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1000 MWe

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Liquid Metal Fast Breeder Reactor

SAFETY ANALYSIS STUDIES

BABCOCK & WILCOX

Accident Analysis and Safety System Design Study

Topical Report

Local Core Fault

Prepared for **ARGONNE NATIONAL LABORATORY**

United States Atomic Energy Commission Division of Technical Information

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1000-MWe LMFBR ACCIDENT ANALYSIS
AND SAFETY SYSTEM DESIGN STUDY

— Topical Report —
Local Core Faults

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1. INTRODUCTION

Babcock & Wilcox is working on the Accident Analysis and Safety System Design Study under ANL Contract No. 31-109-38-2339. Various utility companies are participating in the work on a cost-sharing basis under separate agreements with B&W. The study is being conducted primarily to gain a better understanding of how safety requirements affect the design of large, liquid-metal, breeder reactors. The objective is being pursued by analyzing accidents that determine the design basis of the protective systems and safety features and by developing conceptual designs for these protective systems and engineered safety features. The reference design of B&W's 1000-MWe Follow-On Study¹ is the basis for this study.

This report relates some of the accident analysis efforts performed under activity 221 of the subject contract. Specifically, these efforts concern the analyses of local core faults that might have reactor safety implications. Local core faults are defined as faults that originate within the physical envelope of the reactor boundaries; this includes the radial blanket assemblies.

One of the prime concerns in fast reactor safety is the possibility of propagation of fuel pin failure stemming from an initial single pin failure or other local fault. If undetected, propagation of fuel pin failure may lead to an accident of more than minor consequence. This concern for possible propagation of fuel pin failure arises from the fact that a single pin failure is highly probable; thus, an LMFBR must be capable of operating safely with a limited number of failed fuel pins without multiple interdependent failures.

The propagation of fuel pin failure and the hypothesized effects have been examined, but it is beyond the scope of this investigation to attempt to prove conclusively that this or other serious local faults cannot occur. Instead, the approach has been to evaluate the following:

1. The likelihood of faults progressing to accident conditions.
2. The possibility of detecting fault progression before accident conditions are reached.
3. The possible consequences of fault progression if this progression went undetected and led to a serious local fault on a fuel assembly level.

The investigation of local core faults has been concentrated in four general areas: First, the likely results of localized pin failure have been examined, specifically with respect to failure modes involving the release of fission gas and solid material into the coolant channel(s). Second, we have considered the effects of localized flow blockages resulting from foreign (ex-core) objects in a fuel pin bundle. A third general area is the phenomenon of molten fuel and coolant interaction (hereafter termed MFCI); under certain conditions this might result in a rapid generation of sodium vapor with potential structural damage (a so-called vapor explosion). Last, we have investigated the effects of an extensive MFCI in a single fuel assembly and the possible resultant structural damage to adjacent assemblies.

It was anticipated that further work in the general area of local core faults would be performed under the subject contract. This report describes the work and related conclusions up to this time.

2. SUMMARY OF RESULTS AND CONCLUSIONS

2.1. General

The effects of local faults within the core envelope have been studied. The scope of the study included the faults that are initiated (perhaps) by fuel pin or spacer failures or by deposition of foreign (ex-core) material within a fuel pin bundle. The scope did not include the complete or partial inlet blockage of a fuel assembly. The investigations of the effects of local faults have covered fuel pin failure, the phenomena involved in the molten fuel-sodium interaction, and the effects of such an interaction on the core structures.

2.2. Consequences of Fuel Pin Failures and Other Local Faults

2.2.1. Fission Gas Release

Results have been obtained on the thermohydrodynamic effects of transient fission gas release from a failed fuel pin in the reference core. A simple modeling of the phenomena was sufficient to show that the accompanying reduction of sodium flow in the affected channel is not severe enough to result in sodium boiling. The cladding and fuel thermal transients in the affected channels resulted in short-term cladding temperatures near 1600F but negligible increases in centerline fuel temperatures. Some probability is indicated for additional fuel pin failures for the pins surrounding the affected channel. However, even if failures did occur, no further immediate propagation of fuel pin failure is indicated because of the absence of molten fuel in these pins and their low fission gas inventories.

2.2.2. Flow Blockages

The local effects of flow blockages within a fuel pin bundle were studied. A model was developed to calculate the resultant bulk flow and temperature distributions in a pin array. The types of blockages

considered included generalized types and those corresponding to (1) inlet blockage at the pin array inlet, (2) a cylindrical foreign object within a channel, (3) severe reduction of area and hydraulic diameter over a short length of a single channel, and (4) single and multiple expanded or bowed pins. Some generalized plots were constructed for the effects within a blockage of numerous channels. These plots allow the determination of limiting blockage lengths for a given set of flow area and hydraulic diameter reductions. The calculated results, along with other considerations, indicate the following:

Blockage of a single channel is generally not detectable by outlet temperature measurement because of crossflow and turbulent mixing downstream from the blockage. These effects may mask the presence of very high sodium temperatures within and immediately downstream from a blockage. The boiling of stagnant sodium in a single blocked channel, however, is unlikely because of radial heat conduction; it is possible, though, that some fuel melting could occur within adjacent pins for a severe blockage consisting of fissile material in a single channel.

Single channel blockages of any type are not expected to lead to large amounts of superheated sodium that might flash to vapor. Even so, a modeling of the effects of localized pockets of stagnant sodium flashing to vapor has shown that the duration of the resultant fuel pin blanketing is so short that it has little influence on the rapid propagation of fuel pin failures.

For a multi-channel blockage of two to four adjacent channels, stagnant sodium is not likely to boil. Also, no large volume of high-temperature sodium would be present downstream from such a blockage because of the high values of crossflow in evidence. Again, as in the single-channel case, the magnitude of the downstream crossflows lower the blocked channel sodium temperatures at the bundle outlet, making detection by temperature measurement impractical.

A multi-channel blockage of 19 or more channels could conceivably lead to appreciable amounts of high-temperature sodium in the channels downstream from a blockage. For the cases examined, the maximum temperature occurred within the upper axial blanket. The average flow reductions within the assembly for this severe a blockage

would be difficult to detect by practical flow measurement means. A scheme utilizing measurement of temperature fluctuations at the assembly's outlet is a possibility for detecting a blockage of this extent.

The possibility of radial propagation of fuel pin failures initiated by a single coolant channel blockage appears very remote for most cases. The most severe case of single channel blockage—that of dense fissile material—appears to have some small but significant probability for involving additional channels and fuel pins; however, the probability that such a blockage would actually occur should be vanishingly small. From the blockage studies, it appears that for multiple adjacent channel blockages, the probability for radial propagation of pin failures increases with the number of blocked channels. The probability of failure propagation initiating from the first affected channel, however, should be small.

2.3. Localized Molten Fuel-Sodium Interaction Effects

A model of the molten fuel-coolant interaction processes was developed to determine the resulting pressure-time variations and available work from such an event. Unfortunately, the results obtained from the computer program developed for this purpose (NFCDYN) had not been sufficiently substantiated for publication at this time. A parametric study was planned to investigate the importance of various parameters in the interaction processes. Some limited results, however, were used in determining the work energy per unit fuel mass in a mixture of molten fuel and sodium.

2.4. Structural Effects of Localized Molten Fuel-Sodium Interaction

2.4.1. Integrity of Affected Fuel Assembly

Even though a molten fuel-sodium interaction would be quite improbable for the case of a local fault within a pin bundle, the effects of such an event on the affected fuel assembly have been examined. The amount of molten fuel required for the loss of wrapper can integrity has been examined as a function of the axial extent of deformation and the energy absorption capability of a unit length of wrapper can. Although there is considerable uncertainty in the analysis, it is reasonable to conclude that more fuel and fertile material than is contained in one

pin would be required (when in the molten state and mixed with sodium) to cause the affected wrapper can to fail. This amount pertains to the case where the wrapper can has experienced a neutron fluence of 10^{22} nvt. Under these conditions then, the loss of a wrapper can's integrity due to a localized blockage and subsequent fuel-coolant interaction would be very improbable; it is not reasonable to postulate that a very severe blockage would occupy the length of a single coolant channel (or shorter lengths of numerous adjacent channels) required to form this amount of molten fuel.

2.4.2. Effects on Surrounding Assemblies

A model was developed to evaluate the effects of a localized pressure disturbance on the fuel assemblies surrounding a breached assembly. The structural distortions and possible reactivity insertions from fuel movement were of interest. Unfortunately, the fuel-coolant interaction model MFCDYN was not sufficiently developed to provide input to this structural model. Consequently, the results obtained were limited to a study of the effects of variations in applied impulse to the affected assemblies. It was found that only a very small impulse was required to damage or significantly deform the six assemblies surrounding the disturbance if the wrapper can material was of negligible ductility. Results from the model showed, however, that reactivity insertions from radial fuel movement would be insignificant in most cases.

3. CONSEQUENCES OF FUEL PIN FAILURES AND OTHER LOCAL FAULTS

3.1. General

Several aspects of the possible consequences of a localized fault (on a single-pin or coolant-channel scale) have been examined. The primary concern is with the effects of fission gas release due to cladding failure and the effects of a localized blockage. Other effects may be postulated, but they are considered less likely than gas release or local blockage.

Local faults that occur on a single-coolant-channel scale must be considered in a safety assessment because, if undetected, these faults may lead to an accident of more than minor consequence. The possibility that fuel pin failure propagation may lead to the propagation of fuel assembly failure must be evaluated. This involves some assessment of the relative probabilities of fuel pin failure propagation by the numerous postulated propagation mechanisms.

A detailed discussion of all of the propagation mechanisms and various paths that LMFBR safety analysts have postulated in the past is beyond the scope of this report. The complexity of this subject is exemplified by the large AEC program (experimental and theoretical) that has been initiated to study failure propagation. (Refer to the discussion of Task 10-2.4 of the LMFBR Program Plan² for a description of the program dealing with fuel failure propagation. In addition, the possible propagation modes are discussed in considerable detail in another publication³ describing test facilities for an experimental program on failure propagation.)

Generally, the main considerations concerning the effects of fuel pin failure on possible failure propagation are the following:

1. Pressure pulses.
2. Sodium boiling.
3. Channel blockage.
4. Molten fuel contacting adjacent cladding.
5. Flow maldistributions (transient and long-term effects).
6. Fission gas release.

The effects depend on the mode of initial failure or the type of local fault, the mechanism of failure propagation, and the failure states (criteria) of adjacent pins. Failure propagation mechanisms may involve mechanical or hydraulic forces or thermal effects or a combination of the three.

The consequences of fuel pin failure may have significant safety import independent of fuel pin failure propagation phenomena. In other words, it may be postulated that faults originating in a single pin or coolant channel have consequences on the fuel assembly level without fault progression through the regular chain of either extensive or limited fuel pin failure propagation.

In this section, the consequences of pin failure or other local faults on possible pin failure propagation are discussed along with some aspects of the possible effects relating to single assembly voiding. Other fuel-assembly-level consequences may result directly from localized faults. The particular case of the effect of a localized MFCE on wrapper can integrity is presented in section 5.1.4.

The effects that are most likely to result from a single fuel pin failure include fission gas release and localized blockage (possible fuel pin debris). Blockage by foreign objects must also be considered and is discussed in the general subsection on blockage effects. Other effects, such as sodium boiling, are also treated in that section.

3.2. Fission Gas Release

The transient release of fission product gases from a failed fuel pin is of interest from the safety standpoint. Fission gas release is of some concern since it has been postulated that such a gas release mode might lead to additional pin failures. The postulated mechanisms of pin failure have been (1) extensive gas blanketing accompanying the

large, rapid gas releases; (2) localized gas blanketing of the cladding opposite a persistent gas jet; and (3) localized flow reduction attendant to the simultaneous flow of sodium and fission product gases for low gas release rates. Recent experimental investigations at ANL⁴ have indicated that both extensive and localized gas blanketing, as described above, are probably of little concern. However, very low gas release rates may have adverse effects.

The gas release phenomena has been examined from the standpoint of the effects of fuel pin gas inventories, cladding breach sizes, cladding breach axial locations, and the effects of the resultant flow transients on fluid, cladding, and fuel temperatures.

3.2.1. Technical Approach and Calculational Models

3.2.1.1. Fuel Pin Gas Inventories and Cladding Breach Effects

Calculations were performed to determine the effects of fission gas released from fuel pins that fail in the presence of small quantities of this gas. Although the reference core design employs a vented fuel pin, these pins will probably operate with some small positive gas pressure differential, which will be necessary to prevent sodium ingress through the porous plug during shutdown. To obtain an internal pressure of 25 psia during shutdown conditions, a fuel pin pressure of about 50 psia will be required. The pressure of 25 psia is the hydrostatic sodium pressure at the vent elevation.

It has been postulated that a porous plug vent may become blocked by the deposition of volatile fission products on the inner face of the plug or some other mechanism. A typical internal gas pressure may be determined for a blocked vent in the case where the vent flow area is reduced to 10% of normal. This pressure amounts to approximately 135 psia and is based on the assumption that the steady flow of fission gases from the vent can be characterized by the equation given below.

$$W = \rho AV = \rho A \frac{\Delta p}{K}$$

where

- W = flow rate of fission gas,
- ρ = gas density,
- A = flow area,
- V = gas velocity,
- Δp = pressure differential across the plug,
- K = a flow constant that relates pressure differential and flow velocity.

In arriving at 135 psia for an internal pin pressure with a plugged vent, it was assumed that both the normal and plugged flows are laminar; thus, K was held constant. In the plugged vent pressure calculation, credit was taken for the increased mass flow rate resulting from the increased gas density inside the pin.

We cannot now predict with certainty the character of the defects that are likely to occur upon cladding failure. Therefore, we have examined a range of defect hole sizes and assumed that all the gas present in a pin is not retarded (held up) by the fuel geometry; i. e., the gas is immediately available for release to the coolant channel. The only limits to the gas release rate have been taken to be the hole size and the internal pressure or the pressure differential across the defect.

Gas release rates as functions of time (starting with the time at which the defect forms), defect cross-sectional flow area, and internal pressure have been determined as follows: If the internal-to-external pressure ratio is greater than the critical pressure ratio that gives a Mach number of 1.0 at the defect, then the mass flow of fission gases (for the isentropic flow of a perfect gas) is given below.

$$W = A \left[g_c k \left(\frac{2}{k+1} \right)^{k+1/k-1} \frac{p_1}{v_1} \right]^{1/2}$$

where

- W = mass flow rate,
- A = defect flow area,
- g_c = gravitational conversion factor,
- k = ratio of specific heats,
- p_1 = internal pressure,
- v_1 = internal specific volume.

