristic of sodium-in condition was used for the bulk of the reactor. For the region of the core undergoing sodium voiding, the normal sodium-out equation of state was used. Further, as discussed in Appendix B, a fraction of the sodium pressure responsible for the large sodium ejection rates was included in the disassembly process. For the particular cases considered, the

FIGURE 29. Effect of Doppler Coefficient on Energy Release for Shutdown Accident

region located near the power peak of the inner driver fuel zone was assumed to approximate the portion of the core undergoing sodium voiding. Here an arbitrary pressure of 100 atm was added, and a sodium-out equation of state was utilized for the entire inner driver zone for conservatism. The pressure of 100 atm is conservative since a value of approximately 600 atm was used to obtain the expulsion acceleration of 1000 g's.

Both accidents considered were started in the MELT-I code where the sodium voiding reactivity effect was calculated. The resulting total reactivity versus time curves (both initiating ramp and sodium expulsion feedback) were input in the MAX code. The initial conditions in MAX were those calculated by MELT-I at the start of sodium boiling. The calculated results were 1600 MW-sec and 2000 MW-sec for the at-power and shutdown situations, respectively. These values are the energy in the molten fuel (energy = 0 at the melting point in the solid phase) instead of work from fuel expansion, since, in the presence of sodium, the coolant may be the working fluid. These results may be questionable since they were obtained for only one value of the Doppler coefficient. Further, the local pressure and density changes which occur for a sodium-in case are rather violent, and the ability of the codes to execute reliable analyses for such cases is not established. Clearly, more work is required before a high degree of confidence can be associated with such studies.

The results do indicate that use of a sodium-in equation of state can substantially mitigate the consequences of high reactivity ramp insertion rates which could result from the initiation of central sodium voiding.

6.3 Summary of Phase III

Care should be exercised in using the results of the above parametric studies since they apply specifically to the cases

considered. As Meyer⁽³¹⁾ has stated, the effect of a parameter change can be quite different depending upon whether core dispersion occurs at the top or the bottom of a power pulse. For instance, the peaks observed for the initial temperature in Figure 27 can be displaced (at least horizontally if not vertically) by changing the magnitude of the Doppler effect.

The results indicate that many parameters can significantly affect the energy release. As stated previously, no attempt was made to maximize the energy release by varying all parameters simultaneously, and it is, therefore, possible that a combination could be found which would yield values considerably higher than those shown in this section.

The parametric studies indicated also that the energy release can sometimes be significantly altered if different assumptions are made as to the course of the accident. In view of this, the characteristics of four broad classes of accidents are presented below. These are accidents initiated from either full power or shutdown conditions where, in both cases, the consequences of sodium in or out are considered.

This division was chosen because the presence of sodium can appreciably change the method used in calculating the energy release and because the two "fixed" operating modes of the reactor are the at-power and shutdown conditions.

Two cases showing the effects of the presence of sodium were discussed in Section 6.2. Since no variations were made in the parameters used in the calculations, these cases are presented.

Accidents for which sodium has left the core can be calculated entirely in MAX and may be estimated from the figures of Section 6.2. The reference case for the results presented however, assumed a Doppler coefficient and neutron lifetime characteristic of a core containing sodium. Without the sodium coolant present, both of the parameters should decrease

in value. If it is assumed that the "sodium-out" Doppler coefficient is ~ -0.002 and if the neutron lifetime effects are ignored, the energy release for a "sodium-out", at-power accident would be near the first minimum of the energy-Doppler coefficient curve as shown in Figure 24. Meyer⁽³¹⁾ however, has shown that decreasing the neutron lifetime shifts the energy release oscillation to lower values of the Doppler coefficient. Thus, if both the lifetime and Doppler effects are considered, it is estimated that the "sodium-out" at-power case would result in an energy release at or near the first peak on the energy curve. If it is assumed that the amplitudes of the energy oscillations are independent of neutron lifetime and (to avoid undue optimism) only the peaks in the energy curve are considered, the "sodium-out", at-power case would correspond approximately to the reference case with a Doppler coefficient of -0.003 . Similar arguments for the shutdown mode indicate that the energy release for the sodium-out case could be approximated from Figure 29 with a Doppler coefficient of -0.0026 .

The characteristics of all four accidents are shown in Table XIII.

TABLE XIII. *Characteristics of Postulated
Maximum Accidents for the FTR*

	Case			
	I	II	III	IV
Initial Power (MW)	400	400	0.002	0.002
Initial Temperature ($^{\circ}$ K)	1300	1300	300	300
Sodium (In or Out)	In	Out	In	Out
Energy Generated in Transient (MW-sec)	4000	6700	5500	8000
Energy in Molten Fuel at Termination of Nuclear Excursion (MW-sec)	1600	4020	2000	3700
Work Energy from Fuel Expansion (MW-sec) ^a	14	360	34	320
Fuel in Vapor State After ^b Fuel Expansion (kg)	59	726	125	631
Molten Fuel (kg)	3040	3800	3250	3780
Peak Mass Average Temperature of Molten Fuel ($^{\circ}$ K)	3600	4850	3800	4650
Peak Temperature ($^{\circ}$ K)	4380	6120	4630	6150
Peak Pressure (atm) ^c	7	246	14	256

a. See Section 7 for the work from expansion of sodium.

b. Estimated from the work calculation. See Appendix B.

c. The low peak pressures in the "sodium-in" cases are those existing at the termination of the nuclear excursion at the points with the highest temperatures. Much higher (orders of magnitude) pressures existed earlier in the transient, but were relieved due to fuel expansion.

7. PHASE IV - ENERGY PARTITIONING

The purpose of maximum accident studies is to define design bases for the containment system so that the energy generated can be absorbed and contained without loss of the capability to limit the release of radionuclides. The energy generated in the nuclear excursion may be released from the core in several different forms - e.g., heat, shock, and blast. Therefore, limits on the distribution of the total energy, or its partitioning, must be defined so that the containment system can be designed to absorb the released energy in each of the various forms.

The total energy generated during the excursion is determined by integration of the power transient between initiation of the accident and disassembly. However, evaluation of the fraction of that energy which could do destructive work upon the surrounding structures and partitioning of that work energy is dependent upon the rate at which the power is generated, interactions of fuel and other materials, and the configuration of the surrounding structures. The rate of power generation for a typical transient is illustrated in Figure 30.

The best method for evaluating the energy partitioning, particularly that fraction of the total energy which is capable of producing destructive work, is not obvious. Four techniques which were considered are:

- (a) Efficiency Factor - Assume a certain fraction of the total energy of the excursion is converted to work. Supposedly, the value of the efficiency so chosen would be conservative so that any variations induced by characteristics of the particular reactor are not significant.

FIGURE 30. Energy Generation Rates in a Particular Disassembly Accident

- (b) Total Energy Exceeding Some Threshold - Assume that the total energy generated after reaching some threshold be converted to high explosive on the basis of equal total energy. For example, the threshold might be the melting point of fuel. The capability of the energy generated above the threshold to do destructive work would be assumed equivalent to the high explosive.
- (c) Work of Fuel Expansion - Assume that the maximum work energy is equal to the $\int PdV$ work of expansion of the fuel vapor to 1 atm.
- (d) Work of Coolant Expansion - Assume heat transfer from fuel to coolant so that the coolant becomes the working fluid. Expansion of the fluid provides $\int PdV$ work.

A discussion of these conversion techniques and some of the pertinent experimental and analytical background is presented in Appendix C.

Analysis of the expansion of fuel vapor and of sodium vapor can establish thermodynamic upper limits to the amount of work which could be done by an excursion. These techniques were chosen with expansion of fuel as the working fluid applied in analysis of sodium-out excursions, and sodium as the working fluid for the sodium-in excursions.

