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FAST FLUX
TEST FACILITY
PROJECT REPORT

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PRELIMINARY SAFETY STUDIES
FOR THE
NUCLEAR PROOF TEST FACILITY

A. E. Waltar and R. A. Harris

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AEC RESEARCH &
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PRELIMINARY SAFETY STUDIES
FOR THE NPTF

by

A. E. Waltar
R. A. Harris

FFTF Reactor Physics Section
FFTF Project

November 1967

Approved by

P. L. Hofmann

PACIFIC NORTHWEST LABORATORY
RICHLAND, WASHINGTON

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PRELIMINARY SAFETY STUDIES FOR THE NPTF

I. INTRODUCTION

Two major components are currently envisioned to comprise the Fast Flux Test Facility (FFTF). The first is the Fast Test Reactor (FTR) and the other is the Nuclear Proof Test Facility (NPTF). Goals, purposes, and conceptual design have been established for the FFTF,^(1,2,3) while the design for the NPTF component is still in a very early stage. Despite the relatively small design effort which has thus far been expended on the NPTF, a good deal is known about it due to its similarity to the FTR.

1.1 Scope of Report

The present report is concerned with the analysis of possible major accidents in the NPTF. Such studies, carried out in the very early stages of the NPTF design, are important for at least two reasons:

1. If the NPTF is to be of maximum usefulness to the overall FFTF effort, it must be constructed at the earliest possible date. Hence, studies leading toward an early definition of the Design Basis Accident for the NPTF are of importance.
2. Because of the similarity between the NPTF and the FTR, the methods developed for making the NPTF safety analysis can hopefully also be applied to the safety analysis of the FTR.

It should be clearly understood, however, that the present report is indeed quite preliminary. Work on the "Preliminary Safety Analysis Report" (PSAR) is now underway. Issuance of the PSAR must, however, await the time when the design of the NPTF has been placed on a more firm basis.

The purpose of the present report is to describe the current thinking as to possible calculational models which might be employed to study the large reactor power transients which generally come under the category of "hypothetical accidents". A range of ramp rates, hopefully bounding those which could conceivably be generated, have been analyzed, under the assumption that all safety systems fail and that core meltdown and disassembly subsequently occur. No credence should be given to the postulated chain of events required for such gross transients to occur. Accordingly, no attempt has yet been made to actually define the Design Basis Accident. Indeed, other accident modes (e.g. the sodium fire) must be considered before such a definition can be made.

1.2 Plan of Attack

Since the NPTF is planned to be operated at essentially zero power (the primary heat generation coming from alpha decay and spontaneous fission of plutonium), one does not have to consider a loss-of-coolant accident in the usual power reactor sense. Indeed, if no cooling capacity is operational for a long period of time, one might conceive of eventual core melting, but such a transient would be extremely slow and the reactor would be unloaded before melting occurred.

Attention in this report is, therefore, focused entirely on power transients. Normally, a properly designed safety system will successfully

terminate any power transient of the type reasonably anticipated during the life of the reactor. Despite the redundancy which is always built into the safety system, however, one is forced to consider what might happen if a severe power transient occurred and all protective systems fail.

Because of the complex interactions and uncertainties involved under such circumstances, one cannot possibly hope to trace through the entire transient in any degree of detail. Hence, early in the evolutionary process of analyzing gross fast reactor transients, gross conservatism was used in order to be absolutely certain that the worst conceivable conditions were accounted for.^(4,5,6) Most of these early studies were characterized by a reactivity threshold model, i.e., one in which the core was assumed to be arranged in a subcritical state after which time compaction took place and exceedingly high ramp rates were thereby obtained upon reaching prompt critical. Later studies^(7,8) avoided these grossly pessimistic models by attempting to account for more reasonable ramp rates resulting from core compaction. These might be generally described as continuous slumping models.

The present analysis is guided by the same general philosophy as contained in the FARET⁽⁷⁾ and SEFOR⁽⁸⁾ studies - namely, to attempt a description of the core melting process so that reasonable (even though conservative) initial conditions can be specified for the core disassembly process. In this attempt, we have found it convenient to roughly separate the course of the accident into three phases:

1. Phase I: A combination of failures, malfunctions, or operator errors, with simultaneous loss of the protective system, will initiate a power transient severe enough to damage the core.

2. Phase II: The core will lose its structural integrity and the fuel will begin to slump into a more reactive configuration. This will cause a reactivity addition which could aggravate the accident.
3. Phase III: When the energy content of the core has risen to a sufficiently high value, pressures will be generated which will disperse the core and terminate the accident.

The contents of Sections II, III, and IV are devoted to an analysis of these three phases and the methods of interfacing them are discussed. Finally, in Section V, conclusions are drawn based on the integrated results from these three phases.

II. PHASE I: INITIATING MECHANISMS

Accident analyses during the design of a nuclear facility generally follow an iterative process. First, a failure mechanism is postulated and the results of such an accident in terms of equipment damage or personnel injury are determined. If the consequences of such an accident are too severe, the failure mechanism, if possible, is designed against. In the design of the NPTF we are presently in the process of postulating failure mechanisms for the first iteration. The choice of the failure mechanisms is complicated by the fact that the NPTF design is still in the conceptual stage. For this study, therefore, it was decided to consider only the nuclear consequences (in the form of ramp rates) of a wide range of accidents. The results of the study should then allow future failure mechanisms, as they become evident, to be analyzed fairly easily by simply determining their associated ramp rates.

In order to insure that the range of ramp rates to be considered adequately covers the values which may be of interest, some possible failure mechanisms must be postulated. Three such mechanisms were chosen for investigation. They are:

1. The faces of a split table NPTF close at maximum speed when the core is critical,*
2. A control or safety rod falls out of a critical core under the force of gravity,
3. A fuel rod is dropped into a critical core.

* A split-table NPTF design is presently not a favored configuration. However, this type of design might still be adopted.

In the cases above it is assumed that all safety and control systems are inoperable. These failure mechanisms should in no way be considered typical of those which one would have to contend with in operating the NPTF facility, since no attempt has been made to assess the probability or even possibility of such events occurring. Indeed, if the core is not a split table machine, the face closing accident is meaningless. Similarly a fuel rod drop would not result in a serious power transient if the NPTF is a split table machine since loading would occur with the faces open and the reactor far subcritical. Again, these mechanisms were only postulated in an attempt to define a suitable range of ramp rates to be considered in this study.