For xenon gas, this equation reduces to

$$W = 1.19 A \frac{P_1}{(T_1)^{1/2}},$$

where T_1 is the absolute temperature. For internal-to-external pressure ratios less than the critical pressure ratio, the flow rate is given by

$$W = A P_1 \left[\frac{2g_c k}{(k-1)RT_1} \right]^{1/2} \left[\left(\frac{p}{P_1} \right)^{2/k} - \left(\frac{p}{P_1} \right)^{(k+1)/k} \right]^{1/2},$$

where p represents external pressure and R is the gas constant. In the case of xenon gas, this equation reduces to the following:

$$W = 3.705 A P_1 \left[\left(\frac{25.0}{P_1} \right)^{1.205} - \left(\frac{25.0}{P_1} \right)^{1.602} \right]^{1/2} / T_1^{1/2}.$$

The cumulative mass of gas (M) released at any time (t) is given by

$$M = \int_0^t W dt;$$

this gas release determines the instantaneous pressure as given by

$$P_1 = (M_0 - M) \frac{P_0}{M_0}$$

for a constant internal pin volume and an isothermal gas expansion process. In the expression above, p_0 and M_0 are the initial pin pressure and the mass of fission gas.

The set of equations describing the gas release rates were solved using the MIMIC computer program; the results are shown in Figure 3-1. The mass flow rates as functions of time are shown for various initial pin pressures and various defect cross-sectional areas. The straight lines represent the envelopes of

curves for various initial pin pressures; the envelopes determine the maximum gas flow duration for the flow rate in excess of a given value (the ordinate of Figure 3-1). This envelope eliminates the necessity of considering different defect areas.

The following data are applicable to the cases shown in Figure 3-1:

<u>Pin location</u>	<u>Gas vol, ft³</u>	<u>Gas temp, F</u>
Central void	2.52×10^{-5}	3600
Unsintered fuel region	1.267×10^{-4}	2000
Blanket-porous plug gap	1.43×10^{-4}	1300
Defect	--	1000

3. 2. 1. 2. Effects of Gas Release on Sodium Coolant Flow

It has been assumed that the effects of low gas release rates on the sodium flow rate in a single channel have been adequately predicted by employing the two-phase pressure drop correlation of Lockhart-Martinelli.⁵ From this pressure drop (or pressure gradient) correlation, one can determine the degree to which fission gas mass flow rates can reduce the sodium mass flow rate for a steady-state condition.

The two-phase pressure gradient is determined by the following equation:

$$\left(\frac{\Delta P}{\Delta L}\right)_{TP} = \phi_g^2 \left(\frac{\Delta P}{\Delta L}\right)_g$$

where

$\left(\frac{\Delta P}{\Delta L}\right)_{TP}$ = the two-phase pressure gradient,

$\left(\frac{\Delta P}{\Delta L}\right)_g$ = the pressure gradient calculated for the gas flowing alone,

ϕ_g = a two-phase multiplier for the pressure gradient.

The dimensionless variable ϕ_g is related empirically to another dimensionless variable, x :

$$x^2 = \frac{\left[\left(N_{Re} \right)_{gp} \right]^m C_1 \left(W_1 \right)^2 \rho_g}{\left[\left(N_{Re} \right)_{lp} \right]^n C_g \left(W_g \right)^2 \rho_l},$$

where $\left(N_{Re} \right)_{gp}$ = the Reynolds number of the gas phase flowing alone,
 $\left(N_{Re} \right)_{lp}$ = the Reynolds number of the liquid phase flowing alone,
 C_1 and C_g = the liquid and gas phase friction factors,
 W_1 and W_g = the liquid and gas phase flow rates,
 ρ_l and ρ_g = the liquid and gas densities.

Figure 3-2 shows the pressure gradient for the simultaneous flow of sodium and xenon fission gas. Knowledge of the two-phase pressure gradient alone, as given in the figure, is not sufficient to determine the extent of coolant flow reduction in a single channel. We must know the location of the defect because the location governs the portion of the length over which the pressure losses increase. The effect of a two-phase mixture flowing along the last few inches of a typical reference core coolant channel will be less pronounced than the effect of two-phase flow along the entire channel length.

The flow of fission gas in a channel effectively removes liquid sodium from the channel because of the volume occupied by the fission gas. The volume fraction (α) of the fission gas has been taken to be

$$\alpha = 1.0 - \frac{1.0}{1.0 + (21.0/X_{tt})},$$

where X_{tt} is the two-phase flow parameter of Lockhart-Martinelli. This volume or void fraction may be related to the steady-state fractional sodium flow in a channel, since the X_{tt} parameter determines this fractional sodium flow (along with the channel pressure difference from inlet to outlet). For the reference core case, Figure 3-3 displays this relationship where uniform gas flow occurs along the entire length of the coolant channel.

3.2.1.3. Thermal Effects of Gas Release

The most desirable method of determining the thermal consequences of fission gas would include an account of (1) the fission gas escape rate, (2) the transient effects of this gas flow on the sodium flow rate, and (3) the transient effects of sodium flow reductions on the coolant, cladding, and fuel temperatures in the channel. Since no model was available with which to examine all these effects, a simplified evaluation was made.

In this evaluation, the following assumptions were made:

1. The cladding defect occurs at the channel entrance, and the sodium flow rate instantaneously falls to the steady-state level corresponding to some constant fission gas flow rate from the defect.

2. After formation of the cladding defect, the void fraction in the affected channel instantaneously assumes a value corresponding to that given by the steady flow of sodium and some constant fission gas flow rate from the defect.

3. There is no crossflow of fission gas to other adjacent coolant channels; this gives the maximum magnitude and duration of coolant flow reduction in the channel.

4. There are no transverse heat losses from the channel due to either turbulent fluid mixing or convection by crossflow.

Based on these assumptions, a number of computer calculations were made using the TART program to determine the time required to raise sodium coolant temperatures to 1700F for various levels of sodium flow and void fraction. Of course, the effect of increasing void fraction is to remove sodium from the channel and thereby decrease the capability for energy storage in the system considered (one channel and the associated pins). The results, shown in Figures 3-4 and 3-5, are represented by the "unsafe region" boundary. These boundaries in the two figures are for the hot channel and peak (nominal hot) channel, respectively. A plot on each figure shows the length of time that fission gas may flow for average gas flow rates corresponding to the fractional sodium flows shown on the abscissa. The selection of

a constant mass flow rate of gas from a pin should be conservative since this gives a larger gas flow duration at a given gas flow level than that which is calculated for the gas release model discussed in section 3.2.1.1. This is shown in Figure 3-6, which shows the envelope of curves for variable flow rate (variable with time) for all possible defect areas; the envelope of flow times for a constant flow rate is also shown for comparison. The times for constant flow are always greater than the time for variable flow at a given flow value (the ordinate of Figure 3-6).

3.2.2. Conclusions — Effects of Fission Gas Release

We are interested in determining the likelihood of serious consequences resulting from the transient release of fission gas from a failed fuel pin. Two immediate serious consequences may be postulated: sodium boiling and cladding failure of adjacent fuel pins. In the first instance, sodium boiling might conceivably lead to a transient flow excursion in the coolant channel and possible film dryout in the cladding, which would likely result in additional pin failures. In the second case, coolant and cladding thermal transients associated with the flow transient may be postulated to promote cladding failures of adjacent fuel pins; these failures may then result in additional fission gas release to the coolant channel.

Figures 3-4 and 3-5 indicate that some time margin exists before sodium boiling will occur in a channel owing to the release of fission gas from a single defective fuel pin. The calculated margin for the incidence of sodium boiling in the hot channel is rather small. The conclusion that boiling will not occur should be valid, however, since conservatism has been exercised in obtaining these results.

The second postulated consequence of fission gas release—cladding failure of adjacent fuel pins—must be deemed possible as a result of these simplified analyses. The TART runs conducted to determine the effects of a flow reduction from fission gas release indicate that the maximum cladding surface temperature for the worst case examined (the hot channel with a 0.25 coolant flow fraction and 0.65 void fraction) is approximately 1670F. It is felt that this level of cladding temperature would increase the probability of cladding failure to a level where the possibility of failures must be recognized. (It should

be evident, however, that these calculated temperatures are a result of the conservative assumptions made and would be expected to be considerably lower in an actual event.) The incidence of fuel melting was indicated for the TART runs pertaining to the moderate flow reductions and long time durations. The duration of fission gas flow and the associated flow reduction for the specific case of 135 psia pin pressure, however, indicate that no fuel melting would occur. The expected average fuel temperature and fuel surface temperature increases (axial maximum) for this specific gas release case would not exceed 200 °F and 330 °F respectively.

Even though the possibility of the cladding failures of adjacent fuel pins cannot be shown to be negligible, these failures should have no appreciable effect on further failures. This is based on two considerations. First, the probability of an adjacent fuel pin containing a large fission gas inventory as the result of a blocked gas vent should be negligibly small. Thus, failure of these adjacent pins should not contribute significant amounts of fission gas. Second, the failures of adjacent fuel pins should occur in the high-temperature region of the coolant channel. This region is above the core midplane, where it has been determined that fission gas release has a minimal effect on the coolant flow rate. Therefore, on the basis of the analytical results and qualitative considerations, we do not believe that the transient fission gas release from a defective fuel pin of the B&W reference design is a safety problem. Neither sodium boiling nor significant fuel pin failure propagation is indicated.

3.3. Local Core Flow Blockage

The B&W reference design includes almost 100,000 fuel pins, as do other typical LMFBR core designs. Each fuel pin has two distinct coolant channels, so that there are more than 100,000 coolant channels in the core. The probability that a local coolant channel blockage will occur in an operating plant, then, is not insignificant. The effects of a local coolant channel blockage may be quite serious since the specific power values are in the neighborhood of 200 W/g. Flow blockages may result in rapid temperature increases in the case of a sudden blockage, with possible sodium boiling and/or fuel melting. Blockages formed

more gradually might result in sustained high sodium coolant temperatures as well as sodium boiling, depending on the severity of the blockage.

The problem of flow blockages is of considerable interest for several reasons. The present state of the art of core protective instruments is such that nonviolent events occurring on the single-coolant-channel level are essentially undetectable. A blockage might conceivably form and lead to a serious fault on a larger scale before protective action could be initiated. Faults on a multiple-channel scale may also be included in the category of nondetectable events, depending upon the type of event.

Flow blockage is of concern in the problem of possible fuel pin failure propagation. In the past, the blockage fault has been postulated as a possible failure propagation mechanism. The question of detection plays a role in failure propagation since, as mentioned above, faults may progress to a multiple-channel scale without detection.

3.3.1. Flow Blockage Calculational Model

The calculational model developed for investigating the effects of local flow blockages (BLOCKO) is essentially a simplified approach to the fluid flow boundary-value problem. The axial and transverse fluid momentum and continuity equations for the steady state are solved in an iteration process to satisfy the boundary values of total pressure. The geometry considered is a number of parallel, interconnected channels of which some arbitrary numbers are partially blocked over some variable extent of the axial channel length. The solution of the momentum and continuity equations for a given boundary condition gives the velocity and pressure variations in all the coolant channels included within the boundary. From the flow distribution in and around the blockage, the energy equation is employed to determine the coolant temperature distributions in the channels.

The geometry of the calculational model is limited to 12 equivalent channels representing 24 actual channels (24 channels in a one-sixth segment of an axially symmetric hexagonal pin array—see Figure 3-10). Any of these 12 equivalent channels may be partially blocked up to a total of 11 blocked channels. The blockage is assumed

to be geometrically uniform in the axial direction, but the degree of blockage in each radial channel may be varied.

In order to minimize the possibility of instabilities in the iterative process of solving for axial and transverse (crossflow) velocities, some simplifying assumptions have been made. All crossflow upstream from the blockage is assumed to occur at one node in each channel. The crossflow occurring downstream from a blockage is determined along the entire channel length from the blockage to the channel outlet. The downstream crossflows are determined by both the radial pressure differences in the affected channels at the axial elevation of the blockage outlet and by a "shaping" factor which is determined by the axial pressure distributions.

The axial momentum equation employed in the calculational model is essentially identical to that used by Rowe.⁶ (See Appendix A for details on the flow blockage model.) The transverse momentum equation involves a crossflow resistance coefficient (K) defined by

$$K = 2g_c \rho c^2 \Delta p / w^2, \quad (a)$$

where

g_c = gravitational conversion factor,

ρ = fluid density,

c = pin-to-pin ligament,

Δp = difference in adjacent channel fluid pressures,

w = mass crossflow per unit channel length.

Very few experimental measurements of this resistance coefficient are available. The calculational model provides for variations in the coefficient.

3.3.2. Validity of the Model

Naturally, questions arise as to the validity of the calculational model because of the simplifying assumptions made and the use of a transverse momentum equation containing an empirical coefficient, the value of which is not known. These questions cannot be answered with complete assurance without comparing calculated results with experimental values. Certain characteristics of the calculational

model, however, tend to give credence to the model's results. Specifically, these characteristics are (1) the prediction of rapid flow restoration behind—downstream from—a blockage and (2) the relative insensitivity of crossflow values to variations in the crossflow resistance coefficient.

As mentioned in the previous section, an empirical crossflow resistance coefficient is used in the transverse momentum equation and is defined by equation a. Some experimental data, although not strictly applicable, indicate that this resistance coefficient varies as the ratio of crossflow velocity to axial velocity. Figure 3-7, from an article by Tong,⁷ shows this variation for dividing manifold flow. It has been argued by some investigators⁸ that this variation of resistance coefficient is an inertial effect that represents the larger pressure differentials required to divert a higher axial velocity stream.

Figure 3-8 shows the resulting crossflow (mass flow rate per unit length) for different values of resistance coefficients for the B&W reference core case of two adjacent coolant channels. This figure alone indicates the large crossflow magnitudes likely to occur downstream from a blockage with very nominal values of channel pressure difference even for resistance coefficients of up to 100.0. A typical LMFBR peak pin requires a coolant flow rate of approximately 1000 lb/h or 0.28 lb/s; a single coolant channel carries half this flow—0.14 lb/s. In order to restore full flow behind a single channel blockage (100% blockage) in a distance of 0.1 foot, the axially uniform crossflow of 0.45 lb/ft-s would require pressure differences of 0.02, 0.20, and 2.0 psi for resistance coefficients of 1, 10, and 100 (from Figure 3-8).