For fuel vapor as the working fluid, an isentropic expansion to 1 atmosphere pressure was analyzed, following the approach first described by Jankus.⁽³⁶⁾

For sodium vapor as the working fluid, three methods were used as noted in Section 2.2.; these correspond to different assumptions for heat transfer from fuel to sodium during the expansion process. The most conservative analysis (largest work energy) assumed perfect heat transfer throughout the expansion to one atmosphere.

It has been a common practice to define a mass of high explosive equivalent to a given nuclear excursion. This provides grounds for evaluation of containment response on the basis of experiments with chemical explosives. The time, temperature, and pressure characteristics of nuclear excursions and chemical explosions are so greatly different, however, that general principles of equivalence are difficult to define. Although it is recognized that chemical explosion equivalents for the FFTF DBA may have to be established for the purpose of application of existing experimental data or performance of new experiments, a definition of the chemical equivalent is not included in this study.

The theoretical work by expansion of fuel vapor is calculated by the MAX code, and the results are shown for the cases studied in Section 6.

For theoretical work by expansion of sodium vapor, calculations were made for several assumed average temperatures of molten fuel just prior to mixing with sodium. The results are shown in Table XIV. The highest average molten fuel temperature calculated for sodium-in conditions was about 3800 °K. For conservatism in the maximum accident studies, the work energy was calculated assuming a temperature of 4000 °K.

8. CONCLUSION

The generated energies for the various cases considered in the analysis are summarized in Table XV. Based upon these results, and considering the uncertainties associated with the analyses, the magnitude of a postulated maximum accident for the FFTF is concluded to be in the range of 500 to 1500 MW-sec of work energy release where this energy is the work which theoretically could be done by expansion of vaporized core materials.

TABLE XIV. *Maximum Work Energy by Sodium Vapor Expansion to 1 Atm (MW-sec)*

	<u>Molten Fuel Temperature (°K)</u>		
	<u>3800</u>	<u>4000</u>	<u>4850</u>
Isentropic expansion (no heat transfer with fuel during expansion)	493	541	702
Thermal equilibrium to melting point of fuel, isentropic expansion thereafter		978	
Thermal equilibrium with fuel (perfect heat transfer with fuel during expansion)		1288	

TABLE XV. *Summary of Results*

		Total Energy (MW-sec)	Energy Generated at Temperatures >3000 °K (MW-sec)	Work Energy (MW-sec)
Reactor at Power	Sodium-In	4000	1580	540 ^a
	Sodium-Out	6700	4000	1290 ^b
Reactor Shutdown	Sodium-In	5500	2000	493 ^c
	Sodium-Out	8000	3700	320

a. Isentropic expansion of sodium - Fuel temperature at disassembly assumed 4000 °K rather than 3600 °K which corresponds to 1580 MW-sec.

b. Assumes thermal equilibrium between fuel and sodium throughout expansion - Fuel temperature assumed 4000 °K at end of nuclear excursion.

c. Isentropic expansion of sodium - Fuel temperature at disassembly equals 3800 °K which corresponds to 2000 MW-sec.

It should be noted that the value of 1500 MW-sec is largely based upon the peak calculated value of 1290 MW-sec

as shown in Table XV. However, this peak value resulted from sodium-in calculations which are undoubtedly subject to greater uncertainties than the corresponding sodium-out calculations. These uncertainties have required additional factors of conservatism in the analysis. The two major areas of uncertainty in which the assumed conditions are believed to be quite conservative for the peak case are: (a) the assumed sodium voiding rate, which results in an extremely large reactivity insertion rate, for molten fuel-sodium interactions, and (b) the assumed continual thermal equilibrium of the sodium with the fuel mixture throughout the sodium vapor expansion phase in the analysis for the work energy. Further analysis or applicable experimental work in these areas could very well result in a reduction in the level of conservatism and a corresponding reduction of the value of the work energy.

ACKNOWLEDGEMENTS

The authors wish to acknowledge the contribution of John E. Hanson who has provided information concerning fuel failures and significant fuel interactions expected under severe accident conditions used in the maximum accident studies and who also has served throughout the past year as a member of the Task Force assigned responsibility for definition of the FFTF design basis accident.

The authors also wish to acknowledge the contribution of Winston W. Little, Jr., who has provided overall direction for the physics analyses and developed the MAX program for the disassembly phase.

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APPENDIX A

MAXIMUM ACCIDENT DEFINITIONS
FOR EBR-II, FERMI, FARET, AND SEFOR

The results of safety reviews of the four fast reactors, EBR-II, FERMI, FARET, and SEFOR, are pertinent to the choice of an appropriate DBA for the FFTF. These precedents are summarized below.

A.1 EBR-II

The hypothetical core meltdown accident for EBR-II is described in its Hazard Summary Report.⁽⁵⁾ The set of hypotheses used in the analysis is outlined in the report:

- "(1) The sodium has boiled away from the center of the reactor.
- "(2) The uranium from the middle of the core has trickled down into the lower part of the core and is retained there, producing a region abnormally dense in enriched uranium at the core bottom, with a large gap at the core center.
- "(3) At the worst possible moment, the upper portion of the core falls as a single unit, producing a prompt critical configuration at the highest possible insertion rate."

With a reactivity ramp rate of 1000\$/sec, the total nuclear energy yield of the excursion was calculated to be 2400 MW-sec. However, it was assumed that only the energy generated above the threshold energy, that energy required to fill all the void volume, could be released as explosive energy. With this reduction, the amount of explosive energy was calculated to be ~2000 MW-sec--stated to be equivalent to 1050 lb TNT. The energy was also calculated for a ramp rate of 600\$/sec and 200\$/sec, with the following results:

<u>Ramp Rate, \$/sec</u>	<u>Explosive Energy Yield, lb TNT</u>
200	280
600	660
1000	1050

Because of the extreme pessimism in the 600 and 1000\$/sec insertion rates, the containment design was based upon 300 lb of TNT.

Later analyses⁽⁶⁾ adjusted the above energy yields, but the containment design basis was maintained at 300 lb TNT. These later results are:

<u>Ramp Rate, \$/sec</u>	<u>Explosive Energy Yield, lb TNT</u>
200	370
600	850
1000	1240

A.2 FERMI

The hypothetical meltdown design accident is described in the FERMI Hazards Summary Report.⁽³⁾ The meltdown model chosen for FERMI assumes that the core is completely molten and the top collapses under gravity upon the bottom with the bottom plane of the core fixed. In order to allow maximum acceleration of the upper core regions before disassembly, various fuel loadings were chosen to investigate disassembly for criticality at various levels of collapse.

With a collapse of 20% of its original height, an energy release of 2700 MW-sec (6.5×10^8 g-cal) was calculated as the maximum for an initial power of 100 MW. With an initial power of 10 MW, it was concluded that a release of 3300 MW-sec (8.0×10^8 g-cal) could theoretically result. Therefore, the containment basis was chosen as 8.0×10^8 g-cal.

The work from an excursion of this energy release was calculated by adiabatic expansion of the fuel vapor. Using the equivalence that 1 kilo-calorie of work from adiabatic expansion equals 1 gram of TNT (1.9 MW-sec/lb), the maximum hypothetical meltdown accident of 6.5×10^8 g-cal was equated to the explosive effects of 500 lb of TNT. The containment was evaluated for an explosive release of 1000 lb of TNT.

A.3 FARET

The containment system analysis for FARET was based upon the MCA approach as described in the Preliminary Safety Analysis.⁽⁷⁾ In this analysis, credible accidents resulting from dropping a fuel subassembly into position and from core meltdown were investigated. The fuel drop-in accident was calculated to yield an initial ramp insertion of 20\$/sec.

The core was assumed to melt and collapse initiating a continuous reactivity insertion (as opposed to the reactivity threshold model used for EBR-II and FERMI). In this model, the core temperature was assumed flat radially, but divided axially into discrete units with the specific power of each unit dependent upon the axial power distribution. Collapse of each unit was allowed only when the unit reached some threshold condition.