2.1 Face Closing Accident

The variation in k_{eff} as a function of the separation between the two halves of a split table NPTF was determined from static neutronic calculations employing a two dimensional transport theory code.⁽⁹⁾ The calculations were performed in R-Z geometry in which the central test zone was assumed to be a right cylindrical disc whose plane surfaces lie perpendicular to the axis of the core cylinder. The separation between halves was represented by inserting cylindrical voids in the middle of the test zone. This model is not correct in that an actual NPTF split table would be a vertical cylinder with the split occurring in a plane parallel with the axis of the core cylinder. Depicting this accurately would require three dimensional calculations which were not warranted in this study.

The results of these calculations are shown in Fig. 1. In order to assess the consequences of a face closing accident, the speed with which they

FIGURE 1. k Versus Separation of the Halves of a Split Table NPTF

close must be known. It was postulated that the maximum speed possible would be 0.5 cm/sec. This results in the ramp rates shown on the figure, the maximum being 1.5 \$/sec.

2.2 Control Rod Drop

The spatial distribution of the worth of control rod material (containing 50% natural $B_{10}C$) was determined from two dimensional perturbation theory.⁽¹⁰⁾ The resultant variation in k_{eff} with rod position is shown in Fig. 2. By postulating that the rod falls under the force of gravity, the k_{eff} vs time traces, as a function of the initial rod position, shown in Fig. 3 were obtained.

In order to estimate the resultant reactivity input ramps, the total rod worths must be known. It has been estimated that safety rods and control rods will be worth ~ 6.7 \$ and 3.3 \$, respectively. The resulting ramp rates for a safety rod are shown in Table I.

Table I

Safety Rod Drop

Initial Distance from Bottom of Rod to Top of Upper Reflector (X_0 , cm)	Maximum Ramp Rate (\$/sec)	Maximum Δk Attainable (\$)
80	40	3.9
100	37	5.8
120 (fully inserted rod)	29	6.7

The corresponding ramp rates for a control rod can be easily determined by dividing all of the values in Table I by two.

FIGURE 2. k as a Function of Control Rod Position

FIGURE 3. k Versus Time for Control Rods Falling Out of the Core

2.3 Fuel Rod Drop

Two dimensional perturbation theory⁽¹⁰⁾ was used to obtain the relative worth of fuel material as a function of core height. Gravity fall calculations similar to those performed for the control rods were made and the results are shown in Fig. 4. It was felt that 120 cm (~ 4 ft) was an upper limit on the clearance above the core needed for loading fuel elements.

The total fuel rod worth is, of course, dependent upon its radial position in the core. It was estimated that the maximum worth would be ~ 5 \$. This results in ramp rates of 26 \$/sec, 31 \$/sec, and 33 \$/sec for initial heights above the top of the upper reflector of 50, 90, and 120 cm, respectively. In all cases the maximum Δk attainable is 5 \$.

2.4 Range of Ramp Rates for Meltdown Calculations

In the failure mechanisms just considered, the maximum ramp rate was observed when a safety rod was allowed to fall, unrestricted, out of a just critical NPTF. Certainly all possible failure modes were not considered in these calculations; however, the ones chosen for investigation were felt to result in the worst possible power transients. Thus, the range of ramp rates to be considered in the meltdown calculations was chosen to be from 1 \$/sec to 40 \$/sec.

FIGURE 4. *k* versus Time for Fuel Rods Falling into the Core

III. PHASE II: MELTDOWN

In view of the possible initiating mechanisms just discussed, it is felt that gross power transients can be adequately spanned by analyzing a range of ramp rates up to 40 \$/sec. The present study conservatively assumes these ramp rates to be unbounded. Actually, the most reactive safety rods and fuel subassemblies will be limited to about five or six dollars, and this limit will be considerably smaller for the bulk of the elements located nearer the core periphery.

For the low worth cases it is possible that, for reasonable Doppler coefficients, little or no core damage would occur even for total safety system failures. However, the present studies show that core melting can occur after three or four dollars have been inserted, so the unbounded approach used thus far is felt to be meaningful for this stage of the analysis.

3.1 Description of the Meltdown Code

The main purpose of attempting an analysis of the core meltdown pattern, within the context of studies of this nature, is to provide a reasonable description of the core conditions just prior to disassembly. This is important, since the total energy release calculated by a Bethe-Tait type analysis^(11,12) is very sensitive to the effective ramp rate existing at the start of the disassembly process. Unfortunately, a core meltdown involves complex interactions among numerous phenomena and a detailed description, at this point in time, is indeed impossible. Even if one accurately knew the power and temperature distribution throughout the core as a function of time,

there is presently no way of knowing, for example, at just what time the sodium will boil and, in fact, even the direction in which fuel might move. On the other hand, it is important to have some understanding of the overall type of power and temperature distributions to be expected since such knowledge can certainly define limits as to when fuel melting can start. Further, once it has been decided how fuel melting and consequent slumping might occur, it is necessary to feed the reactivity changes due to fuel collapse back into the equations describing the core dynamics. Clearly, any feedback due to core collapse will influence the melting pattern and, hence, the future feedback due to collapse.

Persiani et al.⁽⁷⁾ developed a meltdown code capable of a rather detailed description of one channel in the reactor. However, both the feedback due to core collapse and that due to the Doppler effect were obtained from predetermined curves, neither of which are truly characteristic of actual dynamic conditions unless the radial temperature distribution is flat. Meyer et al.⁽⁸⁾ followed selected transients up to molten conditions by use of a two-dimensional coupled heat transfer kinetics code and then performed a series of static neutronics calculations for various collapse configurations to determine a ramp rate due to core collapse. They did not actually couple the collapse to the dynamics of the meltdown process.

We have chosen to develop a meltdown code, currently designated as MELT-I, capable of estimating the power and temperature distribution throughout the reactor and, further, to couple the collapse feedback directly to the core dynamics equations. The model 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 8 radial nodes, and the clad and coolant are each assigned a single node. After melting occurs, fuel segment temperatures remain fixed at their 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.

Throughout the analysis, it has been assumed that gross fuel movement cannot occur until clad failure occurs. Presently, the code assumes clad failure whenever any one of the following four mechanisms occur:

1. The clad reaches its normal melting point;
2. Fuel in the outer node adjacent to the clad melts,
(and, hence, comes into direct contact with the clad);
3. Pressure in the center of the fuel pin, either due to
fission gases or fuel vapor pressure, becomes large
enough to rupture the clad; and
4. The coolant on the outer side of the clad reaches its
vaporization point.