Preliminary analytical results from the flow blockage studies being conducted at ORNL⁹ indicate that the crossflow immediately downstream from a blockage is sufficient to give fully developed flow approximately 0.1 foot from the blockage. Results from the BLOCKO calculational model tend to agree with these results.

Figure 3-9 illustrates the rate at which axial flow is re-established in a partially blocked channel. The BLOCKO model results are for seven adjacent channels blocked by an effective area reduction to channel areas equal to 0.1 of the unblocked channel areas and no change

in hydraulic diameter. The blockage length was 0.1 of the channel length, or 6.5 inches. The solid lines are the results of the following equation:

$$\begin{aligned}
 \frac{\dot{m}}{\dot{m}_o} &= 1.0 - \frac{1.0}{\dot{m}_o} \int_0^x \{w\} dx \\
 &= 1.0 - \frac{1.0}{\dot{m}_o} \int_0^x \left\{ \frac{2g_c \rho c^2 \Delta p}{K} \right\}^{1/2} dx \\
 &= 1.0 - \frac{1}{\dot{m}_o} \int_0^x \left\{ \frac{2g_c \rho c^2}{K} \frac{dP}{dx} \right\}_{core} \left[x - \int_0^x \left(\frac{\dot{m}}{\dot{m}_o} \right)^2 dx \right]^{1/2} dx, \quad (3.3-1)
 \end{aligned}$$

where

$$\begin{aligned}
 \dot{m} &= \text{axial mass flow rate,} \\
 \dot{m}_o &= \text{fully re-established flow rate,} \\
 x &= \text{displacement measured upstream from} \\
 &\quad \text{point of fully re-established flow,} \\
 \left. \frac{dP}{dx} \right|_{core} &= \text{unblocked channel pressure gradient.}
 \end{aligned}$$

This equation accounts for the axial variation of the pressure in the blocked channel depending on the axial flow rate (a constant value of friction factor was assumed for the Darcy-Weisbach equation relating pressure loss to flow in a tube). The unblocked channels were assumed to maintain the normal core pressure gradient.

It is seen that fairly good agreement is obtained in comparing BLOCKO results with equation 3.3-1. Although the axial flow distributions may not agree in the immediate vicinity of the blockage, at a distance of about 0.1 to 0.2 foot, the magnitudes of the axial flows are within 10% of the values given by equation 3.3-1 for values of K equal to 1, 10, and 100.

The dashed line of Figure 3-9 shows the rate of flow re-establishment behind a complete blockage with only inertial effects accounted for. This is given by equation 3.3-2, which was obtained by the moment-of-momentum equation applied to two adjacent channels.

$$\frac{\dot{m}}{\dot{m}_o} = \frac{1.0}{\dot{m}_o} \left[\frac{1}{\sqrt{3}} \left(c \left. \frac{dP}{dx} \right|_{core} \right) \left(\frac{g_c}{\rho A P_{ch}} \right) x^3 \right]^{1/2} \quad (3.3-2)$$

where

A = blocked channel area,
 P_{ch} = fuel pin pitch,
 x = downstream displacement from the blockage.

This equation should properly account for the variation of crossflow with the axial stream inertia in the channel that is the crossflow source. Ideally, the blocked channel flow rates should follow the shape predicted by equation 3.3-2 (dashed line in Figure 3-9) immediately behind the blockage and gradually take on the shape predicted by equation 3.3-1 (solid lines in Figure 3-9).

Regardless of the crossflow resistance coefficient employed, Figure 3-9 indicates that re-establishment of coolant flow may be expected at a rather short distance downstream from a blockage. The BLOCKO model shows that this relative insensitivity of distance for flow restoration is a result of the variations in the pressure distributions. For higher crossflow resistances, the pressures in adjacent channels redistribute to maintain appreciable crossflows. This is illustrated in Figure 3-8, where the dashed line shows the increasing channel pressure differentials obtained immediately downstream from a blockage for increasing resistance coefficients. (This blockage case was the same geometry as that mentioned above in connection with Figure 3-9.)

In light of these results, then, it is our conclusion that the calculational model adequately predicts the flow and temperature distributions (bulk temperatures) around a blockage, except perhaps for the region immediately downstream from a blockage. The importance of predicting temperatures in this region depends on the location of the blockage as well as its severity and radial extent (number of channels). This must be borne in mind while evaluating the calculational results.

3.3.3. Results of Local Flow Blockage

On examining the possible results of a local blockage, two items are of prime importance: (1) the possibility of localized sodium temperatures in the neighborhood of the boiling temperature because this may affect immediate or delayed fuel pin failure propagation

and (2) the possibility of forming small quantities of molten fuel which, in certain circumstances, might lead to a limited MFCI.

Two fairly distinct effects may result from a local blockage as far as possible sodium boiling is concerned. Sodium boiling may occur within a blockage, even though the coolant temperatures downstream from the blockage are very near normal because of large cross-flow magnitudes. On the other hand, temperatures may be relatively low in the blockage, whereas the downstream fluid temperatures are elevated much above normal. The steady-state temperature levels in any coolant channel or portion of a channel are determined by the local power-to-flow ratio and the flow and power distributions in the bundle. High temperature levels in the channels downstream from a blockage are likely only in the event of a blockage of numerous channels, according to our results. Blockage of one or several channels is more likely to result in high coolant temperatures within the blockages.

3.3.3.1. Bulk Coolant Temperatures Within a Blockage

Figure 3-10 shows the geometry of the BLOCKO calculational model. A unit channel is taken to be the coolant channel area in the triangle described by the centers of three adjacent pins. An equivalent channel is taken to be a radial row of unit channels which are a given number of channel connections from the central channel. When reference is made to a number of equivalent blocked channels, this number corresponds to a number of actual (unit) channels as follows:

<u>Equivalent channels (cumulative)</u>	<u>Actual channels (cumulative)</u>	
	<u>Channel symmetry</u>	<u>Pin symmetry</u>
1	1	6
2	4	12
3	10	24
4	19	36
5	31	54
6	46	72
7	64	96
8	85	120

In this study of fluid temperatures within a blockage, the crossflow resistance coefficients were taken to be 1.0 and 10.0. The low value pertains to all points except the connection between the outermost blocked channel and the adjacent outer channel at the downstream portion of the bundle. The selection of a low upstream crossflow resistance coefficient is conservative from the standpoint of calculating temperatures within the blockage. This is easily seen when we consider that an increase in the crossflow resistance upstream from a blockage will effectively produce a pressure distribution that would force more flow through the blockage; Figure 3-11 illustrates this effect. The normalized mass flow rate through the blockage is normalized with respect to the undisturbed inlet channel flow. There is evidently very little difference between using values of 1.0 and 10.0 for K ; this provides some justification for selecting a value of 1.0 for the subsequent cases.

Figure 3-12 shows the effects of the blockage of four channels on the bulk coolant temperatures at the blockage outlet for the reference geometry. The dashed line is for the case of 10 blocked channels with an area reduction of $1/10$. It is seen that the 10-channel case is not as bad as the 4-channel case. Three blockage characteristics determine the effective blockage resistance: flow area, hydraulic diameter, and length (one must also know the state of the flow—whether turbulent or laminar). Figure 3-12 pertains to the case of a hydraulic diameter that is identical to the unblocked case.

Simultaneous variations in area and hydraulic diameter are shown in Figure 3-13. The flow (and consequently the temperature rise) through the blockage is somewhat less sensitive to changes in the hydraulic diameter than to changes in the flow area.

An equivalence exists between the case of area reduction alone and the case of combined area and hydraulic diameter reductions as long as the blockage length is the same for each case. Figure 3-14 shows this equivalence for turbulent flow in the blockage. Using an equivalent area reduction allows us to eliminate one of these variables—hydraulic diameter—in the succeeding discussions.

The flow distribution results from the BLOCKO code were used to construct Figure 3-15a. This illustrates the variation

in normalized flow through the blockage per unit length in the blockage as a function of blockage area reduction and total blockage length (for the reference geometry). The flow is normalized with respect to the undisturbed channel flow rate. The required channel flow per unit length ($\dot{m}/L$) is proportional to the ratio of average linear power (q') to average core temperature rise (ΔT). In other words,

$$\frac{\dot{m}}{L} \propto \frac{q'}{\Delta T} \quad (3.3-3)$$

This, then, gives a measure of the temperature rise in the blockage. Figure 3-15a shows limiting (minimum) values of the flow per unit blockage length corresponding to sodium temperatures of 1400 and 1700F (the approximate boiling temperature) at the blockage outlet. These temperatures could represent the limits for delayed and rapid fuel pin failure propagation, respectively.

The effects of different types of blockages can be seen by considering the equivalent area reductions given in Figure 3-15c in conjunction with Figure 3-15a.

Figure 3-15b shows the effects of a radially uniform blockage (occurs in every channel of a single assembly). A comparison of the a and b portions of Figure 3-15 shows that the flow through a radially uniform blockage is less sensitive to area reductions than the blockage of a few channels. This is because the bypass flow around a blockage of a few channels tends to "starve" the blocked volume itself. However, for a uniformly blocked pin assembly there are no flow maldistribution effects (ideally), and the flow entering the assembly must pass through the blockage uniformly. The plot in Figure 3-15b is for the relationship

$$\left(\frac{\dot{m}}{\dot{m}_o} \right)^{1.75} = \frac{\left(A_B/A_o \right)^{1.75}}{\left(L_B/L_o \right) + \left[1 - \left(L_B/L_o \right) \right] \left(A_B/A_o \right)^{1.75}}, \quad (3.3-4)$$

which pertains to the case of turbulent flow in the blocked and unblocked portions of the channels. This might be applicable particularly in the case of crud deposition in an assembly.

Some discussion is in order on the applicability or accuracy of the BLOCKO computer program for calculating the bulk coolant temperatures within a blockage of a few coolant channels. The calculational model does not account for transverse heat conduction from the blockage to adjacent, unblocked channels. This transverse heat conduction is very important for a single channel. The British have published the results of an analysis of coolant channel blockages showing that the sodium temperature in a completely blocked channel cannot exceed boiling, even if the sodium is entirely stagnant.¹⁰ This is for the case in which sodium occupies the entire channel and the local specific power does not exceed 200 W/g (peak specific power of the B&W reference core is 170 W/g). In an attempt to apply the British results to the B&W reference core, we have used a very simplified model to calculate the approximate peak blockage temperatures for multiple-channel blockages.

The simplified model uses the integrated form of the heat flow equation in cylindrical, one-dimensional geometry. An approach has been taken to neglect the heat transfer through the fuel so that only the sodium, in idealized pie-shaped segments, conducts the heat energy to the boundary. The equation for the radial temperature rise from the boundary to the blockage center becomes

$$\Delta T = \frac{V_{\text{fuel}}}{V_{\text{sodium}}} \left(\frac{\dot{q}_{\text{fuel}} r^2}{4K} \right) \quad (3.3-5)$$

where

$$\frac{V_{\text{fuel}}}{V_{\text{sodium}}} = \text{fuel-sodium volume ratio,}$$

$$\dot{q} = \text{volumetric heat generation rate,}$$

$$K = \text{sodium thermal conductivity,}$$

$$r = \text{approximate blockage radius, which is dependent on the number of channels blocked.}$$

For the case where sodium occupies the entire channel at a local heat rating of 13.1 kW/ft, equation 3.3-5 gives the following results for various numbers of blocked channels:

<u>Channels blocked</u>		<u>Blockage radius, in.</u>	<u>Temperature rise, F</u>
<u>Equivalent</u>	<u>Actual</u>		
2	4	0.265	620
3	10	0.427	1610
4	19	0.575	2910

The detailed British results give a temperature rise of 530F through a blockage of four actual channels. This is taken to be adequate proof of the low probability of sodium boiling downstream from a blockage of less than four actual channels, where stagnant sodium occupies the blocked channels. Of course, sodium boiling within a blockage is not precluded since the amount of sodium available for radial heat conduction might be diminished by the presence of either inert or fissile material in the channels.

3. 3. 3. 2. Bulk Coolant Temperature Distributions Outside Blockage Zone

Selected types of blockages have been examined using the BLOCKO program to determine the effects on the bulk coolant temperature distributions outside the blockage zone. The two most important blockage parameters here are the radial extent of the blockage (number of affected channels) and the blockage resistance (resistance includes the axial extent or length of the blockage). The two types of blockages examined were a severe blockage of constant axial but variable radial extent and a moderately severe blockage of constant radial but variable axial extent.

Figures 3-16, 3-17, and 3-18 illustrate the effects of a severe blockage in one, four, and 19 channels, respectively. The severe blockage consisted of a hundred-fold reduction in both channel flow area and hydraulic diameter over a length of 1.6 inches (this length corresponds to one division length where the channel length was divided into 40 equal parts). The axial power profile in each channel was taken to be as follows (symmetrical about the core midplane):

<u>Node No.</u>	<u>Normalized power</u>	<u>Node No.</u>	<u>Normalized power</u>
1	0.008	11	0.528
2	.008	12	.608
3	.008	13	.696
4	.008	14	.752
5	.008	15	.812
6	.016	16	.872
7	.024	17	.916
8	.032	18	.952
9	.048	19	.980
10	0.064	20	0.999

The crossflow resistance coefficients quoted for these figures are for the resistances at the various upstream and downstream crossflow connections. The higher value is for the downstream connection between the outermost blocked channel and its adjacent unblocked channel; the lower value is for all other crossover points. The geometry is that of the reference core.

The upstream axial flows are relatively undisturbed. The worst case shown is the 19-blocked-channel case, where the inlet temperature to channel No. 1 is approximately 70F above the fluid temperature of the typical unblocked channel at the same axial elevation (see Figure 3-18). The main effect of the severe blockage is seen to be the elevated temperatures downstream from the blockage in the innermost channel (channel No. 1). The coolant temperature reductions immediately downstream from the blockage are a result of the low-temperature coolant crossflow. The magnitude of the crossflow entering the channel decreases with increasing numbers of blocked channels. The axially decreasing coolant temperatures in the upper axial blanket portion of channel No. 1 are a result of turbulent mixing between channels (in this portion of the coolant channel the magnitude of the crossflow is negligible). For the turbulent mixing calculations, a value of $\beta = 0.01$ was used (β is the ratio of effective transverse mass velocity to axial mass velocity).

Figures 3-19, 3-20, and 3-21 show the case of a moderately severe blockage (of variable axial extent) of 46 channels. Some fluid temperature effects occur upstream from the blockage; these

are relatively unimportant. The main effect of the blockage is a generally high sodium temperature level downstream from the blockage. The shaded rectangle at the bottom of each figure indicates the axial extent of the blockage. The axial power distribution for these cases is identical to that mentioned in conjunction with Figures 3-16 through 3-18.