The disassembly analyses for FARET were performed using the AX-1⁽³⁷⁾ code. The maximum work from the nuclear excursion was calculated by the code as the summation of the component kinetic energies and the volume integrated pressure. This indicated a conversion efficiency of 4 to 8%. Therefore, a factor of 10% was proposed as the basis for design.

With the initial ramp of 20\$/sec and the 10% conversion factor, a work energy of 6×10^7 joules (60 MW-sec) was calculated as the MCA for FARET. The conversion to the high-explosive equivalent yielded 30 lb of TNT. The containment design was to be based upon 40 lb of TNT.

A.4 SEFOR

The design basis excursion for SEFOR is described in the preliminary safety analysis report.⁽⁸⁾ The sequence of events assumed for this accident analysis is:

- "(1) The FRED (Fast Reactivity Excursion Device) is fired and inserts over a dollar of reactivity at a high rate (in the order of several tens of dollars per second).
- "(2) The core coolant flow is lost, and the reactor does not immediately scram.
- "(3) The prompt power burst resulting from the firing of the FRED is turned around by the Doppler effect during which time a significant amount of fuel has been heated to temperatures above the fuel melting point. The high power in the tail of the prompt power curve continues to raise the fuel temperatures and melts more fuel.
- "(4) Heat transfer from the hot fuel to the fuel cladding melts the cladding beginning at the center of the core.
- "(5) When the fuel cladding reaches the melting point, the fuel in that rod collapses under the influence of gravity into the void space normally occupied by the coolant. No credit is taken for friction or viscosity effects.
- "(6) Scram occurs at the worst possible time, so that the reactor is subcritical when core compaction starts (this gives the maximum reactivity insertion rates at the start of the excursion since it provides time for acceleration of the

fuel before prompt criticality is reached). Without the scram or with the scram occurring at the start of the excursion, the energy release is reduced."

The initial ramp rate for the FRED was conservatively chosen as 100\$/sec for a total insertion of 2\$. This initiated a high power condition leading to core melting. Melting was analyzed as occurring sequentially in radial rings, producing a continuous reactivity model. However, by assuming a scram at the worst possible time to allow acceleration of the collapse, a partial threshold model also is demonstrated.

The peak reactivity ramp rate analyzed during the process was approximately 40\$/sec which decreases to a constant value of 20\$/sec. With the scram, the latter value applies at the time criticality occurs.

A modified Bethe-Tait Analysis was used to calculate the energy release. Work was calculated by isentropic expansion of the core material. This work for the above conditions was calculated at 90 MW-sec and the preliminary containment design basis is 400 MW-sec (stated to be equivalent to 200 lb of TNT).

Additional studies shown in the preliminary report show that a ramp rate of 50\$/sec results in work of 320 MW-sec--still under the 400 MW-sec design basis.

APPENDIX B

EQUATION OF STATE

No experimental data on the pressure, energy and temperature relationships (equation of state) for mixed oxide fuels is available for the temperatures and pressures usually encountered in maximum accident calculations. It has therefore been the practice to estimate these properties of the fuel by extrapolating or otherwise utilizing what little data is available at low temperatures. The data most often used in this procedure are the vapor pressure measurements of Ackermann⁽³⁸⁾ and Ohse⁽³⁹⁾ and the density measurements of Christensen.⁽⁴⁰⁾ These data were obtained for UO_2 ; however, in the absence of similar data for PuO_2 , it was assumed that the properties of UO_2 could be used to describe the reactor fuel. This assumption is quite reasonable since the two oxides should behave similarly. Further, the fuel is made up primarily of UO_2 in intimate contact with the fissile PuO_2 fuel.

The most often employed method to estimate the high pressure-high temperature equation of state is the principle of corresponding states.⁽³³⁾ This principle states that all materials behave similarly if put in terms of the reduced variables of state. These reduced variables are the ratios of the normal variables to the corresponding values at the critical point (i.e., reduced temperature = actual temperature/critical temperature). If a few properties (estimated from low temperature data) of a material are known, all of the required properties including the available work-energy content relationship can be determined using this principle. This method was chosen to obtain the equation of state needed in this study. It is well known that several materials deviate significantly

from this principle.⁽³⁵⁾ However, in the absence of any obviously better method, the procedure will provide at least an estimate of the quantities desired.

Hougen, et al.⁽³³⁾ have generated tables for applying the corresponding states principle based on measurements of several different materials. The procedures for using these tables are shown by Meyer⁽³¹⁾ and, for the sake of brevity, are omitted from this report. The Meyer method requires that several characteristics of the material and system under consideration be specified. These are:

- (a) The critical constants (i.e., temperature at the critical point),
- (b) The specific heats at constant pressure and volume of an ideal gas composed of the fuel material,
- (c) The melting and boiling temperatures,
- (d) The latent heats of vaporization at both the melting and boiling points, and
- (e) The specific volume of the fuel material in the system under consideration.

The choice of critical constants can appreciably alter the equation of state derived from the corresponding states principle. Both Miller⁽³⁵⁾ and Robbins⁽⁴¹⁾ have investigated the possible range of constants and obtained quite large limits for their values. The constants used for this study, however, were chosen from the "probable" values obtained by three investigators. (31,34,35) All used different approaches; however, some "reduced relationships" (corresponding states principles) were utilized in each instance and the low temperature data discussed above (38,39,40) were taken into account. In general, the results compare reasonably well for the accuracy one would expect from their separate methods. These critical constants are shown in Table B.I.

TABLE B.I. Critical Constants of UO_2

	<u>Menzies</u> ⁽³⁴⁾	<u>Miller</u> ⁽³⁵⁾	<u>Meyer et al.</u> ⁽³¹⁾
Critical Pressure (atm.)	2000	1230	1915
Critical Temperature (°K)	8000	9115	7300
Critical Volume (cm ³ /mole)	90	170	85

At first it would appear that making a choice between the constants shown in Table B.I. would merely be an academic exercise. Several properties of the Menzies constants, however, make them the most desirable. In the tables of Hougen et al.,⁽³³⁾ the pressure-temperature relationships in the saturated region (both liquid and vapor phase exist) were obtained from the reduced vapor pressure equation of Riedel.

$$\ln P_r = \alpha \ln T_r - 0.0838(\alpha - 3.75) \left(\frac{36}{T_r} - 35 - T_r^6 + 42 \ln T_r \right) \quad (1)$$

where

$$\alpha = \left(\frac{1}{Z_c} - 1.90 \right) \frac{1}{0.26}$$

and

P_r = the reduced pressure,

T_r = the reduced temperature,

V_r = the reduced volume,

$Z_c = \left(\frac{P_c V_c}{R T_c} \right)$, the compressibility at the critical point, and

R = the gas constant.

Vapor pressure curves using Equation (1) were generated for the Miller and Menzies critical constants (the Meyer constants yield values quite similar to those of Menzies) and are shown in Figure B.1 along with the analytical fits to the data of Ackermann⁽³⁸⁾ and Ohse.⁽³⁹⁾ As was found by APDA⁽³²⁾, the

FIGURE B.1. Calculated and Measured Vapor Pressure Curves

Miller constants yield vapor pressures much lower than the measured data when extrapolated to lower pressures. The vapor pressure curve for the Menzies constants, however, agrees quite well. This is to be expected, since this was a criterion used to obtain the constants. It should be noted that the experimental curves are extrapolations of the measured vapor pressure of the solid. The curves should be corrected slightly to reflect the vapor pressure of the liquid (reduced slope). In addition, as will be discussed later, the specific heat at constant volume estimated from the tables of Hougen, et al.⁽³³⁾ for the Menzies' constants agree with the estimate of Meyer⁽³¹⁾ and APDA⁽³²⁾ (0.42 J/g - °K) while the corresponding value for the Miller constants was significantly lower (0.377 J/g - °K). Equations of state for both sets of constants were generated, however, in order to determine if any large pessimism or optimism is associated with the Menzies constants.