Once the clad has failed, all fuel 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 that point, however, local clad failure must occur before the fuel is allowed to slump. Whenever fuel motion takes place, the reactivity feedback due to collapse is determined by the total displacement down a worth curve. Since

the worth curves (input) are 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 significant fuel movement has occurred.

To accurately obtain 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, further, would require large amounts of computer time. Hence, the method used in MELT-I is to compute the nodal temperatures accurately at only the average point in the reactor by the normal K-III method⁽¹³⁾ 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. This assumption of space-time separability, which further requires assuming a constant thermal conductivity, constitutes the major limitation of the code.

Because of this simplified method for computing core temperatures, several comparison runs were made with the NUTIGER coupled heat transfer kinetics code.⁽¹⁴⁾ 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 calculation desired, but it is ideal for providing "spot checks" for the MELT-I code up to the time of melting.

To carry out such a spot check, the base case considered was a 4 \$/sec ramp inserted into an unzoned NPTF originally at room temperature. A Doppler

coefficient of the form $A = T \frac{dk_D}{dT} = -.004$ was selected for this calculation. Using an axial mesh of 18 nodes and a fuel radial mesh of 7 nodes, the temperature buildup in the average radial channel was investigated. Fig. 5 shows the temperature comparisons in the axial center of the core for the center and outer fuel nodes and for the clad and coolant. The solid lines (MELT-I) all start at higher initial values than the dashed lines (NUTIGER) because of the projection scheme used. The important thing to notice, however, is that the time-to-melt is quite accurately predicted by the MELT-I code. Fig. 6 shows the axial center fuel centerline temperature comparison for three radial channels in the core. (It might be noted that the NUTIGER results required three separate runs - the peak and outer channel runs being made by utilization of the power trace generated by the average channel run - whereas the MELT-I results shown, as well as results for seven other channels not shown, were obtained in a single run.) For a normalized temperature of 1.00 at the average radial channel, the peak is assumed to be 1.44 and the outer edge minimum is 0.72 (as shown by the insert in the figure). Again, MELT-I temperatures early in time are grossly in error, but the comparisons near the melting time are quite good. Similar comparisons were made for ramp rates of 2 \$/sec and 20 \$/sec, and, again, the time-to-melt was predicted by the MELT-I code quite accurately.

3.2 Meltdown Analysis for Zoned Core

Initiating ramp rates from 1 \$/sec to 40 \$/sec have been investigated for the zoned core NPTF. Table II contains the core constants used in the calculations and Table III lists the relevant fuel pin parameters.

FIGURE 5. Comparison of MELT-I with NUTIGER for Temperatures at the Axial Center of an Average Radial Channel Fuel Pin (Ramp Rate = 4 $^{\circ}$ /sec, Doppler = -0.004)

FIGURE 6. Comparison of MELT-I with NUTIGER for Centerline Fuel Temperatures at the Axial Center of Selected Radial Channels (Ramp Rate = 4 \$/sec, Doppler = -0.004)

Table II

Core Parameters Used in 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 Pu ²³⁹ Inventory	=	600 kg
Neutron Lifetime	=	4.0×10^{-7} sec
Initial Average Power	=	.004375 watts/cm ³ fuel (~ 2.5 Kw total)
Initial Average Core Temperature	=	293 °K
Radial Peak-to-Average Power	=	1.15
Axial Peak-to-Average Power	=	1.25

Table III

Fuel Pin Parameters Used in Meltdown Phase

	<u>Fuel</u>	<u>Clad</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

Reduction from three to two dimensional geometry is somewhat obscure in the NPTF because of the split core design. Hence, the worth and power curves for the radial direction, obtained by rough weighting techniques, are approximations at best. Volumes for the test zone and the inner and outer fuel zones likewise do not correspond to actual values due to the problems outlined in going to 2D geometry. The relatively cold test zone, for example, was greatly reduced in volume since a more realistic figure in excess of 100 liters or so would cause an inordinate degree of power and worth depression in the radial direction. Axial power and worth curves are believed to be more accurate - the axial power (and temperature) following a chopped cosine distribution due to such low heat generation and zero sodium flow. As can be seen from the power and worth curves used, namely Figs. 7, 8, and 9, the core was divided into 10 radial channels and 18 axial segments for this study. The fuel pin was divided into 7 fuel nodes, plus a node each for the clad and the coolant.

An important item to consider in developing a meltdown analysis is the decision of when to transfer to a disassembly code. Persiani et al.⁽⁷⁾ chose to wait until enough energy had been generated in the core to vaporize all the fuel (which turned out to correspond to a roughly 90% molten core). Meyer et al. took the peak ramp rate calculated from static collapse considerations and then used an arbitrary initial power. Possibly a more realistic way to merge the two phases is to continue the melting program until core temperatures are sufficiently high to produce a significant amount of vapor pressure. The initial conditions for the disassembly phase are thereby completely specified. By iteratively starting the disassembly code at various times after melting starts, the net reactivity transition can be made quite smooth. This is the approach used in this study.

FIGURE 7. Radial Fuel Worth Employed in the Meltdown Calculations

FIGURE 8. Axial Fuel Worth Employed in the Meltdown Calculations

FIGURE 9. *Relative Radial Power and Temperature Distribution Assumed for the Meltdown Calculations*

By way of illustration, Fig. 10 indicates the reactivity components which are effective throughout the transient caused by an unbounded initiating ramp of 4 $\$/\text{sec}$ (Doppler coefficient = $-.004$). Note that the negative Doppler term becomes effective in holding the net reactivity to slightly less than one dollar after prompt critical is reached. At about 0.85 seconds, some melting in the center of the hottest fuel pin begins and at 0.940 seconds, melting begins in the outer fuel node (adjacent to the clad) in the hottest fuel pin. The latter event is assumed to cause local clad failure and the entire fuel column above that point is allowed to fall freely under gravity. Radial and axial propagation of fuel melting causes further collapse, and the net feedback due to collapse is seen to quickly approach a positive ramp rate substantially larger than the initially applied ramp rate. Since perturbation theory is used to determine the collapse feedback, once a substantial amount of fuel has slumped, the model is no longer valid. Hence, beyond about 1.00 sec, Fig. 10 is totally invalid. Fig. 11 is an enlargement of the collapse feedback during the time of interest. By taking the slope of this curve at various times after collapse begins (the number in paranthesis indicating this time in msec), one can determine the positive ramp rate due to core collapse as a function of time. The numbers shown are given at 5 msec intervals.