It is seen from Figures 3-19 through 3-21 that turbulent mixing is not quite as effective as in the blockage cases involving fewer radial channels (Figures 3-16 through 3-18). This is to be expected; turbulent mixing is most effective in reducing the elevated temperatures in a single channel adjacent to a normal channel.

The apparent anomalous shape of the radial coolant temperature distribution at the blockage outlet in Figures 3-19 through 3-21 is a result of the pressure distribution around the blockage. The axial pressure difference through the blockage is greatest in radial channel No. 1. This results in a higher flow through the blockage and, consequently, a lower temperature rise.

The BLOCKO calculational model determines the flow distribution in a number of coolant channels; this may be used to determine the reduction (if any) of the mean assembly flow. The flow reductions for the cases discussed above are as follows:

Type of blockage	No. of blocked channels (actual)	Blockage length, fraction of channel length	Assembly flow, fraction of normal
Extreme (Figures 3-16 through 3-18)	1	1/40	0.9999
	4	1/40	0.9995
	19	1/40	0.9959
Moderate (Figures 3-19 through 3-21)	46	1/20	0.9860
	46	1/10	0.9800
	46	1/5	0.9760

3.3.3.5. Effects of Particular Blockages

Two blockage types are of special interest because of the identifiable events that could lead to such blockages: the

channel inlet blockage caused by a small foreign object held against the pin bundle by dynamic fluid forces, and the single pin failure with subsequent expansion to partially fill the surrounding coolant channels.

Figure 3-22 shows the individual coolant channel bulk sodium temperatures at the pin bundle outlet for inlet blockages of various numbers of equivalent channels. The figure shows the maximum temperatures experienced in the bundle; this is because the blockage occurs at the inlet where the flow disturbance behind (downstream from) the blockage is located in a very low power segment of the pin bundle. No turbulent mixing between channels was accounted for in this calculation.

Figure 3-23 shows the resulting temperature distributions for the hypothetical case of a single pin failure with subsequent expansion into the neighboring channels. The length of the blockage, shown by the shaded rectangle, is equal to half the channel length (the approximate length of the active core). For this very unlikely axial extent of cladding failure and subsequent blockage, it is seen that the bulk channel coolant temperature does not exceed 1400F. For this case, turbulent mixing was taken into account along the length of the blockage since the crossover between equivalent channels 1 and 2 was considered to be open, as shown below:

3.3.4. Results and Conclusions on Flow Blockage Effects

To be complete, consideration of the types and extents of flow blockages must include the following:

1. Crud—material in solution or suspension.
2. Channel-sized extraneous material lodged within or against (outside of) the bundle.
3. Core material—particles of cladding or fuel, bowed pins or deformed pin array, single enlarged pin, and multiple enlarged pins.

Each of these blockage types must be evaluated with respect to (1) the probability of occurrence, (2) the detectability, (3) the probability of inducing either direct or indirect fuel pin failure propagation, and (4) the probability of inducing conditions leading to a possible molten fuel-coolant interaction.

3.3.4.1. Crud

Crud deposition in the core might arise from a material source in the coolant itself in the form of a solution or solid suspension. This is most assuredly a very unlikely occurrence in a properly designed plant. The sodium purification system, which includes hot- and cold-trapping, should maintain the sodium purity at the point where negligible amounts of material would ordinarily be present in the primary sodium. The presence of large amounts of material in the sodium would have to be a result of some fault occurring in the system.

A primary system fault that results in a large volume of impurities or solid particles being discharged into the sodium should not induce a local crud-type plugging in the core. Should crud deposition occur, the tendency would be toward a relatively uniform deposition in a large number of assemblies, even if the material were to be transported to the core inlet plenum through one of the six available inlets (a pump and inlet pipe combination). This would be a result of the flow turbulence. Another factor that tends to reduce the probability of plugging is the fact that deposition is a rate-controlled process. The time required for significant extents of crud deposition due to a system fault is very likely many times greater than the coolant's transit time through the core. This means that, for a given amount of material released into the sodium, the material would have to be "fed" slowly to the coolant entering a primary pump or would have to make multiple passes through the core. This time required for significant deposition

would tend to minimize the amount of material in the coolant because of the action of the sodium purification system. The possibility of multiple passes through the core would, of course, tend to distribute the material that would tend to deposit or accumulate over the entire core uniformly.

If plugging should occur from a fault that continually feeds extraneous material to one primary system inlet pipe, the uniform distribution to one-sixth of the fuel assemblies could be detected by measuring the core outlet temperature. Uniform deposition (in the radial sense) does not lead to high local temperatures within a blockage; it produces a uniform flow reduction which manifests itself in a high fuel assembly outlet temperature. Thus, an occurrence of significant plugging should be detectable.

Very large amounts of extraneous material would be required to produce significant plugging. This is evidenced by Figure 3-15b, which shows the relative insensitivity of flow through an assembly to area reductions that occur uniformly in each channel. The most likely consequence of plugging or crud deposition would probably not be coolant flow reductions or maldistributions leading to elevated sodium temperatures, but rather a "fouling" of the cladding, which would elevate cladding temperatures as a result of increased resistance to the flow of heat. A very large volume of foreign material is required to create a flow reduction problem in a single assembly if uniform deposition occurs. Reductions in area and hydraulic diameter corresponding to blockage No. 3 in Figure 3-15c over 0.5 of the active core length of the reference design would require 146 in.³ of material per fuel assembly. This volume corresponds to a 5.3-inch cube. The effect of a blockage of this severity is an elevation of the outlet temperature to about 1400F (the 1400F outlet temperature limit corresponds to a normalized mass flow per unit length of blockage equal to 0.93, as given in Figure 3-15a). The equivalent area reduction (which represents the effects of combined area and hydraulic diameter reduction) for this case is not quite as severe as $A_0/10$, where A_0 is the normal channel area.

The probability of a localized severe blockage (resulting from crud accumulation) leading to fuel melting within adjacent pins appears to be extremely low. The probable length of time

required for such an accumulation of material and the tendency for material to distribute uniformly over the core with repeated circulation through the primary system seems to preclude such a localized buildup. Also, the effect of high coolant velocity passing through a localized partial buildup would be expected to limit accumulation by the process of erosion.

3.3.4.2. Channel-Sized Extraneous Material

The effects of reasonable numbers and/or sizes of channel-sized foreign objects appear to be inconsequential as far as objects that could lodge within a channel or against the inlet of the pin bundle are concerned. The result of the lodging incidence of a regular-shaped object in a coolant channel is typified by the results obtained for a channel-sized cylindrical object and the very severe (hundred-fold) area reduction cases. Figure 3-15a shows that an area reduction corresponding to $A_o/3$ (which would be the effect of a cylindrical object) would require a blockage length of up to 0.3 of the active core length or 10.0 inches in order to produce coolant temperatures of 1400F at the blockage outlet. Thus, it is reasonable to dismiss the possibility of such a localized blockage leading to fuel pin failure consequences because of the unreasonable blockage length required. This same conclusion has been reached for the case of the complete blockage that would result from a thin plate-type blockage in a single channel. For this case, the flow distribution around the blockage should be such that a negligible coolant temperature rise occurs in the vicinity of the blockage. This is again indicated in Figure 3-15a, where the case of $A_o/100$ shows that a blockage of this severity would have to be more than one pin diameter in length (0.28 inch) in order to produce bulk coolant temperatures of 1400F in the blockage. The case of $A_o/100$ can be taken as essentially the limiting case since results from the BLOCKO calculational model indicate a flow reduction to 0.001 of normal for this blockage severity.

The calculated results from the BLOCKO computer program are quite conservative for the case of a single channel blockage. This is because the program does not consider the transverse heat flow from a blocked channel to an adjacent unblocked channel. This

is, of course, very important for the single blocked channel case. Analyses intended to account for this effect of transverse heat flow from a blocked channel have shown that transverse conduction is so large that it precludes sodium boiling in a completely blocked channel full of stagnant sodium.¹⁰ (This is for a case of 200 W/g specific power rating, whereas the B&W reference core has a peak specific power rating of 170 W/g.) An extension of the British results to the particular geometry of the reference core has, by using a simplified model, indicated that the blockage of four channels would be required to produce sodium boiling temperatures (see section 3.3.3.1) if stagnant sodium occupies the channel. This result demonstrates the conservatism of Figure 3-15a, which is for the case of four blocked channels. Also, it shows that the blockage of a single channel is relatively unimportant as far as gross sodium boiling is concerned, regardless of the amount of channel cross-flow upstream or downstream from the blockage.

Given the proper blockage geometry, a single channel blockage of realizable axial extent may lead to some fuel melting in the adjacent pins. A blockage of this type might also result in cladding temperatures considerably above normal. A single channel blockage is essentially undetectable by outlet temperature measurements. Figure 3-16, which is for the case of a severe blockage of a single channel, shows the effect of crossflow and turbulent mixing on diminishing channel temperature differences at the bundle outlet.

3.3.4.3. Core Material

The problem of a pin bundle blockage caused by core material is very important because of the possibility of single pin failures as well as multiple failures if failure propagation is postulated. In the following discussions the effects of (1) particles of cladding or fuel, (2) bowed pins or a deformed pin array, (3) a single enlarged pin, and (4) the case of multiple enlarged pins are considered. The probability that cladding or fuel particles, bowed pins, or a single enlarged pin will cause a localized partial blockage is significant. The other case—that of multiple enlarged pins—must be considered to be improbable, since we are assuming here that such a condition could arise only as a result of related pin failures (postulated pin failure

propagation). Each of these types of blockage would be essentially undetectable by temperature measurement at the bundle outlet if they affected only a few channels. Again, this may be a result of crossflow around the blockage and transverse heat transfer by turbulent fluid mixing. (See, for example, the axial temperature profiles in the blocked channels as given in Figure 3-17.) The detectability by flow measurement is also not feasible for cases of a few blocked channels. Fuel assembly flow reductions for several cases are given at the end of section 3.3.3.5; these show that for considerable numbers of blocked channels (say, 46 channels) over an extensive axial length, the flow reduction may approach 3%. Even this reduction is probably not detectable by practical current flow instrumentation.

Particles of Cladding or Fuel — The incidence of a single channel blockage comprised of cladding or fuel is not significantly different from that discussed for foreign material in a single channel. The likelihood of significant lengths of fuel particles building up in a channel should be very low. Any fuel particles that might escape from a cladding fissure would tend to be swept out of the pin bundle, since the terminal velocity of a channel-sized fuel particle is considerably less than the coolant velocity, which is as much as 20 fps. Although the buildup of fuel in a channel should be quite unlikely, the consequences of its occurrence should be considered. Given a severe enough (coolant flow area reduction) single channel blockage by fissile material, localized fuel melting and elevated cladding temperatures in adjacent pins are possible (an analysis of channel blockages performed by the British,¹⁰ however, indicated that the maximum temperature experienced by the fissile material in the channel would probably not exceed the local sodium boiling point). This fuel melting within adjacent pins does not necessarily imply any direct safety hazard. The possibility exists, however, that some delayed fuel pin failure may result. Molten fuel formation within a blocked channel is indicated for the case of two adjacent coolant channels blocked by fissile material with no sodium flowing through the blockage. This situation is expected to be extremely remote since very small flow rates through such a blockage are unlikely and would be very effective in reducing the fissile material temperatures in the blockage.

The extent of any sodium boiling that might occur in the case of fissile material blocking one or several channels should be minimal. Figures 3-16 and 3-17 show that the sodium temperature outside such a severe blockage are at the normal levels. Of course, even within the blockage the temperatures should not exceed the boiling point (corresponding to the local pressure) by more than 100F or so. Two possibilities are evident for increasingly severe blockages of this type as far as sodium boiling is concerned. A sort of uniform, steady dry-out of the sodium could occur, or a cyclic vapor generation-liquid expulsion and condensation-liquid re-entry into the blockage could result. Neither of these boiling phenomena necessarily implies that a molten fuel-coolant interaction or fuel pin failure propagation would occur. In order for sodium boiling in a blockage to occur, a very small quantity of sodium must be present; the change of phase upon boiling would then involve small changes in sodium volume. Some calculations have been made concerning the effects of sodium liquid flashing to vapor within a pin bundle. The results, presented in section 3.4, indicate that no severe consequences are likely regarding the direct effects of sodium boiling as far as rapid fuel pin failure propagation is concerned.

Bowed Pins or Deformed Pin Array — This case pertains to the geometry shown in Figure 3-15c, No. 3, except that the flow area corresponds to the situation where the fuel pin pitch is equal to the original pin diameter instead of vice-versa. Thus, from Figure 3-15c, the fractional flow area is $0.27 \times (\text{pin dia}/\text{pin pitch})^2$ or 0.19, so that the equivalent area reduction (corresponding to the hydraulic diameter reduction) is to approximately $0.10 A_0$. From Figure 3-15a it is seen that an area reduction down to $0.10 A_0$ would require a considerable length to result in a bulk coolant temperature of 1400F. This considerable length required, along with the conservatism of the figure when applied to a single channel, would indicate that a single channel blocked in this manner would not result in sodium boiling. The elevated sodium temperatures at the point of pin contact, however, would have a deleterious effect on the mechanical properties of the cladding for the affected fuel pins. Prolonged operation in this state could conceivably

lead to delayed cladding failures. However, more analytical work is required in this area to determine whether or not this situation might lead to pin failures. A detailed temperature map of the blockage (channel and pins) is required. Fuel melting in the affected pins is not indicated for this case.

Single Enlarged Pin — An examination of Figure 3-15a for this particular case (area reduction to $A_0/3$, as indicated in Figure 3-15c, No. 2) would lead to the conclusion that bulk sodium temperatures within the blockage might approach 1400F for a blockage length of approximately 0.30 of the active core length (about 10.0 inches in length). A more detailed analysis of this case, however, shows that the account of turbulent mixing with adjacent channels results in a maximum temperature of slightly less than 1400F even for the case of a pin enlarged over the full active core length, 34.7 inches. (See Figure 3-23.) This type of blockage, then, would apparently have a minimal effect on possible sodium boiling. The effect on possible delayed cladding failure is not so apparent, however, just as in the blockage type discussed in the previous section. The points where adjacent pins contact (for this type of postulated blockage) would experience elevated sodium temperatures considerably greater than the corresponding local bulk temperatures. A detailed analysis of the temperature distributions in contacting fuel pins and sodium coolant is required to determine the possible deleterious effects on cladding mechanical properties at these locations. In this case, as in the previous one considered, molten fuel formation in adjacent pins is not evidenced.