Meyer obtained the latent heat of vaporization at the melting point by first estimating the latent heat of sublimation at the melting point from the vapor pressure data for UO_2 . A form of the Clausius-Clapeyron equation⁽⁴²⁾ states that

$$\frac{\partial (\ln P)}{\partial \left(\frac{1}{T}\right)} = \frac{-\lambda_{\text{sub}}}{R} \quad (2)$$

where λ_{sub} = the latent heat of sublimation.

The latent heats of sublimation obtained from all four analytical fits to the vapor pressure measurements at the melting point varied from 154,000 to 124,000 cal/mole °K. An estimate of the latent heat of vaporization was obtained by subtracting off a latent heat of fusion of 17,900 cal/mole °K.⁽⁴³⁾ These estimates varied between 136,000 and 106,000 cal/mole °K.

A generalized method for obtaining the latent heat of vaporization is:⁽³³⁾

$$\frac{\lambda_{\text{vap}}}{T_c} = \frac{R T_{rs}^2 (Z_{sg} - Z_{sl})}{P_{rs}} \left(\frac{\partial P_r}{\partial T_r} \right)_s \quad (3)$$

where

- λ_{vap} = the latent heat of vaporization,
- $T_r = T_{rs}$ = the reduced temperature,
- $P_r = P_{rs}$ = the reduced pressure,
- Z_{sg} = the compressibility of the vapor,
- Z_{sl} = the compressibility of the liquid, and
- R = the gas constant.

$\frac{\partial P_r}{\partial T_r}$ and the relationship between P_r and T_r can be obtained from the reduced vapor pressure equation of Riedel (Equation (1)).

Assuming $Z_{sg} = 1.0$ and $Z_{sl} = 0$, a value of 127,000 cal/mole °K was obtained for the latent heat of vaporization at the melting point (3070 °K) for the Menzies constants. This compares quite reasonably with the estimates derived from the experimental data. Therefore, these equations were used to obtain all of the latent heats of vaporization needed in these calculations.

The boiling points were obtained from Equation (3) by assuming $P = 1$ atm and solving for the boiling temperature.

The values of the specific heats at constant volume and pressure for an ideal gas composed of UO_2 were chosen to be $(13/2)R$ and $(15/2)R$, respectively, following Meyer⁽³¹⁾ and Menzies.⁽³⁴⁾

A summary of the parameters needed to obtain the fuel properties (except for the specific volume) is shown in Table B.II.

The corresponding states calculations were performed for several values of the specific volume of the fuel and the results for the Menzies constants showing the effect of fuel density are shown in Figure B.2. The energy base is at the melting point of the liquid in this figure.

TABLE B.II. *Parameters for Corresponding States Calculations*

	<u>Menzies'</u> <u>Constants</u>	<u>Miller's</u> <u>Constants</u>
Melting temperature ($^{\circ}\text{K}$)	3070	3070
Boiling temperature ($^{\circ}\text{K}$)	3824	4287
Latent heat of vaporization at melting (cal/mole $^{\circ}\text{K}$)	127,000	129,000
Latent heat of vaporization at boiling (cal/mole $^{\circ}\text{K}$)	120,000	122,000
Cp (cal/mole $^{\circ}\text{K}$)	19.9	19.9
Cv (cal/mole $^{\circ}\text{K}$)	12.9	12.9

The sharp breaks in the pressure versus energy curve as a function of density (or reduced specific volume) occur when the liquid has expanded enough to fill the available volume. The fuel is then entirely in the liquid phase and a slight increase in energy causes a significant increase in pressure. Obviously a slight increase in volume would cause the system to revert back to the vapor pressure curve. As mentioned previously, the computer code used for the calculations in this study allows fuel movement and thus can adequately cope with the phenomena. Analytical fits were made to these calculated points to provide a density-dependent equation of state in much the same manner as Menzies.⁽³⁴⁾

Also shown on Figure B.2 are the pressure-energy relationships for the saturated vapor pressure region using

FIGURE B.2. Pressure-Energy Relationships

Miller's constants, for a previous study made at PNL on the Nuclear Proof Test Facility⁽¹⁶⁾ and for a study by APDA.⁽³²⁾ The density dependence of the Miller pressures over the density range of interest ($V_r \leq 0.4$) were not obtained since the tables of Hougen and Watson are inaccurate in that region. The BNWL-611 equation of state was obtained from the fit to experimental vapor pressure data⁽⁴⁴⁾ used by Menzies⁽³⁴⁾ and was intended for the lower pressure range (<100 atm). The APDA relationship employed the low temperature fit to the Ackermann data (the lower of the two Ackermann curves on Figure B.1) in the saturated vapor region. The point of departure was obtained from a density-temperature curve⁽³⁵⁾ for the Miller constants, and the slope of the pressure energy curve after departure was estimated from Christensen's⁽⁴⁰⁾ data for the coefficient of expansion of the liquid. It is noteworthy that the Gruneison's constants (slope of the pressure versus energy curve times the specific volume) obtained for the Menzies constants are quite similar to those estimated by Robbins⁽⁴¹⁾ and at no time are they greater than that obtained by APDA.

The curves shown on Figure B.2 do not completely specify the equation of state of UO_2 since the temperature energy relationships are not defined. APDA⁽³²⁾ estimated the specific heat at constant volume to be $0.42 \text{ J/g-}^\circ\text{K}$ as did Meyer.⁽³¹⁾ The values obtained from the corresponding states temperature-energy relationships for the Menzies and Miller constants were $0.42 \text{ J/g-}^\circ\text{K}$ and $0.377 \text{ J/g-}^\circ\text{K}$, respectively. A value of 0.38 was used in the previous PNL study.⁽¹⁶⁾

The isentropic work by expansion of the fuel vapor as a function of the energy density for several equations of state are shown in Figure B.3. For point of reference, energy = 0 at the melting temperature in the liquid phase. A similar curve was used in BNWL-611; however, it was pessimistic and

FIGURE B.3. Maximum Work Energy from Fuel Expansion

overestimated the work energy. It is interesting to note that in general, all of the curves are quite similar, and, as was found by Meyer,⁽³¹⁾ the curves for the Miller and Menzies constants are quite insensitive to the reactor composition (these curves are applicable for reduced specific volumes of from 0.5 to 1.1).

In the Meyer⁽³¹⁾ method for estimating the work done by the expanding fuel, the fraction of the fuel in the vapor state after expansion occurs is obtained. This fraction is a function of the energy density of the fuel before expansion and can be used to obtain an upper limit on the amount of Pu vapor which must be confined in a design basis accident. The required fraction computed for the Menzies' critical constants is shown in Figure B.4.

The curves shown on Figure B.2 are, of course, applicable only above the melting point. Below this point, the pressures are so small that they are negligible. A temperature energy relationship is needed in this region, however, since the major portion of the Doppler effect occurs there. It was assumed that the specific heats discussed above for the high temperature region were equally applicable below the melting point.

In these types of calculations, it is generally assumed that the sodium coolant has been removed from the core. If this is not the case, and if it is still assumed that no heat is transferred out of the fuel, the appropriate equation of state can be obtained from the fitted curve in Figure B.2 by merely specifying that a smaller volume is available to the fuel. In Section 5 it was assumed that sodium received heat from the molten fuel, generating high pressures which expelled the sodium. It would, therefore, be incongruous not to include that sodium pressure in the disassembly calculation. This can be accomplished by merely specifying in the MAX code that the sodium is in the reactor in all areas except where sodium

FIGURE B.4. *Final Fuel Vapor Fraction After Isentropic Expansion Using Menzies' Critical Constants*

boiling occurs. In these boiling regions, a pressure of ~ 100 atm is added to the pressure of the fuel vapor. This is quite conservative since a significantly larger pressure (~ 600 atm) was assumed in expelling the sodium at an acceleration of 1000 g's.