Also shown on Fig. 11 is an indication of peak fuel centerline temperatures in the reactor. Prior to the time when any fuel is above about 3500 $^{\circ}\text{K}$, fuel vapor pressure is so low that an additional reactivity term (negative) due to core disassembly is not possible. After that point, however, such a feedback term should be included. Initial analysis indicates that the

FIGURE 10. Reactivity Components Associated with a 4 \$/sec Initiating Ramp Through the Meltdown Phase (Doppler = -0.004)

FIGURE 11. Reactivity Feedback Due to Collapse for a 4 \$/sec Initiating Ramp (Doppler = -0.004)

disassembly term becomes important once peak fuel temperatures are in the vicinity of 4000°K . Hence, the question of when to transfer from the melting phase to the explosion phase of the analysis can be somewhat bracketed by starting the disassembly phase within the time interval corresponding to a peak fuel temperature between, say 3500°K and 6000°K . For the $4\text{ }^{\circ}\text{K}/\text{sec}$ case shown, the feedback term due to collapse in this temperature interval can be closely approximated by a ramp of about $23\text{ }^{\circ}\text{K}/\text{sec}$. It is therefore concluded that the total ramp rate at the start of disassembly is not too sensitive to the exact transfer time. It is quite important, however, to properly match core temperatures in going to the disassembly code because the Doppler term is so large. Hence, a new disassembly code, capable of allowing for the heat of fusion in the fuel, was developed. This code is described in the next section.

Fig. 12 is a pictorial representation of the core during the melting phase. 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 of 3000°K). Times given correspond to those enclosed in parenthesis in Fig. 11, i.e., they are the times after melting and collapse begin. The two hot spots on either side of the core center correspond to the power peaks of Fig. 9.

The area within the single crosshatched boundary (starting at $t = 20\text{ msec}$) indicates the portion of the core where fuel temperatures in excess of 4000°K exist. Finally, the double crosshatched area represents the core region which contains fuel temperatures in excess of 5000°K . As discussed earlier, the

External Ramp Rate = 4 \$/sec
Doppler = -0.004

FIGURE 12. Melting Pattern in Zoned NPTF for an Initiating Ramp Rate of 4 \$/sec (Doppler = -0.004)

negative feedback due to core disassembly begins to overwhelm all other feedback effects when fuel temperatures exceed approximately 4000 °K so that the core pictures beyond $t = 25$ msec are certainly not valid. The core will have disassembled prior to these times.

Before presenting and discussing the results for the other cases considered, it may be worthwhile to comment briefly on a few aspects of the collapse model, namely, the effects of 1) viscosity, 2) radial power distribution, and 3) axial segmentation.

First, as mentioned earlier, a viscosity term was included in the equation of motion, but for all reasonable values considered, the effect was quite small. Certainly the value used for the forcing function itself is a much more sensitive parameter.

Second, since the radial power distribution of Fig. 9 is rather unconventional, the 4 \$/sec case was reconsidered using a flat radial power temperature profile. As shown in Fig. 13, the flat profile causes a larger collapse reactivity feedback than does the actual profile since more of the core is involved once melting starts. If a peak core temperature of 5000 °K is assumed to correspond to reactor conditions characteristic of an explosive core, it is seen that the ramp rate due to collapse is about 32 \$/sec for the flat profile whereas it is only 27 \$/sec for the zoned profile. If a peak of 4000 °K is assumed for disassembly, the ramp rate for the two cases is comparable. Also shown on the figure is a worth curve computed from a two dimensional transport code,⁽⁹⁾ where the fuel was allowed to compact statically to a region slightly below the core midplane. This is almost precisely the condition depicted by the MELT-I code using a flat power distribution and, in fact, the two upper curves are seen to compare very well

FIGURE 13. Comparison of Transport Theory Calculations Versus Perturbation Theory for Reactor Collapse Model

for early times. As expected, the curves then separate as fuel in the meltdown model below the midplane begins to melt and fall down the worth curve.

Third, even though the meltdown code allows axial segmentation of up to 40 nodes, it is reasonable to ask if such detail is necessary, in lieu of machine time considerations. Hence, for the 4 \$/sec case, three degrees of segmentation were investigated, namely 9, 18, and 40 segments. In observing the results, shown in Fig. 14, it was concluded that an axial division of 9 segments is too coarse although sufficient accuracy can be obtained by doubling the number to 18. Hence, all other runs in this report were made using an axial division of 18 segments.

One further parameter which is of paramount importance is the Doppler coefficient. Since the NPTF is a zero power reactor, the sodium will normally be frozen and in a fast power transient it might reasonably be assumed that the sodium will remain in the core. Hence, most of the meltdown analyses have assumed a Na-in Doppler coefficient of $-.004$. It is certainly possible, however, especially for the "slow" transients, that the sodium could flash - in which case the Doppler coefficient would drop to approximately $-.002$. Changes in the Doppler coefficient during the melting process turn out to be quite interesting and, as such, will be discussed separately in Section 3.3.

Assuming, therefore, a base case Doppler coefficient of $-.004$, Fig. 15 shows the effective total ramp rate at the time of disassembly as a function of the initiating ramp rate. The results clearly indicate that core collapse can aggravate the original power transient, especially for the low ramp rates. The low initiating ramp rates are more pronouncedly affected by core collapse

FIGURE 14. Sensitivity of Collapse Feedback to Axial Segmentation

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FIGURE 15. Total Disassembly Ramp Rate as a Function of Initiating Ramp Rate

than are the fast rates since the relatively slow power rise associated with the low ramps allows more time for the initial molten segments of the core to fall. Actually, as shown in Fig. 16, more of the core is molten at the time of disassembly for the high initial ramp cases, but the transient in those cases is so fast that collapse is less significant. As the initiating ramps become very large, one would expect the reactivity contribution due to collapse to become correspondingly smaller. This trend is clearly indicated in Fig. 15.

It should be pointed out, however, that much more time is available for remedial action in the slow transients than for the faster ones. As shown in Fig. 17, the time-to-failure of the clad is nearly 4 seconds for the 1 \$/sec case, whereas it is less than 0.1 seconds for the 40 \$/sec case. Nonetheless, if all safety systems fail, the present results show that the effective ramp rate appropriate for use at the start of the disassembly code is not too sensitive to the initiating ramp rate.