Multiple, Enlarged Pins — As mentioned under section 3.3.4.3, the postulated blockage corresponding to multiple expanded pins might pertain to an assumed extent of fuel pin failure propagation. It is not a geometry resulting from any identifiable sequence of events; it is intended only to illustrate the detectability aspects of an extensive (radial and axial) blockage as well as its probable consequence.

The axial and radial temperature profiles for various axial extents of the blockage of 46 channels are shown in Figures 3-19 through 3-21. The maximum fluid temperatures in each case occur in the upper axial blanket area rather than the blockage region. For this

number of blocked channels, measuring the temperature fluctuation near the pin bundle outlet may be feasible for detection (this method was proposed by Bentley¹¹). The largest blockage of this type that was examined is that of 0.15 fractional channel length. The maximum coolant temperature for this case, approximately 1675F, occurs in the upper axial blanket of the innermost blocked channel (Figure 3-21). As mentioned previously, the assembly flow reduction accompanying a blockage of this type is quite small—less than 3%. Detecting this blockage extent by flow measurement does not appear to be feasible.

Since elevated temperatures occur within the blockage (maximum for the outermost blocked channel) and downstream from the blockage (innermost channel), it would appear that once this type of blockage developed, it could lead to increased pin failures as a result of the degradation of cladding mechanical properties with the high temperatures. However, it is quite unlikely that this extent of blockage will develop; the results for single channel blockages indicate that the propagation mechanism would have to be something other than large amounts of high-temperature sodium in a channel. The fluid temperature effects from a single channel blockage are localized within or at the boundary of the blockage. Failure propagation from a single blocked channel appears very remote and would probably have to result from a propagation mechanism involving sodium boiling or some mechanism operating through some internal fuel pin phenomenon.

It is possible that some amount of molten fuel could be formed within the pins affected by blockages of these extents. Since the fluid temperatures may approach 1600F, some fuel melting is conceivable if the blockage involves the "hot pin" of the core. General or gross melting would not occur in the peak pins involved in this type of blockage because of the steady-state margin to fuel melting (nearly 600F).

3.4. Effects of Localized Sodium Boiling With Superheat

The results of the flow blockage studies indicate that small quantities of sodium coolant could be heated to beyond the saturation temperature for particular severities and extents of blockage. Indeed, the

possibility of appreciable quantities of liquid-phase sodium flashing to vapor must be considered, even if the simplified cases of blockages examined indicated that sodium temperatures of this magnitude were not probable. Once the possibility of multiple channel blockages is admitted, we must account for the possibility, however remote, that the blockage configuration might effectively trap pockets of sodium at the upstream boundary of the blockage. A blockage might form one or many inverted "cups" of random size at the upstream edge.

A model was constructed to determine the possible effects of a localized volume of superheated sodium flashing to vapor. The details of this model are presented in Appendix D. In general, the effects of flashing sodium are determined by calculating the bubble displacements as well as upper and lower liquid slug displacements with time; the bubble pressure at any time is determined by the temperature of the originally superheated liquid sodium. The variation of liquid sodium temperature accounts for heat transfer to some fraction of the fuel pins within the bubble envelope and the energy required to vaporize the sodium. Condensation is also accounted for by assuming that some fraction of the fuel pin surface within the bubble or void envelope is available for condensation because of the thin film of low-temperature (normal level) sodium remaining.

The calculational model was applied to a number of cases involving different amounts of superheated sodium, levels of superheat, axial locations of the disturbance, and fractions of the cladding surface area within the bubble envelope that is available for hot liquid sodium and cladding contact. The following tabulation lists the various cases, including the time for the bubble existence (from inception to collapse), as well as the calculated value of collapse pressure resulting from slug impact. (The impact pressure (p) is calculated by the product of fluid density, the acoustic velocity, and the differences in slug velocities.)

Case No.	Original hot Na vol, ft ³	Axial location	Original temp, F	Fraction of surface for liquid Na-cladding contact	Time for bubble existence, ms	Slug impact pressure, psi
1	10 ⁻⁴	MP ^(a)	2200	1.0	16	664
2	10 ⁻³	MP	1750	1.0	19	276
3	10 ⁻⁵	LB/C ^(a)	2000	0.1	7	492
4	10 ⁻⁵	LB/C	2000	1.0	4	310
5	10 ⁻³	LB/C	2000	0.1	45	1544
6	3 × 10 ⁻⁴	MP	3000	0.1	80	2240

(a) MP: midplane; LB/C: lower blanket/core interface.

Case No. 6 has no real significance as far as ordinary sodium boiling is concerned. It is included here because of its possible significance for a case of molten fuel-sodium interaction.

Figure 3-24 shows the bubble traces for the cases above. In cases 1 through 4, the length of time that any one axial position is voided is considerably less than the bubble lifetime. This is due to the effect of coolant inertia, which effectively carries the bubble downstream during its stages of growth and collapse. Vapor blanketing of fuel pins for just a few milliseconds has a negligible effect on cladding and fuel temperatures. For this reason, most cases of sodium boiling with superheat should have little influence on the possibility of either delayed or rapid fuel pin failure propagation.

The variations in sodium, cladding, and fuel temperatures for case 6 are shown in Figure 3-24. The cladding temperatures are for the portion of the fuel pins that come in contact with the liquid-phase hot sodium. This temperature plot is for the maximum cladding temperature, not the average of this region. The point at which the maximum cladding temperatures occur is assumed to "see" hot sodium during the entire calculational time regardless of the axial bubble position. This accounts for the time scale taken to 0.080 second, even though no axial position in the bundle is enveloped by the bubble for more than about 0.038 second. The variations in the sodium and cladding temperatures in this figure are due first to the transfer of thermal energy to the cladding from the sodium; once the temperature differential reverses,

the cladding transfers energy to the sodium as the sodium temperature decreases as a result of vaporization.

This worst case indicates no likely chance for delayed or rapid fuel pin failure propagation. With the nearly constant fuel temperatures, the overall effect of the cladding temperature rise would be for the cladding to expand and move away from the fuel meat, thereby lowering the cladding stress contribution of fuel-cladding mechanical interaction. If the cladding temperature rise is circumferentially uniform around a pin, then the cladding thermal expansion for a 600F temperature rise is

$$\begin{aligned}\epsilon &= \alpha \Delta T = (10^{-5} \text{ in. /in. - } ^\circ\text{F})(600\text{F}) \\ &= 0.6\%,\end{aligned}$$

which is three times the strain for yielding unirradiated cladding. Thus, the cladding, which is originally assumed to be at the yield point, would expand to remove all the mechanical interaction stresses. In the expression above, ϵ is the strain, α is the coefficient of thermal expansion, and ΔT is the temperature change.

Figure 3-2. Calculated Two-Phase Pressure Gradient for Steady Flow of Liquid Sodium and Xenon Fission Gas at 20 psi Based on Lockhart-Martinelli Correlation

Figure 3-3. Void Fraction for Sodium Flows Resulting From Two-Phase Flow of Sodium and Fission Gas

Figure 3-4. Length of Time Fission Gas May Flow at Constant Rate for Various Sodium Flow Reductions — Hot Pin

Figure 3-5. Length of Time Fission Gas May Flow at Constant Rate for Various Sodium Flow Reductions — Nominal Hot Pin

Figure 3-6. Comparison of Flow Durations for Flow Rates Above a Given Value (Variable Vs Constant Flow)

Figure 3-7. Variation of Crossflow Resistance Coefficient With Ratio of Transverse to Axial Fluid Velocities for Dividing Manifold Flow

Figure 3-8. Crossflow for Various Channel Pressure Differences and Values of Crossflow Resistance Coefficients

Figure 3-9. Reestablishment of Flow Downstream From Blockage

Figure 3-10. Geometry of Calculational Model

Figure 3-11. Normalized Mass Flow Rates Through Blockage With Area Reduced to 10% of Normal

Figure 3-12. Maximum Bulk Coolant Temperature in Blockage of Four Channels (No Variation in Hydraulic Diameter)

Figure 3-13. Maximum Bulk Coolant Temperatures in Blockage of Four Channels (Variations in Channel Area and Hydraulic Diameter)

Figure 3-14. Equivalent Area Reduction for Blockage of Given Area and Hydraulic Diameter Reduction (for Given Blockage Length)

Figure 3-15a. Normalized Flow per Unit Length Through Blockages With Various Characteristics

Figure 3-15b. Normalized Mass Flow Through Radially Uniform Blockage (Occurs in Every Channel of a Single Assembly)

Figure 3-15c. Equivalent Area Reductions for Various Blockage Types

Blockage Type		A/A_0	$De/(De)_0$	$(A/A_0)_{\text{Equivalent}}$
Cylindrical Foreign Object		0.48	0.28	$< 1/3$
Single Failed and Expanded Fuel Pin		0.48	0.43	$\approx 1/3$
Multiple Failed and Expanded Fuel Pins		0.27	0.28	$> 1/10$
Small Cylindrical Passage		0.10	0.30	$> 1/30$
Inert Spherical Particles		0.095	< 0.034	$< 1/100$

Figure 3-16. Fluid Temperatures for Extreme Blockage of a Single Coolant Channel

Figure 3-17. Fluid Temperatures for Extreme Blockage of Four Coolant Channels

Figure 3-18. Fluid Temperatures for Extreme Blockage of 19 Coolant Channels

Figure 3-19. Fluid Temperatures for Blockage Equivalent to Failed and Expanded Pins (46 Channels),

Figure 3-20. Fluid Temperatures for Blockage Equivalent to Failed and Expanded Pins (46 Channels),

Figure 3-21. Fluid Temperatures for Blockage Equivalent to Failed and Expanded Pins (46 Channels),

Figure 3-22. Outlet Coolant Temperature Profiles for Various Degrees of Inlet Flow Blockage

Figure 3-23. Fluid Temperatures for Blockage Equivalent to Single Failed and Expanded Pin

Figure 3-24. Bubble Traces for Incidence of Localized Steam Boiling With Superheat

Figure 3-25. Calculated Temperatures for Hot Sodium, Cladding, and Fuel in Bubble Region—Case No. 6

4. STUDIES OF MOLTEN FUEL-SODIUM INTERACTION

The incidence of a molten fuel-sodium interaction on a localized level has only recently been recognized as a possible safety problem for the LMFBR. As our examinations of the effects of local accidents commenced under this contract, there was some doubt (as there is at this writing) as to whether or not a so-called sodium vapor explosion could occur within an actual reactor. At the present time there is still no conclusive evidence that a violent molten fuel-coolant interaction has occurred in any operating reactor. (The SPERT-1D incident has been attributed to a combination of molten cladding-coolant interaction with a water hammer effect.) In addition, attempts to induce violent molten metal-coolant interactions under controlled experimental conditions have not always been successful. Therefore, in an attempt to understand the so-called trigger mechanism for violent interaction and whether or not it would be active under operating reactor conditions, a literature search and appraisal was conducted (see Appendix C for details).

The conclusions drawn from the literature search were essentially as follows:

1. The triggering mechanism for a vapor explosion is the fragmentation of the hot molten mass (metal or fuel).
2. We can identify numerous causes of fragmentation, none of which can be excluded as being inoperable under actual reactor conditions in the event of molten fuel-coolant contact.

Therefore, it was concluded that the safety evaluation of the effects of local faults must admit the possibility of a fuel-coolant interaction.

Based on the information obtained from the literature, an effort was initiated to model the fuel-coolant interaction phenomena. Considerations of fragmentation mechanisms were neglected in the formulation of this model (MFCODYN) because of the large uncertainties and

the absence of any real quantitative knowledge in this area. The model that was developed accounts for the time-variable heat transfer rate from spherical fuel fragments to the surrounding sodium. (See the details of this model in Appendix E.) The heated sodium is prevented from expanding only to the extent that the surrounding sodium can offer constraint, which is determined by considering its compressibility during the initial phase (acoustic constraint) and its inertia during the latter phase.

At this time, the development of the computer program had not progressed enough to permit a comprehensive study of the important parameters. Some results were obtained for the work done by the expanding sodium in the heated mixture; these are given in section 5.1.4.1. For those computer calculations made, the sodium pressures occasionally attained values in excess of the critical pressure (about 7000 psia) depending primarily on the sodium-to-fuel mass ratio, the ratio of heat transfer area to effective area for sodium expansion, and the constraining mass of the surrounding sodium. These pressure levels are not necessarily of great significance, since the capacity of the heated sodium for structural damage and assembly voiding depends on its mass and the duration of these pressure levels.

5. FUEL ASSEMBLY RESPONSE

This section deals with the capability of fuel assembly structures to limit the consequences of localized molten fuel-sodium interactions. The basic fault propagation mode under consideration involves the postulated inadequate cooling over a short length of a single coolant channel followed by fuel melting, fuel pin cladding failure, mixing of molten fuel and sodium, and the conversion of thermal energy in the fuel to mechanical energy in the form of vaporization of sodium. The ability of the fuel assembly structures to dissipate or to direct the mechanical work potential from the last event is highly dependent on the rate at which the event takes place. This rate, in turn, depends on the temperatures, properties, and volumes of the fuel and sodium involved.

The main approach adopted here is to establish a limit for the amount of structural damage that can be tolerated and then determine whether the amount of molten fuel that can credibly be available at the site of a local cooling failure interacts with liquid sodium to produce the mechanical energy equivalent of this damage.

If such an energy release is sufficient to cause failure of the fuel assembly can within which it originates, the adjacent fuel assemblies and safety rod guide tubes could be displaced and/or crushed. This could lead to fuel movement in the core and significant flow reduction within the affected assemblies. The reactivity effects caused by the fuel movement may be of little consequence; however, the flow reductions could result in local coolant boiling and progressive flow starvation. Since this is a potential accident propagation mechanism, it is particularly important to determine: (1) the rupture pressure of the fuel assembly can, (2) the pressure at which nearby fuel assemblies and safety rod guide tubes would be deformed enough to interfere with their functions, and (3) the time sequence of the preceding events, since it is possible that fuel melting and sodium vaporization could violate the

core's integrity and propagate throughout the core if reactor scram time is not short enough. Consequently, from a protection viewpoint, it is important to know the extent of any damage as well as the time span involved because this knowledge helps to determine the adequacy of the reactor control system. Therefore, the objective of this effort has been to analyze the structural responses of B&W's LMFBR fuel assemblies to an energy release of this type in order to determine gross damage or complete failure thresholds as well as the total time involved from fault inception to fault consequence abatement.