Several aspects of this problem should be considered in more detail, however, since the treatment above assumed the sodium to be inert and act only to reduce the volume available for fuel expansion. The pressure-energy relationship for the sodium-in case, for example, was found to be very sensitive to the density of the fuel in its available volume. Small changes in either the energy dependence of the liquid fuel density or the volume available to the fuel can cause orders of magnitude changes in pressure.

The volume available to the fuel is quite sensitive to the volume occupied by the sodium since $\sim 40\%$ of the core volume contains coolant. Two mechanisms which can significantly affect the sodium volume are high local pressures and the transfer of heat from the fuel to the sodium. Under disassembly conditions, very high (1000 's of atmospheres) pressures exist in the core. If these pressures are present long enough, the sodium could be expelled from the core causing, among other things, a much larger volume available to the fuel. Over the more probable short time intervals, no significant bulk movement of sodium can occur. However, the fuel volume will still be increased somewhat since the sodium has a finite compressibility.

Thus far it has been assumed that no heat was transferred to the sodium during the disassembly process. If a small amount is transferred, the sodium will expand causing higher fuel pressures. For large amounts of heat transfer, sodium boiling will occur and the resulting sodium vapor pressure would increase the disassembly pressures. Those instances

in which sodium voiding occurs have been partially treated in the analyses of Section 6.2, but clearly, more investigation is needed.

The pressure, temperature, work energy, and energy relationships derived in this section are, of course, only approximations. If the sodium is out of the core (and thus the vapor pressure curve applies over most of the energy region), the relationships appear to be reasonable, since they extrapolate to measured values at low temperatures. If sodium is in, however, departure from the vapor pressure curve occurs at quite low energies. The exact point of departure significantly affects the final energy release and varies quite rapidly with the volume available to the fuel.

APPENDIX C

WORK ENERGY CONSIDERATIONSC.1 TECHNIQUES FOR DETERMINING WORK ENERGYC.1.1 Efficiency Factor

The efficiency factor technique was applied in the FARET⁽⁷⁾ analysis where the factor was chosen as 10% of the total energy generated during disassembly.

In this analysis, the disassembly was computed using the AX-1 code.⁽³⁷⁾ Use of this code provided a calculation of the kinetic energy of the displaced material and the volume-integrated pressure. The sum of these quantities provided an estimate of the fraction of total energy converted to work and it was estimated to be less than 10% (less than 1% from kinetic energy and less than 8% from the volume integrated pressure).

C.1.2 Total Energy Exceeding Some Threshold

In the analysis of EBR-II,⁽⁵⁾ the total energy generated above the threshold energy (called Q^* in the report and equal to the threshold in the equation of state) was converted to equivalent pounds of TNT. Therefore, it was assumed that the efficiency of converting the total energy above Q^* to work would be no greater than the conversion of work in a TNT explosion.

In a review of the SL-1 accident, Proctor⁽⁴⁵⁾ implied the use of the total energy in fuel at temperatures above melting in all molten and vaporized core regions to evaluate the number of pounds of TNT. Evaluation of work which this quantity of TNT can do upon the system (applying expansion techniques to the explosive products) then determined the fractional work energy from the excursion.

C.1.3 Work of Fuel Expansion

At the end of the excursion, the vaporized fuel has potential to do work by its expansion and the maximum potential to do work is calculated by isentropic expansion of the fuel to 1 atm. This process is discussed by Jankus⁽³⁶⁾ where he compares this technique with AX-1. This method was applied in the FERMI⁽³⁾ and SEFOR⁽⁸⁾ analyses. In both these cases, the sodium was assumed out of the core at the time of disassembly so that the conservative sodium-out equation of state applied. In the absence of sodium in fairly direct contact with the fuel, it seems reasonable to assume that the fuel is the working fluid.

The analytical technique for evaluating the work energy by isentropic expansion of the fuel vapor products is discussed in Appendix B.

C.1.4 Work of Coolant Expansion

With sodium in the core during the excursion, it seems reasonable to assume that extremely rapid transfer of energy from the molten fuel to the sodium will occur. Since the efficiency of converting energy into work is higher with expansion of sodium vapor than with expansion of the fuel, this becomes a major area of concern for the sodium-in cases. When molten fuel comes into contact with the sodium coolant, the severity of the accident is determined by such factors as:

- Relative amounts of sodium and molten fuel,
- Initial temperatures of sodium and molten fuel,
- Rate at which molten fuel comes into contact with sodium,
- Size of molten fuel particles,
- Mechanism of heat transfer between molten fuel particles and sodium,
- Resistance to expansion by liquid inertia and friction.

Clearly, a detailed transient analysis would be required to fully characterize the phenomenon.

An upper limit to the amount of work energy which is generated by a molten fuel-sodium incident can be calculated using a thermodynamic approach. It is assumed that the molten fuel mixes instantaneously with the sodium in the core just at the time of disassembly, and also that thermodynamic equilibrium is established instantaneously. For the cases under consideration, the calculated equilibrium temperature is above the critical temperature of sodium.

The sodium is then expanded to a pressure of 1 atm in a reversible process. Two possible paths for the expansion can be postulated: with or without further heat transfer from the molten fuel. If there is no heat transfer from the molten fuel, the sodium undergoes isentropic expansion. If further heat transfer between the sodium and the fuel is considered, the process must be considered in three steps: heat transfer from the molten fuel, heat transfer during solidification of the fuel, and heat transfer from the solid fuel.

The processes are best illustrated with a thermodynamic diagram. Let Figure C.1 be the temperature-entropy diagram for sodium. It is assumed that the entropy of the subcooled sodium before coming into contact with the molten fuel is approximately equal to that of the saturated liquid at the same temperature, T_1 . When the sodium is mixed with the molten fuel, it is heated along the saturated liquid line to the critical point and then to some temperature T_2 and pressure P_2 above the critical point.

For isentropic expansion of the sodium, the sodium goes from P_2, T_2 to P_3, T_3 at constant entropy. If the final pressure P_3 is low enough, liquefaction will occur. The work done for an isentropic flow expansion process is equal to the enthalpy difference ($H_2 - H_3$) and can be obtained directly from the enthalpy-entropy (Mollier) diagram.

FIGURE C.1. Temperature-Entropy Diagram for Sodium

The expansion process has been divided into three phases when heat transfer between the sodium and the fuel is considered. In the first phase, the sodium expands to a pressure below the critical pressure (at least for the cases considered here) and a temperature corresponding to the melting point of the fuel (which is higher than the critical temperature for sodium). The second phase consists of an isothermal expansion while the molten fuel solidifies and releases its heat of fusion. The third phase of the expansion to the reference pressure P_3 is similar to the first phase. The work done for each phase of the flow expansion process is equal to the difference between the heat added to the sodium by the fuel and the change in enthalpy of the sodium. As shown in Figure C.1, this process does not result in liquefaction. It may be reasonable to replace the last two phases by an isentropic expansion due to vapor blanketing of the fuel particles.

The method of Hicks and Menzies⁽⁴⁶⁾ was also carried out for comparison. They considered an adiabatic expansion of the sodium-fuel mixture, which corresponds to the expansion of sodium with perfect heat transfer from the fuel. Hicks and Menzies assumed:

- Constant physical properties for the sodium and fuel
- No phase change for the fuel
- The sodium is still liquid at the equilibrium temperature of the sodium-fuel mixture.

Therefore, their expansion was carried out within the two-phase region until all of the liquid was vaporized.