3.3 Doppler Effects During Melting

A large, prompt negative Doppler coefficient is almost universally considered to be a desirable reactor design goal from the standpoint of safety. Should the complete failure of the safety system ever allow the core to melt, however, it is possible that core compaction could be aggravated by the presence of a large negative Doppler coefficient.⁽⁷⁾ Such a phenomenon arises from the fact that a large Doppler will limit the power rise, relative to the low Doppler case, allowing the melted portions of the core more time to collapse before disassembly pressures are generated. Fig. 18 illustrates this

FIGURE 16. Core Melting Propagation as a Function of Initiating Ramp Rate

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FIGURE 17. Time-to-Failure of Clad Versus Initiating Ramp Rate (Doppler = -0.004)

effect for a 4 \$/sec initiating ramp rate. In all calculations the Doppler feedback terms were assumed to be of the form $A = T \frac{dk}{dT} D$ where A is the Doppler coefficient. No other feedback terms (e.g., expansion coefficients) were included at any point of the transient.

Although the curve in Fig. 18 is smooth over the range of interest for oxide cores, an interesting variation occurs for very small Doppler coefficients. For zero Doppler, the power curve is rising very fast at the time the core begins melting and disassembly pressures (associated with high fuel temperatures) are attained so quickly that essentially no time is available for core collapse. Hence, as shown on the figure, the accident is not appreciably aggravated by core collapse. As the Doppler becomes finite, the power curve is turned over momentarily until it again rises due to the continual insertion of the initiating ramp rate. This momentary power depression, even for very low Doppler coefficients, is sufficient to allow some time for collapse and, in fact, the reactivity feedback due to fuel collapse increases very rapidly with the Doppler coefficient in this region. For a certain critical region, melting begins when the power trace is decreasing and sufficient energy to generate disassembly pressures does not build up until well after the power begins to climb again. Hence, a relatively long time is allowed for collapse and, as seen for this case of $-.0003$, the collapse feedback peaks. For yet a slightly stronger Doppler coefficient, melting does not even begin during the first power pulse. Rather, melting is delayed until the power trace is rapidly climbing on the second pulse so that little time lapse occurs between the start of melting and the generation of disassembly pressures. Hence, as seen in the illustration, the feedback due to collapse

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FIGURE 18. Effect of the Doppler Coefficient on the Disassembly Ramp Rate

is low. Finally, with still larger Dopplers (corresponding to typical oxide cores), this dependence upon the fine detail of the power trace is almost entirely absent (there is possibly more variation than the figure illustrates, since no attempt was made to disclose such detail). Fig. 19, a parametric comparison of Fig. 16, shows the core melting times as a function of the Doppler coefficient. The "resonance" effect for small Doppler values again appears in this graph.

Even though the preceding discussion points out an area where large negative Doppler coefficients could be undesirable, it is only part of the overall picture. Certainly large Dopplers delay the time required to fail the clad (because of the power depression) and, thus, much more time is available for remedial action for large Doppler coefficients. This is illustrated in Fig. 20. Perhaps the most meaningful gage of the desirability of a given Doppler is the energy release obtained by carrying the postulated accident through disassembly. As will be shown in the next section, other considerations such as reactor power at the time of disassembly and the Doppler coefficient during disassembly lead ultimately to the conclusion that, in general, the presence of large negative Doppler coefficients is desirable.

FIGURE 19. Core Melting Propagation as a Function of the Doppler Coefficient

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FIGURE 20. Effect of the Doppler Coefficient on the Time-to-Beginning of Core Collapse

IV. PHASE III: DISASSEMBLY

The amount of energy released in the range of accidents just described in Section III was calculated assuming that they were terminated by dispersion of the core. The initial conditions for the disassembly phase were taken directly from the meltdown results and the transitions between phases were carried out as smoothly as possible. This required that certain parameters (i.e., Doppler coefficient and total ramp rate) be continuous through the interface. Since the core is changing rather violently during this transition, there is reason to suspect that such "continuities" are not realistic. It is also desirable to be able to estimate the effect on the final energy release of errors in calculating the values of the parameters at the interface. With this in mind, the effect on the final energy release of variations in several core parameters was obtained..

4.1 Computational Model and Method

The meltdown calculations previously discussed provide a complete description (within the accuracy of the model chosen) of the reactor core from the start of the accident through the time that significant core dispersion takes place. In order to utilize these results, a disassembly computer code was needed which allowed complete versatility in specifying the core conditions prior to disassembly. Such a code, MAX,⁽¹⁷⁾ written by W. W. Little, Jr., was used in these calculations. This code permits the initial temperature, fuel worth and power distributions to be specified independently. The core power and delayed neutron precursor densities are supplied as input. In addition, three zones with distinct material properties are possible. One of

the major benefits which was derived from using this code was the fact that the heat of fusion was taken into account. This permitted more accurate calculations of the Doppler feedback and more realistic pressure generation distributions.

The major limitation of the code is that it treats only one dimensional geometry. This required that the spatially dependent parameters supplied from the meltdown calculations be converted from cylindrical to spherical geometry. The core zones described in Section III were converted to spherical geometry by conserving volume. The oxide densities used in this calculation were 1.64, 3.29, and 3.29 kg/liter of core in the central test zone, inner fuel zone, and outer fuel zone, respectively. The MAX code assumes that all of the energy generated in the fuel remains in the fuel. To account for this difference between MAX and MELT-I, the specific heat of the fuel was increased slightly to 0.382 joules/gm °K.

Approximate power and worth curves were drawn from the corresponding curves used in the meltdown calculations (Figs. 7, 8, and 9). The results are shown in Figs. 21 and 22. The power curve was adjusted as much as feasible to duplicate the peak-to-average temperature distribution observed in MELT-I.

The primary additional material parameter needed in the disassembly phase is the equation of state of the core material. Almost all of the heat energy produced is deposited in the oxide fuel since little time is available to transfer the heat to other reactor materials. Thus, the equation of state of $\text{PuO}_2\text{-UO}_2$ is needed. Since very little reliable data is available on PuO_2 , the properties of UO_2 were used.