5. 1. Individual Fuel Assembly

5. 1. 1. General

The failure thresholds for a single fuel assembly duct with internal loading are functions of the loading to which the duct is subjected, its geometry, and the properties of its construction materials.

Two loading conditions must be considered: first, the static loading condition, for which the deflections and stresses of the duct wall are in equilibrium with the applied load; second, the dynamic loading condition, for which the deflection and stresses of the duct wall are influenced by both the applied load and the inertia of its mass. The fundamental period of vibration was used to categorize the loading condition in accordance with the following definition. If the loading time is equal to or greater than twice the fundamental period, the loading is classified as static. If the loading time is less than twice the fundamental period, the loading is classified as dynamic. In both cases, it was assumed that the loading was in the form of uniform internal pressure.

To determine failure thresholds under these loading conditions, it was assumed that the duct configuration would change from the original hexagonal shape to a more stable circular shape. It is realized that the final shape of the duct would not be perfectly circular; however, it can be used to establish an upper limit for the pressure-sustaining and energy-dissipating capabilities of the duct.

5. 1. 2. Static Loading

As the pressure differential across the duct wall is increased, several thresholds of damage are encountered. Initially, the

flats of the duct wall act as fixed-end beams under uniformly distributed loads. Under these conditions, the maximum bending moment and material fiber stress will occur at the corners of the duct. At some loading, the most highly stressed fiber will experience yield stress. This is the first damage threshold. If the loading is removed at this point, the duct will return to its original shape.

If the loading is increased, the most highly stressed fibers will suffer plastic deformation, which will propagate through the entire thickness of the duct wall with sufficient increases in the loading. This is the range of the second damage threshold. If the loading is not increased until a region of complete plasticity is formed in the duct wall, the duct is not expected to rupture, although some permanent deformation will occur.

For loadings equal to or slightly greater than the loading required to form a region of complete plasticity in the duct wall, the plasticity will propagate throughout the duct wall and allow the duct to assume a circular shape. In this configuration the duct is capable of sustaining further increases in loading. After the duct becomes circular, further deformation is resisted by hoop stress in the duct wall. For this configuration, failure can be predicted on the basis of the ultimate strength or the ductility limit of the duct wall material.

The pressure corresponding to the first and second damage thresholds, i.e., the onset of plastic deformation and the transition from hexagonal to circular, were calculated using the following equation:

$$P = K S_g (T/L)^2$$

where

P = pressure, psi,

K = 2 and 4 for first and second damage thresholds, respectively,

S_g = yield strength, psi,

T = wall thickness, in.,

L = length of one duct flat, in.

The graphs of these pressures as a function of fluence are shown in Figure 5-1. The abscissa values were obtained by considering the change in material yield strength with irradiation. The

lower curve shows the pressures corresponding to the onset of plastic deformation, and the upper curve shows the pressures at which the fuel assembly duct is expected to assume a circular shape.

The maximum possible pressure that can be sustained by the fuel assembly duct was calculated to be approximately 2000 psi using the following equation and a value of 54,000 psi for unirradiated ultimate strength.

$$P = S_u T / R$$

where P = pressure, psi,
 S_u = ultimate strength, psi,
 T = wall thickness, in. ,
 R = duct equivalent radius, in.

5.1.3. Dynamic Loading

For conditions in which the fuel assembly duct wall is presumed to act dynamically, its energy-dissipating capability was selected as the criterion for establishing damage thresholds and failure. This approach was used because the analyses of the energy release from a molten fuel-sodium interaction and the resultant structural damage are subject to considerable uncertainty and because it provides a simple and direct method for establishing upper and lower limits for structural damage.

The lower-limit damage threshold for the duct was established by considering its elastic-energy-absorbing capability. As stated previously, it was assumed that the loading is in the form of uniform pressure. It was also assumed that the deflections and deformations have the same form as those associated with a static analysis.

The energy-absorbing capacity of the fuel assembly duct material (304 SS) for deformations within the elastic range is relatively small when compared with that for plastic deformation, provided the ductility of the material has not been reduced to insignificant levels by irradiation. Since the experimental data (to date) show that this might be the case with existing materials, the elastic-energy-dissipating capability of the duct was used as a lower-limit value. For a material

condition of nil ductility and high yield strength, the results showed that the duct could absorb approximately one ft-lb of energy per inch of axial length. The equation used to calculate this value is as follows:

$$E_e = (S_y)^2 TL / (60E)$$

where

E_e = elastic strain energy in bending, ft-lb,

T = wall thickness, in.,

L = length of one duct flat, in.,

E = Young's modulus of elasticity, psi,

S_y = yield strength, psi.

The upper-limit energy-absorbing capacity for the duct was established by assuming that its construction material would accept the deformations required to permit it to transform from hexagonal to circular, then calculating the energy-absorbing capacity of the equivalent circular configuration. This energy was calculated using the following equation:

$$E_p = \pi S_y T B D / 12$$

where

E_p = plastic strain energy per axial inch of duct, ft-lb,

S_y = yield strength, psi,

T = wall thickness, in.,

B = plastic elongation limit, in./in.,

D = duct equivalent diameter, in.

Figure 5-2 is the graph of E_p as a function of fluence up to 10^{22} nvt. The abscissa values were obtained by considering the changes in material yield strength and ductility with irradiation at a temperature of 1100F, as given in the FFTF Materials Design Data document.¹⁵ Since the data concerning the relationships of fluence, yield strength, and ductility at fluence levels above this point are limited, the energy-dissipating capabilities of the duct are presented in Figure 5-3 as the plastic energy potential of the cylindrical duct for various values of

material yield strength and ductility. The minimum total elongation at a fluence of 10^{22} nvt for 304 SS at 1100F is approximately 13.1%, as determined from reference 15. At these same conditions, the yield strength is about 30,000 psi.

5.1.4. Mechanical Energy Release and Damage Potential for Localized Fuel-Coolant Interaction

In the event that large amounts of molten fuel and sodium coolant are mixed within a fuel assembly, the integrity of the assembly structure may be jeopardized. Given the proper conditions of fuel particle size, sodium-to-fuel mass ratio, direction of energy deposition, and other pertinent conditions, the thermal energy within the fuel can be converted to damaging mechanical energy in the form of high-pressure fluid. (For an appraisal of the important parameters and the phenomena involved in the molten fuel-sodium interaction, see Appendix C.)

The approach taken to determine the amounts of fuel required to cause a wrapper can to fail was as follows: The work done by an expanding quantity of sodium mixed with various amounts of molten fuel was determined. A portion of this work energy was assumed to be directed to the wrapper can deformation; the remainder was assumed to be deposited in the kinetic energy and frictional losses in the unheated, displaced sodium. One-third of the work done by the expanding fluid was assumed to be utilized in the wrapper can deformation; this is similar to an energy deposition used by Campise¹² in an analysis of fuel-coolant interaction effects in an EBR-II driver-fuel subassembly. Two methods were used to determine the available work from a mixture of molten fuel and sodium. First, the Hicks and Menzies¹³ method was used, and second, results from the MFCDYN model were employed. The MFCDYN computer program was developed under this contract to calculate the pressure-time history and the work done in a molten fuel-sodium interaction. (However, the state of development of the program at the time of this writing was such that the results must be considered preliminary.) The code is described in Appendix E. The values of work done by a mixture of fuel and sodium were used in determining the masses of fuel required to supply the energy for wrapper can failure—the energy given in the previous section.

5.1.4.1. Available Work

Hicks and Menzies¹³ calculated the work done by a mixture of fuel and sodium in expanding to various final pressures. They assumed that fuel at 5750F and liquid sodium at 1600F and a pressure of one atmosphere are brought to thermal equilibrium at constant volume and then allowed to expand adiabatically to some final pressure (with the fuel and sodium in thermal equilibrium during the expansion process). Some of the assumptions for this calculation included (1) the sodium and fuel liquids are incompressible and have negligible specific volumes compared to the vapor phase, (2) the sodium vapor behaves as a perfect gas, and (3) the specific and latent heats are constant. The results of this calculation are shown in Figure 5-4. Dashed lines for expansion to various final pressures are shown for variations in the ratio of sodium to fuel mass.

The MFCDYN computer program treats the heat transfer variation with time starting with a mixture of fragmented fuel and liquid-phase sodium at two arbitrary (input) temperatures. The program treats the effects of the unheated sodium coolant acting as the constraint for the sodium-fuel mixture; this constraint is either acoustic or inertial. The effect of the wrapper can is not included in the model; it has been assumed that the wrapper can is a perfectly rigid body as far as its effects on the expanding fluid volume, pressure, and work are concerned. The following values of pertinent program parameters were used:

1. Initial fuel temperature — 6200F (this value was used to account for the additional energy in the fuel's latent heat of fusion, so that the actual energy corresponds to completely melted fuel at 5000F).
2. Initial sodium temperature — 1050F.
3. Distance to free surface (or constant pressure reservoir) — 4.17 ft.
4. Area for mixture expansion — 0.0867 ft² (the fuel assembly coolant flow area, or half the flow area "seen" by a mixture expanding upward and downward).
5. Heat transfer coefficient between sodium and fuel in the mixture — 20,000 Btu/h-ft²-°F.

6. Typical fragmented fuel particle radius —
0.000457 ft (139 microns).
7. Volume of fission gas in the mixture volume —
0.0 ft³.

The MFCDYN calculations for the work done by the expanding sodium-fuel mixture include the work done during the initial phase, where the rapidly heated sodium suffers an increase in pressure. Of course, the expanding system does work during the entire expansion process, but for these purposes, we considered only the work that was performed with the mixture pressure in excess of a given amount. Figure 5-4 shows the results. The maximum amount of work performed above a given pressure is indicated as higher for the MFCDYN calculations, compared to Hicks and Menzies' results. For the work done above 80 and 15 psi, the difference is a factor of two; however, the differences in fuel temperature and fuel specific heats for the two calculations account for approximately one-half of this difference.

5. 1. 4. 2. Amount of Fuel Required for Loss of Fuel Assembly Wrapper Can Integrity

We have determined the amount of fuel that would be required to cause a loss of wrapper can integrity in the event of a localized molten fuel-sodium interaction. The important considerations in this evaluation are (1) the energy absorption capability per unit wrapper can length and the required fluid pressure, (2) the failure length, (3) the work energy capability of the heated sodium, and (4) the deposition of this work energy, i. e. , whether this energy is utilized in deforming the structure or increasing the kinetic energy of the unheated, surrounding fluid.

The required fluid pressure for the loss of wrapper can integrity may range from about 80 to 2000 psi (see section 5. 1). It is reasonable to expect that a maximum fluid pressure of less than 80 psi would cause no large permanent deformations of the wrapper can. In such a case, all of the thermal energy of the fuel that is converted to work would appear in the change in kinetic energy of the unheated coolant and also in frictional losses. As mentioned in section 5. 1, the fluid pressure required to fail a perfectly circular wrapper can is approximately 2000 psi for unirradiated 304 SS at 1100F.

The failure lengths considered in this evaluation were the full assembly length and a length corresponding to three hexagonal flat widths. The value of three flat widths is important because it is considered to be the minimum axial loading length for which the flat may be treated as a uniformly loaded beam, free of end effects (end effects caused by the restraint due to the adjacent unloaded portion of the wrapper can).

As mentioned previously, the portion of available work energy assumed to be directed to deformation of the wrapper can is one-third of the total. This is strictly true only for the energy deposition associated with a shock wave. Of course, given a low enough pressure, essentially no energy is expected to be absorbed in the wrapper can, as mentioned above.

With these considerations, the required mass of fuel for the loss of wrapper can integrity becomes

$$\text{Fuel mass} = \frac{\text{Energy absorption capability} \times \text{Deformed length}}{\text{Fraction of energy directed to wrapper can deformation} \times \text{Available work energy from Na}}$$

Figure 5-5 gives the value of fuel masses for different failure lengths, failure pressures, and unit length wrapper can energy absorption values using the optimum available work values from the Hicks and Menzies calculations (optimum with respect to the sodium-to-fuel mass ratio). For comparison, the fuel masses for a 66-inch failure length are shown in Figure 5-6 for both the Hicks and Menzies and MFCDYN results. These two plots also show the equivalent number of fuel pins required to supply the fuel mass, assuming that both the active fuel (mixed oxide) and blanket material interact with the optimum amount of sodium.

5.1.5. Results and Conclusions — Local Fuel-Coolant Interaction Effects on Wrapper Can Integrity

The results of the Hicks and Menzies calculation for the available work for a mixture of molten fuel and sodium are in fair agreement with the preliminary results obtained from the MFCDYN model. Certainly, the difference of a factor of two between the two results is

no greater than some of the other uncertainties, such as the particle sizes for fragmented fuel, the heat transfer rate to the heated sodium, the effects of the presence of fission product gases, and a host of others.

One of the more important uncertainties is the length of the fuel assembly wrapper can that absorbs energy. It is reasonable to expect that a fairly long length of fuel assembly would be involved. This is because the pressure distribution from the fuel-sodium interaction region to the constant pressure plenum is linear for the case of inertial constraint. This may be seen from the following force balance on an incompressible fluid slug with no friction:

$$\Sigma F = m\ddot{x}/g_c,$$

$$-\int_{p_{\max}}^p A dp = \int_0^x \rho \frac{A\ddot{x}}{g_c} dx,$$

or

$$p = p_{\max} - \rho x\ddot{x}/g_c,$$

where

- p = pressure,
- $p_{\max}$ = pressure in fuel-sodium mixture,
- x = displacement,
- $\ddot{x}$ = slug acceleration,
- ρ = fluid density,
- g_c = gravitational conversion factor.

The acceleration of the liquid slug can be expressed in terms of the slug length (L) and constant pressure plenum (located, say at the pin bundle exit), which gives, after rearrangement,

$$\frac{p_{\max} - p}{p_{\max} - p_{\text{ref}}} = \frac{x}{L}$$

where p_{ref} is the constant reference pressure. For large values of $p_{\max}$,

$$p_{\max} - p_{\text{ref}} \approx p_{\max},$$

and the proportion of the wrapper can length adjacent to the slug that experiences a pressure of at least $p_{\max}/10$ is

$$\frac{p_{\max} - (p_{\max}/10)}{p_{\max}} = \frac{x}{L} = 0.9.$$

Therefore, during the inertial constraint phase of the expansion of heated sodium for large peak pressures, most of the assembly wrapper can is available for energy absorption.