Table C.I shows the typical parameters used in the analysis for the work energy with the sodium-in cases as reported in Section 6.3. The mass of sodium to mass of fuel ratio was based on a fuel pin diameter of 0.250-in., a cladding thickness of 0.016-in., a fuel smear density of 88%, and a pin pitch-to-diameter ratio of 1.12. For the

method of Hicks and Menzies, the same sodium and fuel physical properties as in their article were used. In the present analysis, the properties for sodium were taken from Tokar and Golden.⁽⁴⁷⁾ The law of corresponding states⁽³³⁾ was used to estimate the thermodynamic properties beyond the range of the data of Tokar and Golden.⁽⁴⁷⁾ This law implies that substances are characterized by their thermodynamic critical properties, and that substances at the same reduced temperature and pressure will have similar behavior. The critical constants assumed for sodium were those of Miller.⁽³⁵⁾

TABLE C.I. *Typical Parameters for Evaluation of Work Energy by Sodium Expansion*

Initial Molten Fuel Temperature, °K	4000
Initial Sodium Temperature T_1 , °K	672
Fuel Melting Point, °K	3000
Total Weight of Fuel, g	3.8×10^6
Ratio, Mass of Sodium to Mass of Fuel	0.051
Fuel Heat Capacity, joules/g - °C	0.405(solid) 0.420(liquid)
Fuel Heat of Fusion, joules/g	280

The results of the analysis are given in Table C.II. If the initial temperature of the molten fuel is 4850 °K instead of 4000 °K, then the work for the isentropic expansion increases from 541 to 702 MW-sec. For thermal equilibrium with the fuel, the first phase of the expansion reduces the pressure from 616 atm to 44 atm. The second phase results in solidifying only 73% of the molten fuel when the final pressure of 1 atm is attained. If it is assumed that no further heat transfer between the sodium and the fuel occurs after the first phase, then an isentropic instead of an isothermal expansion reduces the total work from 1288 to 978 MW-sec. The details of the calculations are presented in Appendix D.

TABLE C.II. *Comparative Results: Sodium Expansion Analysis*

	<u>Isentropic Expansion</u>	<u>Non-Adiabatic Expansion</u>	<u>Hicks and Menzies</u>
Equilibrium Temperature, °K	3428	3428	3440
Equilibrium Pressure, atm	616	616	1008
Work of Expansion to 1 atm,	541	1288	1513

C.2 Experimental Bases for Work Energy Evaluation

The experimental evidence for determining the distribution of energy from a reactor explosion can be obtained from two sources. First, there are reactor excursions, both deliberate and accidental, which provide information. Second, an extensive series of tests with TNT-type explosive in-scale models of reactor containment structures have provided information about the maximum shock deformations that can occur and maximum shock-induced vessel head loadings. In addition, the weight of TNT that will be contained in cylinders has been established.

C.2.1 Reactor Excursions

(a) SL-1 Reactor.⁽⁴⁵⁾ Post-accident investigation of the SL-1 accident provided information for both vessel wall damage from the pressure excursion and vessel head damage from impact of the water slug. The total energy generated in the excursion was approximately 130 MW-sec with 50 to 60 MW-sec released promptly enough to be responsible for vessel deformations. These deformations appeared to be caused by gas bubble effects rather than a TNT-type shock effect.

The gas bubble (steam) appears to have been generated slowly enough that a TNT-type shock wave did not occur. This bubble deformed the core and the vessel locally about the core and accelerated a coolant slug toward the vessel cover. The impact of this slug caused a bulge just below the cover and lifted the vessel with an estimated initial velocity of 29 ft/sec.

The total mechanical damage accounted for about 12% of the prompt energy release (less than 5% of the total energy generated). The lower vessel near the core region absorbed 5% of the prompt energy release while the remaining 7% was absorbed in the upper vessel and head as the result of the water slug impact.

An accident model which reflects the reactor operating temperature was proposed by J. F. Proctor⁽⁴⁵⁾ to explain the damage phenomena at the SL-1. Summarily, this model consists of the following steps:

- (1) Convert the prompt energy release to equivalent mass of TNT.
- (2) Convert TNT to its gaseous products at initial volume and reactor coolant temperature; neglect the TNT shock wave.
- (3) Expand the gaseous products isothermally by deforming the local vessel wall until pressure in the gas bubble is in equilibrium with pressure retaining capability of the wall.
- (4) Continue the expansion process isothermally by accelerating the coolant slug above the core toward the vessel cover (the slug impacts the cover with kinetic energy equal to work of expansion).
- (5) Evaluate the momentum imparted by inelastic impact upon the reactor vessel.

While this model disregards the actual driving forces of a reactor explosion, it is simple to apply and provides blast energy estimates that closely correlate to those measured.

(b) Other Reactor Excursions. A number of thermal reactors have been intentionally subjected to power excursions up to core disassembly. Each reactor generated core

pressures characteristic of its particular core composition, and little, if any, correlation can be obtained to fast sodium cooled reactors. A tabulation of the disassembly mechanism for each is shown in Table C.III. Estimates of the mechanical work efficiency for KIWI-TNT and SNAPTRAN-2 are 2% and 1 to 1 1/2%, respectively. However, these were based upon the total energy of the excursion from initiation of the transient.

TABLE C.III. *Disassembly Mechanisms for Thermal Reactor Destruct Tests*

<u>Reactor</u>	<u>Disassembly Mechanism</u>
KIWI-TNT ⁽⁴⁸⁾	Vaporization of the graphite matrix
SNAPTRAN-2 ⁽⁴⁹⁾	Hydrogen liberation from Zr-hydride-uranium fuel
SPERT-1 ⁽⁵⁰⁾	Superheat and vaporization of trapped coolant

C.2.2 Chemical Explosion Tests

TNT-type explosives have been used extensively in model tests because of convenience in obtaining concentrated energy releases that were repeatable and subject to scaling laws. Reactor model tests have been performed along with another series of tests to establish a set of general containment laws for cylindrical vessels.

The reactor model experiments have been performed by both the U. S. Naval Ordnance Laboratory and U. K. Atomic Energy Authority.

(a) United States Tests.⁽⁵¹⁾ Simulated Enrico Fermi reactor tests were performed by the U. S. Naval Ordnance Laboratory in which TNT was exploded at the core location to evaluate the structural response and possible jump of the top shield plug. The model tests simulated an explosion of 1000 lb of TNT in the full size reactor and tests were performed in 1/15 and 1/30 scale models.

The series of tests showed that the maximum height to which the shield plug could be driven by 1000 lb of TNT in the Enrico Fermi reactor was 102 ft. This represented 1.6% of the total energy released. The vessel deformation found about the core region provided a pressure reducing expansion volume which reduced plug jump. The results also indicated that blast pressures are the primary mechanism for plug jump in the FERMI reactor and not the shock wave. An analytical model was formulated to predict the height of the vessel plug jump which is similar to the model proposed to explain the SL-1 accident.

The U. S. Naval Ordnance Laboratory also performed a test series with cylinders to establish semi-empirical laws⁽⁵²⁾ for the weight of explosives that could be contained without rupture. Cylinder material, diameter, and wall thickness were varied in addition to the explosive charge weight during the test series. The containment equations were formulated from material law assumptions and fit the experimental data within an average error of $\pm 6\%$.

These equations should provide safe upper bounds for containment potential of a single walled cylinder.

(b) U. K. Atomic Energy Authority Tests.⁽⁵³⁾ The British have performed a test series with a reactor model consisting of an overstrong cylinder 24 in. in diam and 24 in. deep. Pressure cells were located in the vessel wall and head structure so peak pressures could be measured and the impulse calculated. The coolant level was varied from no air gap between the coolant and vessel cover to an 8.3 in. gap. The plug received a maximum energy of 2.1% of the charge energy when there was no air gap. Increasing the air gap to 1.1 in. reduced the plug ejection energy to 1.4% of the charge energy. Larger air gaps further decreased the plug kinetic energy.

The primary mechanism for the pressure loading appeared to be the impact of the coolant as it was thrown upward by the shock reflection at the coolant-air interface.