Even for UO_2 , however, data is scarce and discrepancies exist for the critical constants appropriate for constructing an equation of state. (18,19,20,21,22)

FIGURE 21. Power Distribution Employed in the Disassembly Phase

FIGURE 22. *Fuel Worth Distribution Employed in the Disassembly Phase*

Most previous analyses of this nature required equations of state for very high pressures, since pressures well in excess of 1000 atm are necessary to terminate accidents initiated by ramp rates of the order of hundreds of dollars per second. Our present studies, however, are intended to show that such large ramp rates are not reasonable, and the result is that pressures required for termination are only of the order of a few atmospheres. Hence, our interest is in the vapor pressure region and such schemes as the theory of corresponding states⁽²³⁾ are not required.

The equation of state for UO_2 presented by Menzies⁽²¹⁾ was used in these calculations. His values were considered reasonable since the critical constants obtained by extrapolating his vapor pressure relationship ($P_c = 2000$ atm, $V_c = 90$ cm³/mole, $T_c = 8000$ °K) are in good agreement with those obtained by Meyer and Wolfe⁽¹⁹⁾ using different methods. Menzies's values were further compared with those of Robbins⁽²²⁾ who presents what appear to be reasonable limits on the equation of state for UO_2 . The vapor pressure curves of both Menzies and Robbins are shown in Fig. 23 where the critical constants are used to designate the various pressure energy relationships (Menzies's values are curve 4). As can be seen in the figure, Menzies's equation of state lies within the bounds specified by Robbins. To further assess the adequacy of the equation of state used, the energy release values for a representative meltdown case (4 \$/sec initiating ramp with a Doppler of -.004) were calculated using each of the curves shown on Fig. 23. It was found that the energy release obtained using Menzies's equation of state was 18% lower and 29% higher than the maximum and minimum energy releases calculated using Robbins's values. It was therefore concluded that Menzies's equation of state would be adequate for the calculations in this study.

FIGURE 23. Pressure-Energy Relationships Considered for Core Disassembly

The equations of state used above were previously described as being applicable to dilute fuel systems. This requirement was made because the vapor pressure curve of a saturated liquid and vapor was used. If the fuel density is too high, the pressure-energy relationship will deviate from the vapor pressure curve. The resultant pressures rise very rapidly with energy and thus would result in lower energy releases than calculated with the vapor pressure values. The point of departure for the NPTF core was estimated from the calculation of Robbins.⁽²²⁾ If the Na is lost during the transient, sufficient volume is available to the fuel that departure is not observed until the pressure is of the order of 1000 atm (well beyond the pressure range encountered in these calculations). If, however, the Na remains in the core, departure is observed immediately upon melting. Using the Gruneisen constant as recommended by Robbins, this would result in a pressure varying linearly with energy at a slope of ~ 125 atm gm/joule or ~ 48 atm/ $^{\circ}$ K. Calculations were attempted using the "Na in" equation of state; however, the pressure gradients encountered in the MAX code were too severe and the results were meaningless. All calculations in the remainder of this section employ the "Na-out" (or vapor pressure) equation of state. This should yield conservative results.

Throughout this section, the final energy release values are quoted as "energy available for work". By this is meant the energy available to do destructive work on the containment vessel. These values were obtained by assuming that the UO_2 does not transfer any of its energy to its surroundings and expands to a pressure of one atmosphere. Under these conditions, R. A. Meyer⁽²⁴⁾ has found that the fraction of the energy generated

over melting which is available to do destructive work is a function only of the energy content of the fuel. His results were fit to an equation of the form,

$$Q_{\text{avail}}(\text{joules/gm}) = Q_{\text{gen}}^1 A + Q_{\text{gen}}^2 B + Q_{\text{gen}}^3 C.$$

A routine was added to MAX which determines the desired fraction as a function of position and integrates over volume. Subsequent calculations indicate that the volume integrated available energy fraction varies from .38 to .54 for the NPTF.

There is, however, some question as to the applicability of this method to these calculations since the pressures and corresponding energy densities used in Meyer's calculations were substantially higher than those of interest in this study. His results were, however, obtained from corresponding states calculations using critical parameters very similar to the ones assumed in this study. Further, his results were found to be insensitive to the reactor composition. In view of these latter considerations, it was felt that this calculational method could be used in this study with reasonable confidence.

4.2 Coupling Meltdown and Disassembly Phases

In transferring the accident calculation from the meltdown to the disassembly codes, care must be taken that the transfer takes place at the proper time. The transfer must be made soon enough in time so that all of the dispersion feedback is observed. Similarly, the calculation must remain in the meltdown phase as long as possible to correctly calculate the feedback due to collapse. The energy release results should be relatively insensitive

to this transfer time, since the MAX code will approximately duplicate the MELT-I results until disassembly starts. To test the sensitivity of this transfer point, the energy release from a representative accident was calculated for several transfer times. The case considered was initiated by a $4 \text{ } \$/\text{ramp rate}$, and a Doppler of $-.004$ was used in both the meltdown and disassembly phases. Table IV shows the results of these calculations.

The feedback due to disassembly appeared to be quite insensitive to the starting time. This feedback for the case starting at .9595 sec is shown on Fig. 24. It was concluded that this case was started soon enough since the feedback term has a relatively low value for several msec after transfer takes place.

In the calculations starting at .9581 and .9590 secs, the powers calculated by MAX rose significantly higher than the corresponding MELT-I power. This was not observed for the case starting at .9595 sec. Fig. 25 shows the power as a function of time in both the MAX and MELT-I codes.

The fact that the MAX code did not duplicate the MELT-I results for all starting times was attributed to the Doppler feedback. Subsequent investigation indicated that the core average temperatures calculated by MAX were significantly lower than those calculated by MELT-I for the cases starting at .9581 and .9590 secs. In joining the meltdown and disassembly phases, the temperature continuity was maintained by specifying that the core average temperatures be equal. This requirement was imposed because the Doppler feedback is based on the core average temperature. An option in MAX allows one to specify only the core average temperature. MAX then calculates the spatial distribution of the temperature using the power distribution and the

Table IV

Energy Release Available for Work for Various Transfer Times

Note: Case shown was initiated by a 4 \$/sec ramp rate with
a Doppler of $-.004$

<u>Transfer Time (sec. after start of accident)</u>	<u>Core Average Temperature ($^{\circ}$K)</u>	<u>Energy Available for Work (MW-sec)</u>
.9581	2699.7	106.8
.9590	2724.2	105.9
.9595	2767.4	97.4

FIGURE 24. Reactivity Components During the Disassembly Phase

FIGURE 25. Comparison of Power Traces in the Meltdown and Disassembly Phases

initial temperature distribution (293°K throughout in this case). Since the power shape used in MAX is only an approximation to that used in MELT-I, one cannot be assured that the peak-to-average temperatures and fraction of fuel undergoing fusion will be the same in the MELT-I and MAX calculations. Indeed, the MAX cases starting at .9581 and .9590 secs had correspondingly higher fractions of fuel undergoing fusion than the MELT-I calculation had. This accounts for the lower temperatures in MAX. The adjustment required in the MAX power shape to correct for this would result in considerably more violent power discontinuities, which were not felt to be realistic. In any event, this effect results in an error in the energy release of only $\sim 10\%$, well within the errors of the calculational models. All subsequent calculations were transferred when the core average temperature reached $\sim 2770^{\circ}\text{K}$ and the MAX and MELT-I power traces were compared to insure that the transfer times were adequate.