From Figure 5-6 then, which corresponds to a deformation length of 66 inches (the length of the fuel pin bundle), the amount of fuel required for wrapper can failure can be approximated. At a fluence of 10^{22} nvt, the changes in yield strength and ductility from the unirradiated state cause a reduction in energy absorption per unit length of 1500 to 940 ft-lb/in. (see section 5.1 for the yield strength and ductility values at this fluence and for the plot of energy absorption for these property values). For 940 ft-lb/in. and a 100 psi fluid pressure for wrapper can deformation, both the Hicks and Menzies and the MFCDYN results predict an excess of one fuel pin required for the loss of fuel assembly integrity for a 66-inch deformation length.

The mass of fuel required for the loss of fuel assembly wrapper can integrity is very likely greater than the amount in one fuel pin. This is based on the following qualitative considerations:

1. The effect of deformation and energy absorption by the wrapper can would have a strong effect on limiting peak fluid pressures in an actual event. This was not accounted for in the model.

2. Considerable support and energy absorption might be offered by adjacent fuel assemblies in the case of large wrapper can deformations.

3. The calculations performed here have assumed complete fragmentation of the available molten fuel.

4. The material properties of the wrapper can were taken to correspond to those at 1100F. This is essentially the maximum temperature, whereas the average is approximately 950F.

5. The sodium-to-fuel mass ratio was taken to be the ratio that resulted in the optimum (maximum) sodium work energy.

6. Energy absorption by the fuel pins was not accounted for.

7. No account was made for the pressure-limiting effects of fission gas or other voids in the pins or pin bundle.

8. The area for expansion of the heated sodium was taken to be half of the coolant flow area "seen" by a mixture expanding upward and downward in the assembly. Thus, the peak pressures should be lower than those predicted by the MFCDYN calculation.

9. No account was taken for probable sodium condensation on the thin film of "cold" sodium left on the fuel pins within an expanding vapor bubble. This could be quite important, as shown in section 3.4.

A reasonable guess concerning the effects of these factors on the amount of permissible molten fuel within a fuel assembly would be to increase the fuel mass by an order of magnitude over that given in Figure 5-6.

5.2. Multiple Fuel Assemblies

The motions and deformations of interacting, externally loaded assemblies were analyzed by developing and solving the differential equation for predicting their coupled response to a forcing function such as might result from a pressure rupture of a single assembly can. The equations were developed by equating the forces acting at each mass point of a discrete mass system representing the core geometry in idealized form. Utilizing the computer code MIMIC,¹⁶ these equations were

solved for the forces and displacements associated with the motion of each mass point at specific instances of time. The program provides for calculating the effects of elastic and linearly varying inelastic structural deformation, clearances between structures, and fluid inertia and friction on the motion of each mass point.

5.2.1. Model Description

The actual system for which the model was developed is B&W's LMFBR Follow-On Study reference core. It consists of hexagonal fuel assemblies, blanket assemblies, and control rod guide tubes arranged in a modified hexagonal configuration. Each assembly and guide tube is located and supported at its lower end by a grid plate and is vertically held and positioned at its top by a holddown structure. The system is described in detail in BAW-1328, Volume 2, section 4.

Figure 5-7 relates the model to the actual system. This figure shows a sectional view of the Follow-On Study reference core and indicates the use of the core symmetry to reduce the calculational effort required to predict the structural response of the core structures to suddenly applied loads; i.e., because of symmetry, the motion of one-sixth of the assemblies in a given array indicate the motion of all assemblies in the array.

Figures 5-8a and 5-8b show the assumed loading conditions for the individual members of the system. Figure 5-8a shows how each member is supported, and Figure 5-8b shows how structural forces are transmitted from one member to another.

The model used to present this system can be described as a triangular array of discrete masses interconnected and attached to a fixed reference point by springs and dashpot mechanisms (Figures 5-9a and 5-9b). Figure 5-9a shows the geometric relationships of the mass points to be used during the analysis as well as the lines of symmetry referred to previously. It also shows the position of the force-generating function relative to the mass point array being analyzed. The paths along which the input force is transmitted are shown by the heavy directed lines. Figure 5-9b illustrates the model used in formulating the equations for calculating the interacting forces between the masses.

5.2.2. Motion Equations

For each mass point in a discrete mass system, an equation of motion can be written as

$$MA = \Sigma F,$$

where
M = mass,
A = acceleration,
F = force.

Two types of forces must be allowed for apart from those of inertia: First, those inherent to the system, such as spring and fluid friction forces, and second, those which are applied by an outside agency. In this case, the latter are the forces generated as a result of a molten fuel-sodium interaction.

Although the forces resulting from a molten fuel-sodium interaction are the ultimate source of the motions that we wish to predict, it should be kept in mind that these forces are applied only to the first mass of the array shown in Figure 5-9a. This implies that the forces acting throughout the system are the result of mass movement. Therefore, the forces resisting the motion of one mass can be the input forcing function for an adjacent mass. Applying the equation above to a mass point of the array shown in Figure 5-9a with this in mind results in the following typical relationship:

$$\text{Mass} \times \text{acceleration} = \begin{matrix} \text{Input} \\ \text{forcing} \\ \text{function} \end{matrix} - \begin{matrix} \text{Structural} \\ \text{resistance} \end{matrix} - \begin{matrix} \text{Fluid} \\ \text{resistance} \end{matrix}.$$

Applying this relationship to a mass point system coupled and arranged as shown in Figure 5-9a produces the following system of equations:

$$M0 \cdot A0X = F - F01 - F01F.$$

$$M1 \cdot A1X = F01 + F01F - F13 - F13F - 2 \cdot (F12 + F12F) \cdot CB - F1.$$

$$M2 \cdot A2A = (F12 + F12F - F26 - F26F) \cdot CT - F2.$$

$$M3*A3X = F13 + F13F - F35 - F35F - 2.*(F36 + F36F)*CB - F3.$$

$$M4*A4A = (F64 + F64F)*CT - F4F - F4.$$

$$M5*A5X = F35 + F35F - F57 - F67F - 2.*(F58 + F58F)*CB - F5.$$

$$M6*A6A = (F26 + F26F + F36 + F36F - F64 - F64F - F68 - F68F)*CT - F6.$$

$$M7*A7X = F57 + F57F - F7F - F7.$$

$$M8*A8A = (F68 + F68F + F58 + F58F)*CT - F8F - F8.$$

The fluid force equations can be written as follows:

$$F01F = B01*A0X - B01*A1X - C01.$$

$$F13F = B13*A1X - B13*A3X - C13.$$

$$F12F = B12*A1X*CB - B12*A2A*CT - C12.$$

$$F26F = B26*A2A*CT - B26*A6A*CT - C26.$$

$$F35F = B35*A3X - B35*A5X - C35.$$

$$F36F = B36*A3X*CB - B36*A6A*CT - C36.$$

$$F64F = B64*A6A*CT - B64*A4A*CT - C64.$$

$$F57F = B57*A5X - B57*A7X - C57.$$

$$F58F = B58*A5X*CB - B58*A8A*CT - C58.$$

$$F68F = B68*A6A*CT - B68*A8A*CT - C68.$$

Substituting these equations into the equations of motion and solving for the acceleration terms, we obtain the following:

$$A0X = (F - F01 + B01*NP + C01)/[M0 + B01*(1 - N2P)].$$

$$A1X = NP + N2P*A0X.$$

$$A2A = LP + L2P*A1X.$$

$$A3X = K3P + KP*A2A + K2P*A1X.$$

$$A4A = CP + C2P*A6A.$$

$$A5X = I3P + IP*A2A + I2P*A3X.$$

$$A6A = H3P + HP*A3X + H2P*A2A.$$

$$A7X = BP + B2P*A5X.$$

$$A8A = AP + A2P*A5X + A3P*A6A.$$

The simultaneous solution of these equations and the integration required to determine the velocities, displacements, and corresponding structural and dynamic forces are accomplished by the computer code MIMIC.¹⁶ A complete printout showing all the mathematical statements needed to effect the problem solution is given in Appendix D in the MIMIC programming language.

5.2.3. Structural Stiffness Characteristics

The structural forces as a function of displacement are evaluated by the individual fuel assembly stiffness coefficients. Figures 5-10 and 5-11 show the characteristic curves used for this force-displacement relationship.

For deformation in the elastic region, the crushing and bending stiffness coefficients are calculated from the configuration shown in Figure 5-8. The bending stiffness coefficient is calculated by assuming that the beam deflections are proportional to the magnitude of the applied loads. The crushing stiffness coefficient is calculated on the basis that the stresses due to the loading are appreciably affected by the deformation. This results in a coefficient that varies with displacement. To simplify the use of this coefficient, a single representative value is used in the structural force equations. The values for each coefficient are calculated as follows:

$$K = P/D,$$

where K = stiffness coefficient,
 P = applied load,
 D = deflection.

The bending P/D ratio is calculated by assuming that the fuel assembly acts as a beam with one end fixed, one end supported, and a center load (see Figure 5-8a). The resulting equation is

$$P/D = 768 EI/7l^3$$

where E = Young's modulus,
 I = second moment of area of fuel assembly,
 l = length of beam between supports.

The crushing P/D ratios per inch of fuel assembly length are calculated by assuming that the fuel assembly wall is loaded as shown in Figure 5-8b and noting that the wall acts as a closed structure. The resulting equations are

$$\frac{M_o}{EI} = \frac{(N/M)[6 \cos (ML/3) - 6 - ML \sin (ML/3)]}{ML \cos (ML/3) + 6 \sin (ML/3)};$$

$$D = (N/M^3) \sin (ML/3) - NL/3M^2 - M_o/EIM^2 \\ + (M_o/EIM)^2 \cos (ML/3);$$

$$S = \sqrt{3P/2T + (M_o/EI)(TE/2)};$$

where E, P, D = (defined above),
 M_o = moment at non-loaded corner shown in Figure 5-8b.
 S = stress,
 $N = P/2EI$,
 $M = (N \sqrt{3})^{1/2}$,
 L = one-half of the perimeter of the fuel assembly can wall shown in Figure 5-8b,
 T = fuel assembly can wall thickness,
 I = second moment of area of fuel assembly can wall.

5.2.4. Fluid Force Equations

The equations for predicting the effects of core sodium inertia and friction on the motion of the core structures are obtained by considering the changes in the fluid pressure. This can be determined as a function of the change in the available fluid flow area of the individual fuel assemblies. Since the change in flow area can be

expressed as a function of the relative motion of the center and one side of a fuel assembly, the fluid force equations can be written in terms of the relative acceleration, velocity, and displacement of these points. The following equation expresses these relationships:

$$F = CW^2B \left[\frac{L}{2A} + \frac{L_o - L}{A_o} \right] \frac{L^2 A_c}{4G_c} \\ + CW^3B \left[\frac{1}{A^2} + \frac{F_r L}{D_e A^2} + F_r (L_o - L) D_e A_o^2 \right] \frac{L^3 V^2}{8G_c}$$

where, in terms of section 5.2.5,

$$\begin{aligned} C &= AB, \\ W &= AC / \sqrt{3}, \\ B &= AD, \\ L &= L_{ab}, \\ A &= A_o - (W) * (DX_{ab} / 2), \\ A_o &= AH, \\ L_o &= AG, \\ A_c &= Aad - Abd, \\ G_c &= AE, \\ F_r &= AJ, \\ D_e &= AK, \\ V &= DV_{ab}. \end{aligned}$$

5.2.5. Notations for Multiple-Assembly Model Equations

- $a = 0, 1, 2, \dots$ = position or mass point ident numbers.
 $b = 0, 1, 2, \dots$ = position or mass point ident numbers.
 $d = X, Z, A$ = direction identification.
 $\theta = \text{THETA}$ = angle between directions X and A or Z and A, radians.
 $\beta = \text{BETA}$ = angle between directions X and Z, radians.
 $CT = \cos (\theta).$
 $CB = \cos (\beta).$
 Ma = quantity of mass at a, slugs.
 Aad = acceleration of mass a in the d direction, fps.

AJ = fluid friction factor.
 AK = fluid flow area equivalent diameter, ft.
 F = input forcing function.
 T = time, seconds.
 FO = maximum input force, lb.
 DTF = time interval during which F influences system motion, seconds.
 Fab = structural force between adjacent masses, lb.
 FabF = fluid force between adjacent masses, lb.
 Fa = structural force between mass a and a fixed reference point, lb.
 DXa = displacement of mass a relative to a fixed reference point, ft.
 DTda = velocity of mass a in the d direction, fps.
 DVab = relative velocity between adjacent masses, fps.
 Ka = coefficient representing bending stiffness of the structure supporting mass a, lb/ft.
 Kab = coefficient representing crushing stiffness of structure between adjacent masses, lb/ft.
 KabM = coefficient representing crushing stiffness between adjacent masses during impact, lb/ft.
 G = initial clearance between structures supporting adjacent masses, ft.
 DXabY = relative displacement between adjacent masses at which construction materials of supporting structures reach yield stress, ft.
 DXabR = relative displacement between adjacent masses at which construction materials of supporting structures reach ultimate stress, ft.
 DXabM = relative displacement between adjacent masses at which they impact, ft.
 DXaY = displacement of mass a relative to fixed reference point at which construction materials of supporting structures reach yield stress, ft.
 DXaR = displacement of mass a relative to fixed reference point at which construction materials of supporting structures reach ultimate stress, ft.
 FaF = fluid force between mass a and an adjacent fluid, lb.
 AC = assembly width across flats, ft.
 AD = fluid density, lb/ft³.
 AE = gravitational constant, lbf/ft (ft/s²).

AB = empirical coefficient.

Lab = contact length between assemblies a and b, ft.

AG = length between assembly end supports, ft.

AH = normal sodium-flow area, ft².

A3,D3,C2, = limiting functions to make the range of certain vari-
E7,I6,H5, ables conform to system's physical limits.
N,P7,R8,Q8

IMPLS = impulse imparted to system by input forcing function.

5.2.6. Results From Multiple-Assembly Structural Model

Calculations were made using the multiple-fuel-assembly model to determine the structural effects of variations in applied impulse. The variation in applied loading time ranged up to 4.5 ms, and the peak loading in the first assembly was up to 150,000 pounds. The loading variation with time was taken to be triangular, with the peak load occurring at half the load duration time. The 150,000 pounds would correspond to a uniform fluid pressure of 630 psi over a 66-inch length of one hexagonal can flat.

The results of the calculations show that crushing and bending failure of one row of fuel assemblies is likely if the impulse applied to each assembly in the row exceeds 37.5 lb-s. However, this was obtained with the assumption that failure occurs once the yield stress of the material is exceeded. In this case, one row consists of six fuel assemblies surrounding the site where the impulse-generating force originated.