C.3 Work Energy Due to Molten Fuel-Sodium Interaction

In calculating the work which can be obtained by expansion of the sodium in a molten fuel-sodium accident, there are uncertainties both in the analytical assumptions and in the parameters used in the analysis. The more significant analytical assumptions are:

- Heat transfer from the molten fuel to the sodium during the expansion. It is seen that allowing heat transfer increases the work by almost a factor of 2 1/2 over an isentropic expansion of sodium. It can be argued that at some point during the expansion, the fuel particles will be blanketed by a large volume of sodium vapor resulting in poor heat transfer between the fuel and the sodium. However, it is difficult to determine the point beyond which heat transfer need not be considered.
- Expansion to a reference pressure of 1 atm. A significant portion of the total work is done at the lower pressures. It is not unreasonable to postulate higher reference pressures when applying the energy to the vessel. This would result in a substantial reduction in the calculated work energy. For example, increasing the reference pressure to 20 atm decreases the work by ~30% for the isentropic expansion case.
- Equilibrium temperature of molten fuel-sodium mixture can exceed the critical temperature of sodium. The effect of limiting the superheating of sodium to the critical temperature has not yet been determined.

The more important parameters used in the analysis are:

- Total mass of fuel
The value of 3.8×10^6 g of fuel is an upper limit to the total mass of fuel in the core.

- Critical constants for sodium

There may be very large uncertainties in the estimated critical temperature and pressure. For example, the value of 440 atm suggested by Miller⁽³⁵⁾ for the critical pressure is beyond the range of 350 ± 70 atm given by Dillon.⁽⁵⁴⁾ The sensitivity of the calculated work energy to the critical constants for sodium has not been determined.

- Mass of sodium to mass of fuel ratio

The value used corresponds to a specific fuel pin size and spacing and will change with the fuel design. Hicks and Menzies⁽⁴⁶⁾ have shown that as the mass of sodium to mass of fuel ratio increases, the work energy first increases to a maximum and then decreases. They conclude that the initial increase is due to the formation of an increasing volume of sodium vapor, and that the latter decrease is due to the loss of energy in heating sodium which remains in liquid form. Since the present method of analysis does not assume that the sodium is liquid at the equilibrium temperature and that expansion results in vaporization of the liquid, the existence of a maximum work energy must eventually be verified. If the mass of sodium to mass of fuel ratio is increased by assuming that the fuel pin pitch-to-diameter ratio is 1.16 instead of 1.12 (0.040-in.-diam spacer instead of 0.030-in.), then the work energy due to isentropic expansion of the sodium increases by 18% from 541 MW-sec to 641 MW-sec for the same total mass of fuel.

Below are the detailed calculations for determining the work energy caused by possible molten fuel-sodium interactions:

(a) Equilibrium Temperature of Molten Fuel-Sodium Mixture. The equilibrium temperature is found from the condition:

$$\int_{T_1}^{T_2} m_{\text{Na}} C_{\text{PNa}} dT = \int_{T_f}^{T_2} m_f C_{\text{Pf}} dT \quad (1)$$

The only difficulty in using Equation (1) is in estimating the heat capacity of sodium as a function of temperature. Figure C.2 shows the data which Tokar and Golden⁽⁴⁷⁾ tabulate up to 2500 °F. Using the law of corresponding states, the heat capacity increases rapidly as the critical temperature is approached and theoretically becomes infinite at the critical point. It was decided to linearly extrapolate the data up to the critical temperature. The lower heat capacities are conservative since they result in higher equilibrium temperatures. Figure C.3 is a plot of Equation (1) for various initial fuel temperatures T_f . For the reference case of an initial fuel temperature of 4000 °K, the equilibrium temperature is 3428 °K (5710 °F).

(b) Thermodynamic Properties of Sodium at the Equilibrium Temperature. In order to uniquely determine the thermodynamic state of a pure substance, two intensive properties are required. Thus one more property in addition to the equilibrium temperature will be required to be able to determine the other thermodynamic properties necessary to evaluate the work energy. The entropy at the equilibrium temperature can be obtained by:

$$S_2 = S_1 + \int_{T_1}^{T_2} \frac{C_{\text{PNa}}}{T} dT$$

FIGURE C.2. Heat Capacity of Saturated Sodium Liquid

FIGURE C.3. Heat Gained or Lost by Sodium and Molten Fuel

For the reference case of an initial fuel temperature of 4000 °K, S_2 is found to be 1.480 Btu/lb_m-°R. The other thermodynamic properties (pressure, enthalpy, internal energy, density) can now be determined from the temperature and entropy.

The pressure is determined by a trial-and-error procedure using the law of corresponding states in the following manner:

$$(1) \text{ Assume } P_2 = P_{r2} P_c = (1.4)(440) = 616 \text{ atm}$$

$$(2) \text{ At } T_{r2} = \frac{6170}{5004} = 1.233 \text{ and } P_{r2} = 1.4,$$

$$(S_2^* - S_2) = 0.06 \text{ Btu/lb}_m\text{-}^\circ\text{R} \quad (\text{from Reference 33})$$

$$(3) \text{ Calculate } S_2^*, \text{ the entropy of ideal sodium gas at } 5710^\circ\text{F} \text{ and } 616 \text{ atm. For saturated vapor at } 500^\circ\text{F} \text{ and } 0.00000354292 \text{ atm,}$$

$$S_a = 2.77194 \text{ Btu/mole-}^\circ\text{R}$$

$$(S_a^* - S_a) \sim 0$$

Therefore,

$$S_2^* = S_a + (S_a^* - S_a) + \int_{T_a}^{T_2} \frac{C_{PNa}^*}{T} dT - R \ln\left(\frac{P_2}{P_a}\right)$$

From NASA SP-3001,⁽⁵⁵⁾ C_{PNa}^* is essentially constant at 5 Btu/mole-°F

Therefore,

$$S_2^* = 2.77194 + \frac{5}{22.991} \ln \frac{6170}{960} - \frac{1.987}{22.991} \ln \frac{616}{0.00000354292}$$

$$S_2^* = 1.54 \text{ Btu/lb}_m\text{-}^\circ\text{R}$$

(4) Calculate S_2 and compare with $1.48 \text{ Btu/lb}_m\text{-}^\circ\text{R}$

$$S_2 = S_2^* - (S_2^* - S_2) = 1.54 - 0.06 = 1.48 \text{ Btu/lb}_m\text{-}^\circ\text{R}$$

The enthalpy is determined from the reduced temperature and pressure.

$$\text{At } T_{r2} = 1.233 \text{ and } P_{r2} = 1.40,$$

$$\frac{H_2^* - H_2}{T_c} = 2.48 \text{ Btu/mole-}^\circ\text{R} \quad (\text{from Reference 33})$$

The enthalpy of ideal sodium gas at 5710°F and 616 atm is determined by starting with saturated vapor at 500°F .

$$H_2^* = H_a + (H_a^* - H_a) + \int_{T_a}^{T_2} C_{PNa}^* dT$$

$$H_a = 2204.83 \text{ Btu/lb}_m$$

$$(H_a^* - H_a) \approx 0$$

$$H_2^* = 2204.83 + \frac{5}{22.991} (6170 - 960) = 3338 \text{ Btu/lb}_m$$

Therefore,

$$\begin{aligned} H_2 &= H_2^* - \left(\frac{H_2^* - H_2}{T_c} \right) T_c = 3338 - \frac{2.48}{22.991} (5004) \\ &= 2798 \text{ Btu/lb}_m \end{aligned}$$

(c) Adiabatic Expansion of Sodium to 1 atm

$$\text{At } P_3 = 1 \text{ atm}$$

$$\text{and } S_3 = S_2 = 1.48 \text{ Btu/lb}_m\text{-}^\circ\text{R}$$

$$H_3 = 1588 \text{ Btu/lb}_m \quad (\text{from the enthalpy-entropy diagram of Tokar and Golden})$$

The flow expansion work is given by:

$$w = -\Delta H = -(1588 - 2798) = 1210 \text{ Btu/lb}_m$$

The work energy is given by:

$$w = \left\{ 1210 \frac{\text{Btu}}{\text{lb}_m} \right\} \left\{ 0.05057 \frac{\text{lb}_m \text{Na}}{\text{lb}_m \text{fuel}} \right\} \left\{ \frac{5.8 \times 10^6}{153.6} \text{lb}_m \text{fuel} \right\} \left\{ \frac{3.6 \text{ MW-sec}}{3412.2 \text{ Btu}} \right\}$$

$$w = 541 \text{ MW-sec}$$

(d) Expansion with Thermal Equilibrium with Fuel

(1) From T_2 to the fuel melting point

The amount of sensible heat given up by the molten fuel from 6170 °R to 5400 °R (3000 °K) is 77.1 Btu/lb_m fuel.