The reactivity curve mentioned above (Fig. 24) contains not only the feedback due to disassembly but all of the components in the net reactivity. From this it is obvious that, once disassembly starts, the accident is over in a very short time interval.

4.3 Effect of Doppler Coefficient and Ramp Rate on Energy Release

The Doppler coefficient during the disassembly phase has been shown to have a profound effect on the energy release values.⁽¹⁵⁾ The calculations in reference (15) employed the same Doppler throughout the accident calculation (from inception of the transient through disassembly). With the present method of calculation, it is possible to change the Doppler coefficient

as the accident is transferred from the meltdown to the disassembly phase. This may, in fact, be realistic, since Na boiling may occur at approximately the same time as disassembly starts, resulting in a reduction in the Doppler coefficient. To test the effect of the Doppler coefficient, the energy release in a representative case (initiated by a 4 \$/sec ramp rate with a Doppler of $-.004$) was calculated as a function of the Doppler only during the disassembly phase. The results are shown in Fig. 26. Even if the Doppler is reduced to zero at the beginning of disassembly, the resultant energy release is increased by only a factor of 3.5. For comparison purposes, the same case was calculated as a function of the Doppler coefficient in both the disassembly and meltdown phases. These results are shown in Fig. 27.

Comparing Figs. 26 and 27, it can be concluded (at least for the case considered) that maintaining a reasonable Doppler coefficient during the meltdown phase significantly reduces the final energy release values. Two competing effects, reactor power and total ramp rate, cause these results. Fig. 18 shows that as the Doppler coefficient decreases, the total ramp rate at the time of disassembly decreases. In the meltdown cases with low Doppler ($0 \rightarrow -.002$), however, the reactor power at the time of disassembly is significantly higher than the power with a Doppler of $-.004$. These high reactor powers cause increases in the energy release which far overshadow the decrease due to the lower ramp rates. The power for a meltdown Doppler coefficient of $-.008$, however, is comparable to that for a meltdown Doppler coefficient of $-.004$ and thus the increased ramp rate causes a slight increase in the energy release.

FIGURE 26. *Effect on Energy Release of the Doppler Coefficient During the Disassembly Phase*

FIGURE 27. *Effect on Energy Release of the Doppler Coefficient During Both the Disassembly and Meltdown Phases*

The energy releases shown on Fig. 27 are smooth monotonic functions of the Doppler coefficient. The results of others have shown that oscillations appear in such curves when they are examined in detail.⁽¹⁵⁾ Although it is suspected that such oscillations may, in fact, be present for our case, it was felt that the numerous calculations necessary to reveal such behavior were not warranted for this preliminary study.

As stated earlier, the ramp rates during the disassembly phase can significantly affect the final energy release values. The ramp rate at the time of disassembly was varied for our previously described base case (4 \$/sec ramp with a Doppler coefficient of $-.004$) and the results are shown in Fig. 28. From this figure it is obvious that, all other reactor parameters remaining the same, the energy release values vary markedly for changes in the ramp rate. Thus, it can be concluded that relatively accurate ramp rates are required.

The energy release values shown on this figure are not consistent with the calculational model used in this report since all of the disassembly phases were started with parameters characteristic of the 4 \$/sec ramp case (total ramp rate = 23 \$/sec at disassembly). To show again the importance of the power, etc. at the time of disassembly, several other points are shown on the graph which have initial conditions more characteristic of the total ramp rates shown. The case with a total ramp rate of 48 \$/sec was initiated by a 40 \$/sec ramp in MELT-I. The 150 \$/sec case was calculated entirely in the MAX code since, with such a large ramp rate, insufficient time would be available for collapse feedback to occur.

FIGURE 28. Effect on Energy Release of the Ramp Rate During Disassembly

4.4 Energy Release Calculations for the NPTF

The results of the meltdown calculations were used to calculate the energy release for initiating ramp rates from 1 to 40 \$/sec. Two Doppler coefficients, representing the "Na-in" and "Na-out" cores were considered. The results are shown in Fig. 29. Except at very low (< 4 \$/sec) ramp rates, it appears that the cases with a Doppler coefficient of $-.004$ yield energy releases almost exactly $1/2$ the releases with Doppler coefficients of $-.002$. The energy release curves for each Doppler coefficient exhibit a minimum at an initiating ramp rate of 4 \$/sec. This can be attributed directly to a corresponding minimum in the total ramp rate at the time of disassembly (see Fig. 15). The range in both initiating ramp rates and possible Doppler coefficients should cover the power transient accidents possible in the NPTF. If the results of Section 4.3 above hold for all ramp rates, the energy releases for cases in which the Doppler coefficients change from $-.004$ to $-.002$ (i.e., Na boiling) during the transient will be bounded above by the curve for the Doppler coefficient of $-.002$.

The pressures at the termination of these accidents are significantly lower than those encountered in most other disassembly calculations. For instance, in the 4 \$/sec ramp case with a Doppler of $-.004$, the maximum pressure is ~ 1 atmosphere. In the disassembly calculation it is assumed that pressures are continuous functions, i.e., the pressure in one pin can be felt by all adjacent pins. If it is somehow postulated that the clad remains intact, even though meltdown has occurred, this would not be true. In this instance no disassembly feedback would occur until sufficient pressures had been built up to rupture the clad. In order to estimate the effect of

FIGURE 29. *Effect on Energy Release of the Initiating Ramp Rate*

such events, a calculation was performed in MAX in which the local pressures in the core were zero until 100 atmospheres had been reached inside each pin. Since NPTF fuel will not be sintered and no fission gas plenum will be used, the fuel will only be able to expand into its own voids. Hence, the equation of state would correspond to that described in Section 4.1 as the "Na-in" case. The energy density required for 100 atms pressure would be ~ 0.8 joules/gm. After clad rupture, however, the fuel will be able to expand into the voids left by the Na (assumed to have been expelled) and the pressure at that point in the reactor would then drop to the value calculated from the "Na-out" or vapor pressure curve.

The calculation just described was performed for the case initiated by a 4 \$/sec ramp rate. The resultant energy release was only 7% higher than the corresponding energy release calculated with no clad rupture considerations. This slight increase is negligible when compared with the errors in the overall calculational model.

If fuel pins with sintered fuel or fission gas plenums (i.e., irradiated FTR elements) are placed in the NPTF, there may be enough void inside the pins that the "Na-out" or vapor pressure equation of state would be applicable. In these pins the energy density required to attain a pressure of 100 atmospheres would be quite high (~ 1250 joules/gm). Such isolated pins, or elements, however, would have a negligible effect on the energy release since they would occupy only a small fraction of the total core volume.

V. DISCUSSION AND CONCLUSIONS

As pointed out in the introduction, this report represents only a preliminary study of the NPTF safety problems and, as such, little significance should currently be attached to absolute numbers. A few general conclusions, however, can be drawn with regard to relative numbers.

First, it is quite clear that, within the context of studies of this type, a strong negative Doppler coefficient is a desirable safety feature. It was shown that a large Doppler coefficient can have an adverse effect on the feedback reactivity due to collapse, but that this phenomenon is far overshadowed by the beneficial effect of the Doppler coefficient in the disassembly phase. The primary reason that a large negative Doppler is so desirable in the NPTF is that severe transients would probably start from cold core conditions - just the point from which Doppler feedback is strongest. For transients starting with a hot core, as could occur in the FTR, the Doppler will be far less important. This general effect was shown in Section IV where the Doppler coefficient was varied only in the disassembly phase. Unfortunately, our analyses do not yield any clearcut way to establish the minimum Doppler which can be accepted for a safe design. The type of energy releases calculated thus far tend to indicate that relatively low Dopplers may be tolerable.

A second observation which can be made is that, for the model and range of initiating ramp rates considered, the effective ramp rate at the time of disassembly varies only by about a factor of two, whereas the initiating ramp rates vary by a factor of forty. Low initiating ramp rates allow

sufficient time for a relatively large degree of core collapse to occur whereas the large initiating ramps are so fast that collapse feedback does not have time to be felt. Such a behavior should somewhat ease the job of constructing a safety envelope when actually arriving at the Design Basis Accident.

Another item which is worthy of special comment is the very low termination pressures which were obtained in the disassembly phase. When terminal pressures reach the 1000 to 10,000 atmosphere range, as indeed they do for initiating ramp rates of the order of several hundred dollars per second, it is meaningful to talk about a TNT equivalence for the available energy release. Such a comparison for pressures of the order of 1 to 100 atmospheres, however, probably makes little sense. There simply is no blast problem. On the other hand, one might reasonably ask if such low terminal pressures are really possible, i.e., should structural rigidity be considered as a threshold. The brief analysis reported at the end of the preceding section was performed in an attempt to gain some feeling for this problem. Others⁽¹⁶⁾, however, have also calculated terminal pressures in the low atmosphere range and have felt such values to be reasonable.

Although the present analysis, i.e., the attempt to follow the transient all the way from inception to disassembly, is felt to be a reasonable way of attacking the problem, major assumptions had to be made in numerous places. Some discussion may, therefore, be in order as to how the choices made might affect the ultimate work energy calculations. First, it may be worthwhile to point out some of the areas where more conservative assumptions would have led to larger energy releases:

1. Different Doppler coefficients were assumed at various stages of the analysis, partly because this number cannot be calculated accurately, but mainly because the sodium may leave the reactor at some phase of the transient. No account was taken of the reactivity consequences of sodium loss. It is possible that Na flashing near the core center could introduce a positive reactivity addition which would probably aggravate the accident. Overall core Na worth, however, is expected to be small.
2. No attempt was made to treat the Na fire. This event (if possible) may turn out to impose the pressure limit for the containment.
3. The collapsing fuel was assumed to be freely responding to a driving force of 1-G. Certainly, one can talk about higher driving forces, but associated with such an assumption goes the necessity of physically describing a process whereby such forces could be obtained.
4. If, indeed, the terminal pressures attained are determined to be physically unreal, due to structural effects, etc., the calculated energy releases will increase.
5. The available work energy calculations assumed the transient to be over when the fuel "bubble" was relieved to one atmosphere. It is possible that if an adequate amount of sodium is in the vicinity, heat could yet be transferred

from the fuel to the sodium to enable the latter to become the working fluid.

On the other hand, there are also several reasons to suspect that the energy release calculations have been too conservative:

1. In all cases considered, the total kinetic energy of all the fuel segments at the conclusion of the transient was exceedingly small.
2. It is possible that the collapse model is too conservative. With all the wire wraps, etc. around the fuel pins, it is unlikely that compaction could really take place under free fall. Further, during early stages of collapse, the collapse model presently forces fuel to pile up at the worst possible place, namely, the center of the reactor.
3. It is entirely possible that sodium boiling, if it occurs, would move fuel into a less reactive configuration rather than allowing compaction as the current model assumes. Such movement would quickly terminate the transient.
4. No credit has been taken for any negative feedback term (prior to disassembly) other than the Doppler coefficient. Even though the negative axial expansion coefficient may be unreliable, it would be expected to compare favorably in magnitude with the Doppler coefficient in most cases.
5. As pointed out in Section IV, only a sodium-out equation of state was used. If the sodium were to stay in the core throughout the transient, the pressure vs energy curve

could be so steep as to preclude any significant energy generation at all.

6. No credence whatsoever has yet been established as to the initiating mechanisms discussed in Section II. Even though the ramp rates herein discussed may be considered low, from the standpoint of original DBA-type studies, they may yet be too high for conditions which could physically exist in the core.

Because of the uncertainties which still exist, it is currently difficult to conclude that any particular energy release value is reasonable. Within the context of the present models, however, an upper bound appears to be around 330 MW-sec, as calculated for an initiating ramp rate of 40 \$/sec where the Doppler coefficient was assumed to be $-.002$ throughout the transient. The terminal disassembly pressure for this case is about 14 atmospheres. Studies now in progress are intended to assess the significance of various uncertainties and, thereby, allow a greater degree of confidence to be established for the final DBA calculations.

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