The entropy increase is given by

$$\Delta S = \int_{5400 \text{ °R}}^{6170 \text{ °R}} \frac{dQ}{T} \sim \frac{77.1 \text{ Btu/lb}_m \text{fuel}}{(5785 \text{ °R})(0.05057 \frac{\text{lb}_m \text{Na}}{\text{lb}_m \text{fuel}})}$$

$$= 0.26 \text{ Btu/lb}_m \text{-°R}$$

$$S_4 = 1.48 + 0.26 = 1.74 \text{ Btu/lb}_m \text{-°R}$$

The pressure is determined by a trial-and-error procedure similar to Section (c).

$$\text{Assume } P_4 = P_{r4}, P_c = (0.1)(440) = 44 \text{ atm}$$

$$\text{At } T_{r4} = \frac{3000}{2780} = 1.079, P_{r4} = 0.1, \text{ and } Z_c = 0.285$$

$$(S_4^* - S_4) = 0.005 \text{ Btu/lb}_m \text{-°R}$$

Calculate S_4^* starting from saturated vapor at 500 °F

$$S_4^* = 2.77194 + \frac{5}{22.991} \ln \frac{5400}{960} - \frac{1.987}{22.991} \ln \frac{44}{0.00000354292} = 1.74 \text{ Btu/lb}_m \text{-°R}$$

Calculate S_4 and compare with 1.74 Btu/lb_m-°R

$$S_4 = S_4^* - (S_4^* - S_4) = 1.74 - 0.005 \sim 1.74 \text{ Btu/lb}_m \text{-°R}$$

The enthalpy is determined from the reduced temperature and pressure.

At $T_{r4} = 1.079$, $P_{r4} = 0.1$, and $Z_c = 0.285$,

$$\frac{H_4^* - H_4}{T_c} = 0.15 \text{ Btu/mole-}^\circ\text{R}$$

Starting with saturated vapor at 500 °F,

$$H_4^* = H_a + (H_a^* - H_a) + \int_{T_a}^{T_4} C_{pNa}^* dT$$

$$H_4^* = 2204.83 + \frac{5}{22.991} (5400 - 960) = 3170 \text{ Btu/lb}_m$$

Therefore,

$$\begin{aligned} H_4 &= H_4^* - \frac{(H_4^* - H_4)T_c}{T_c} = 3170 - \frac{0.15}{22.991}(5004) \\ &= 3138 \text{ Btu/lb}_m \end{aligned}$$

The flow-expansion work for this phase is given by

$$\begin{aligned} w &= T\Delta S - \Delta H = 5785(0.26) - (3138 - 2798) \\ &= 1185 \text{ Btu/lb}_m \end{aligned}$$

This corresponds to a work energy of 530 MW-sec.

(2) Isothermal expansion of sodium during freezing of fuel

$$T_{r5} = T_{r4} = 1.079$$

$$P_{r5} = \frac{1}{440} = 0.00227$$

At this high temperature and low pressure,

$$H_5^* - H_5 \sim 0$$

$$S_5^* - S_5 \sim 0$$

The entropy and enthalpy are evaluated starting with saturated vapor at 500 °F.

$$\begin{aligned}
 S_5 \sim S_5^* &= 2.77194 + \frac{5}{22.991} \ln \frac{5400}{960} \\
 &\quad - \frac{1.987}{22.991} \ln \frac{1}{0.00000354292} \\
 &= 2.06 \text{ Btu/lb}_m \text{ } ^\circ\text{R}
 \end{aligned}$$

$$\begin{aligned}
 H_5 \sim H_5^* &= 2204.83 + \frac{5}{22.991} (5400 - 960) \\
 &= 3170 \text{ Btu/lb}_m
 \end{aligned}$$

The flow expansion work for this phase is given by

$$\begin{aligned}
 w &= T\Delta S - \Delta H = 5400 (2.06 - 1.74) - (3170 - 3138) \\
 &= 1696 \text{ Btu/lb}_m
 \end{aligned}$$

This corresponds to a work energy of 758 MW-sec.

Note that the entropy increase for total solidification of the fuel is given by

$$\Delta S = \frac{\Delta Q}{T} = \frac{120.46 \text{ Btu/lb}_m \text{ fuel}}{(5400 \text{ } ^\circ\text{R}) \left(0.05057 \frac{\text{lb}_m \text{ Na}}{\text{lb}_m \text{ fuel}} \right)} = 0.441 \text{ Btu/lb}_m \text{ } ^\circ\text{R}$$

$$\text{The fraction of fuel frozen is } \frac{(2.06 - 1.74)}{0.441} = 0.73$$

The total work energy for the two phases of the expansion is 1288 MW-sec.

APPENDIX D

SODIUM EXPULSION MECHANISM

Experimental evidence to date indicates that boiling of sodium in a single channel at low pressures is followed by rapid voiding of the channel. The rate of expulsion depends primarily on the amount of superheat attained by the liquid before flashing occurs. The liquid superheat is a complicated function of many parameters such as operating conditions, purity and dissolved gas content, heat flux, surface characteristics, and the pressure-temperature history of the system. Superheats of several hundreds of degrees Centigrade have been obtained for sodium under nonflowing conditions. It appears that actual superheats under reactor operating conditions would be relatively low. However, much more data under reactor conditions are required to obtain reliable quantitative estimates of liquid superheat.

There have been several analytical methods developed to handle the voiding of coolant from reactor channels. (26, 27, 29, 56) Two of these, VOID⁽²⁷⁾ and BLOW⁽²⁹⁾, were developed specifically for superheated liquid metals. However, these methods apply only to the situation where the coolant is heated above its boiling point by heat transferred across the outer surface of the fuel clad. For the loss of flow accidents considered in the present report, the German⁽²⁹⁾ work was used because of its simplicity and because it has at least some experimental basis.

The pressures generated in a molten fuel-sodium interaction are estimated to be one to two orders of magnitude higher than for flashing of superheated sodium. The maximum pressure which can be attained is equal to the saturation pressure corresponding to the temperature of the superheated liquid. For example, sodium at 1 atm which is

superheated to 540 °F (300 °C) above the normal boiling point would be able to generate a maximum pressure of 8.5 atm. When molten fuel is mixed with sodium, the equilibrium temperature is expected to be above 5000 °F. Such high temperatures would give pressures above the critical pressure of 440 atm. For example, the pressure corresponding to a sodium temperature of 5710 °F is found to be 616 atm. (See Appendix C.3)

These extremely high pressures would result in rapid voiding of the core. Assuming that the sodium pool level is 20 feet above the core, a pressure of 616 atm would result in an initial expulsion rate on the order of 1000 times the acceleration of gravity. If the acceleration is relatively constant, then the core would be voided in approximately 10 milliseconds.

The pessimistic assumption of an extremely high equilibrium temperature and pressure for the sodium must be verified by a detailed transient analysis. The transient pressures are determined by the difference between the rate of vapor generation and the rate at which liquid discharges from the coolant channel. The rate of vapor generation depends on the initial temperatures of the molten fuel and sodium, the relative amounts of each material, the rate at which the molten fuel enters the coolant channel, the size of the fuel particles, and the mechanism of heat transfer at the surface of fuel particles. The rate of liquid discharge depends on the liquid inertia and the pressure drop characteristics of the channel.

In any event, the determination of sodium expulsion rates involves a knowledge of many unknown parameters and much more experimental data on the interaction of molten fuel and sodium are needed to guide the analysis.

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