Figure 5-12 shows the variation of the displacement of the center of mass of one of these assemblies with applied impulse. The displacement is a nonlinear and discontinuous function of impulse. It is nonlinear because the impulse is calculated and applied to the assembly in incremental time steps, rather than being calculated and applied to the structure as a total quantity, and because the forces resisting motion are nonlinear. The discontinuity is a result of the assumption (made in formulating the calculational model) that structural and fluid forces associated with the motion of an individual fuel assembly cease whenever the displacement causing rupture is exceeded. As shown by the figure, the discontinuity begins with bending failure of the

assembly and ends when the mass of the failed assembly contacts an adjacent unfailed assembly. However, the most useful conclusion to be drawn is that the model predicts, for initial impulse loadings of less than 37.5 lb-s, that gross mechanical core damage will be limited to seven fuel assemblies.

Results from the MFCDYN calculational model indicate that the impulse applied to a rigid wall might approach 3000 lb-s if the interacting fuel mass is as large as 1.0 lb or 454 g; this is several orders of magnitude larger than that mentioned above. However, the applied impulse from such a fuel-sodium interaction should be substantially less than 3000 lb-s. A large part of this impulse would be applied internally to the first fuel assembly before the loss of its integrity. Also, the subsequent displacement of the surrounding assemblies should be very effective in reducing the applied impulse because of the pressure reduction accompanying an increase in the volume of the heated sodium. This "feedback" effect has not been treated since it would require a coupling of the MFCDYN and structural response models. In addition, the arguments given in section 5.1.5 for expecting smaller values of work energy from fuel-sodium mixtures should also apply to the expectation of lower pressures and impulses.

Figure 5-13 shows the reactivity (Δk) as a function of impulse. The reactivity values were calculated for fuel movements only and are valid only if the fuel movements occur at the interface of core zones 2 and 3. The first step in the curve indicates the structural failure of one row of assemblies, and the second indicates that two rows have failed. The construction material properties used for these calculations are as follows:

Modulus of elasticity, psi	2.2×10^7
Ultimate strength, psi	5.0×10^4
Yield strength, psi	5.0×10^4
Ductility, %	Nil

Figure 5-1. Pressure Range for Plastic Deformation of a Fuel Assembly Duct Vs Fluence

Figure 5-2. Effects of Neutron Fluence on Energy Absorption Capability for Plastic Deformation of a Unit Length of Fuel Assembly Wrapper Can

Figure 5-3. Energy Absorption Capability per Unit Length —
Effects of Material Yield Strength and Ductility

Figure 5-4. MFCDYN Results Vs Results of Hicks and Menzies for the Work Done by a Fuel-Sodium Mixture

Figure 5-5. Fuel Mass Required for Loss of Integrity of Fuel Assembly Wrapper Can for a Fuel-Sodium Interaction

Figure 5-6. MFCDYN Results Vs Result of Hicks and Menzies for Mass of Interacting Fuel Required for Loss of Integrity of the Assembly Wrapper Can

Figure 5-7. Core Arrangement (A31099E)

SECTION Z-Z (SEE DWG NO. A311002)
SCALE 1 1/2" = 1'-0"

LEGEND

- FUEL ASSEMBLY - 288 REQ'D.
- CONTROL ROD - 25 REQ'D.
(S - SAFETY ROD POSITION)
- BLANKET ASSEMBLY - 138 REQ'D.

Figure 5-8a. Bending Force Configuration

Figure 5-8b. Crushing Force Configuration

Figure 5-9a. Calculational Model Array

Figure 5-9b. Calculational Model

Figure 5-10. Crushing Stiffness Coefficient

Figure 5-11. Bending Stiffness Coefficient

Figure 5-12. Displacement Vs Impulse Center of Mass of First Assembly

Figure 5-13. Reactivity Vs Impulse Center of Mass of First Assembly

REFERENCES

- ¹ 1000-MWe LMFBR Follow-On Study, BAW-1328, Vol. 1, Lynchburg, Va., March 1969.
- ² LMFBR Program Plan, Vol. 10, Safety, LMFBR Program Office, WASH-1110, Argonne National Laboratory, Chicago, Ill., Aug. 1968.
- ³ Fuel Element Failure Propagation Program Plan, ANL-MET-01, Argonne National Laboratory, Aug. 1969.
- ⁴ B. M. Hoglund, Argonne National Laboratory, Personal Communication.
- ⁵ R. W. Lockhart and R. C. Martinelli, "Proposed Correlation of Data for Isothermal Two-Phase, Two-Component Flow in Pipes," Chemical Engineering Progress, 45, No. 1, Jan. 1949, pp 39 - 48.
- ⁶ D. S. Rowe, Cross-Flow Mixing Between Parallel Flow Channels During Boiling, Part I: COBRA — A Computer Program for Coolant Boiling in Rod Arrays, BNWL-371, Battelle, Mar. 1967.
- ⁷ L. S. Tong, H. Chelemer, and E. A. McCabe, Jr., Hot Channel Factors for Flow Distribution and Mixing in Core Thermal Design, WCAP-2211, Feb. 1963.
- ⁸ J. T. Rogers and N. E. Todreas, "Coolant Interchannel Mixing in Reactor Fuel Rod Bundles, Single-Phase Coolants," Heat Transfer in Rod Bundles, ASME Annual Winter Meeting, N. Y., Dec. 1968.
- ⁹ R. Delacier, Oak Ridge National Laboratory, Personal Communication.
- ¹⁰ F. R. Farmer, et al., An Appreciation of Fast Reactor Safety, Safeguards Division, AHSB, United Kingdom Atomic Energy Authority (1970).

- ¹¹ P. G. Bentley, The Problems of Detection of Blockages in the Coolant Passages of a Fast Reactor by Measurement of Coolant Temperature, UKAEA, TRG Report 488L (1963).
- ¹² A. V. Campise, Fault Propagation in an EBR-II Mark-II Driver-Fuel Subassembly, ANL-7560, Argonne National Laboratory, Mar. 1969.
- ¹³ E. P. Hicks and D. C. Menzies, "Theoretical Studies on the Fast Reactor Maximum Accident," Conference on Safety, Fuels, and Core Design in Large, Fast Power Reactors; ANL-7120, Argonne National Laboratory, Oct. 1965.
- ¹⁴ S. J. Stachura, AEC-EURATOM Fast Reactor Exchange Program, Report No. 17, Mar. - Apr. 1969.
- ¹⁵ R. S. Moen, Fast Flux Test Facility Materials Design Data, BNWL-891, Battelle Memorial Institute, Oct. 1968.
- ¹⁶ Control Data MIMIC, Control Data Corporation, Publication No. 44610400, Apr. 1968.

APPENDIX A
Flow Blockage Model Description

1. General

The model developed for investigating the effects of flow blockage in a pin bundle essentially solves for the flow distribution in and around the blockage. This is a boundary value problem where the flow distributions are adjusted to be consistent with the upstream and downstream boundary values of total or static pressure. Also, the radial or transverse flows must satisfy the pressure-flow relationships for adjacent channels. From the resulting flow distribution, the fluid flow energy equation is used to calculate the temperature distributions of the bulk coolant in the channels.

The geometry considered is that of up to 12 parallel, interconnected channels in a 1/12 segment of a group of pins centered about the blockage. These 12 equivalent channels actually represent 24 channels in the array. The number of blocked channels may number up to 11 equivalent channels. An error is introduced, however, if a large proportion of the available channels are blocked, resulting in an appreciable flow disturbance in the outermost (twelfth) channel.

The type of blockage that can be considered is limited to one where a number of adjacent channels are blocked over a given axial length. The severity of blockage in each channel may be varied; i. e., the flow area and hydraulic diameter for each channel are to be specified within the blockage zone.

The problem of determining flow distribution is actually separated into two boundary value problems—one upstream from the blockage and one downstream. The upstream boundary value problem involves upstream flow distribution iterations to arrive at a flow and pressure distribution at the axial elevation of the blockage outlet to agree with the downstream solution (within some allowable error). Only one upstream node for crossflow between channels is considered. This node is positioned directly upstream from the blockage and allows for variation in the axial length of the crossover node.

The upstream flow distribution is established iteratively by determining how the flow splits from a single upstream channel to two downstream channels. The flows are adjusted until the calculated pressures at the branch point agree to within a certain error. The calculated

pressures are those determined by calculating the pressure drops to the common branch point, starting at the bundle inlet and outlet boundaries.

The downstream flow distributions are adjusted to correspond to the channel pressure differences at the axial plane of the blockage outlet. The axial shape of the channel crossflows (w) is as follows:

$$w = w_o \left(1 - \frac{x}{L}\right)^n \quad (A-1)$$

where

- w_o = calculated crossflow at blockage outlet plane,
- x = downstream displacement measured from blockage outlet plane,
- L = distance from blockage outlet to pin bundle outlet,
- n = exponent.

The value of n is selected to make the outlet velocity from the outermost blocked channel equal to that for the innermost unblocked channel. This guarantees that the pressure differences between these two channels will be consistent with the crossflow direction at every axial position. The channel crossflows and pressures at the axial position of the blockage outlet are solved simultaneously by solving the algebraic expressions for channel pressures expressed in terms of crossflow values.

2. Finite Difference Forms of the Governing Equations

The equations of continuity, momentum, and energy are similar to those given by Rowe.* These are obtained by simple mass, momentum, and energy balances on static control volumes. Single-phase flow is considered, and the turbulent momentum exchange between channels is neglected.

- - - - -
 *D. S. Rowe, Cross-Flow Mixing Between Parallel Flow Channels During Boiling, Part I: COBRA — A Computer Program for Coolant Boiling in Rod Arrays, BNWL-371, Battelle, Mar. 1967.

2. 1. Continuity Equation

The mass balance illustrated in Figure A-1 becomes

$$\dot{m}_{out} = \dot{m}_{in} + (w_{ji} - w_{ik})\Delta L \quad (A-2)$$

where w is the crossflow per unit length and ΔL is the axial segment length.

2. 2. Axial Momentum Equation

Upon inspection of Figure A-2, a force and time rate change of momentum equation,

$$\Sigma F_x = \frac{1}{g_c} \frac{d(mV)}{dt} = \frac{1}{g_c} [(\rho A V_{out}) V_{out} - (\rho A V_{in}) V_{in}], \quad (A-3)$$

becomes

$$[A_i p_i - W_i - F_i - A_i (p_i + dp_i)] = \frac{1}{g_c} \left\{ [f_m w_{ik} u_i \Delta L + \dot{m}_i u_i + d(\dot{m}_i u_i)] - (\dot{m}_i u_i + f_m w_{ji} u_j \Delta L) \right\} \quad (A-4)$$

where

$$w_i = \gamma A_i \Delta L, \quad (A-5)$$

and since

$$\frac{d(\dot{m}_i u_i)}{dx} = \frac{d(\dot{m}_i^2 / \rho A_i)}{dx} = 2u_i \frac{d\dot{m}_i}{dx} - A_i u_i^2 \frac{d\rho_i}{dx}. \quad (A-6)$$

Substituting equation A-6 into A-3 gives

$$-dp_i = \Delta L \left\{ \gamma + \frac{F_i}{A_i} + \frac{1}{g_c} A_i \left[w_{ik} (f_m u_i - 2u_i) + w_{ji} (2u_i - f_m u_j) - A_i u_i^2 \frac{d\rho}{dx} \right] \right\}. \quad (A-7)$$

The factor f_m is included in an attempt to account for the nonuniform velocity distributions. (The transverse momentum flux is determined by some fraction of the average channel axial velocity since the axial velocity in the area between pins is less than the average.) The frictional force (F_i) is given by

$$F_i = \frac{f_p V^2 A_i}{2g_c D_e} \quad (A-8)$$

where the friction factor (f) in the model is taken from the Blasius equation,

$$f = \frac{0.316}{N_{Re}^{0.250}} \quad (A-9)$$

2.3. Radial Momentum Equation

The radial momentum equation used is as follows:

$$P_i - P_j = \frac{K w_{ij} |w_{ij}|}{2g_c \rho c^2} \quad (A-10)$$

where K is an empirically determined crossflow resistance coefficient.

2.4. Thermal Energy Balance

After inspection of Figure A-3, the thermal energy equation may be written as

$$\dot{m}_{out} h_{out} + w_{ik} \Delta L h_{in} = \dot{m}_{in} h_{in} + w_{ji} \Delta L h_j + \dot{q}_i \quad (A-11)$$

Rearranging A-11 and employing equation A-2, equation A-11 becomes

$$h_{out} = \frac{\dot{m}_{in} h_{in} + w_{ji} \Delta L h_j + \dot{q}_i - w_{ik} \Delta L h_{in}}{\dot{m}_{in} - w_{ik} \Delta L + w_{ji} \Delta L} \quad (A-12)$$

In some portions of a fuel pin bundle where forced crossflow is not significant but turbulent crossflow is, the energy equation becomes

$$h_{out} = \frac{\dot{m}_{in} h_{in} + w'_{ji} \Delta L (h_j - h_i) + \dot{q}_i - w'_{ik} \Delta L (h_i - h_k)}{\dot{m}_{in}}, \quad (A-13)$$

where $\dot{m}_{in} = \dot{m}_{out}$ since turbulent mixing does not involve a net mass flow between adjacent channels.

3. Nomenclature for Flow Blockage Model

- A = area of subchannel.
- c = pin-to-pin ligament.
- D_e = equivalent hydraulic diameter.
- F = frictional force.
- f = friction factor.
- f_m = factor for crossflow momentum flux.
- g_c = gravitational conversion factor.
- h = fluid specific enthalpy.
- i, j, k = subchannel subscripts.
- K = crossflow resistance coefficient.
- L = axial channel length from blockage outlet to bundle outlet.
- ΔL = length of control volume.
- m = mass flow rate.
- N_{Re} = Reynolds number.
- n = exponent.
- p = pressure.
- $\dot{q}$ = heat rate.
- u, V = axial fluid velocity.
- W = gravitational force.
- w = crossflow per unit length.
- w_o = crossflow at blockage outlet elevation.
- w' = turbulent crossflow per unit length.
- x = axial displacement.
- γ = fluid specific weight.
- ρ = fluid density.

Figure A-1. Continuity Schematic Diagram

Figure A-2. Momentum Balance

Figure A-3. Thermal Energy Balance

4. BLOCKO Computer Code Edit

```

PROGRAM BLOCKO (INPUT,OUTPUT,TAPE6=OUTPUT,TAPE11=INPUT,PUNCH)
C  NSTAGE IS NO. OF STAGES TAKEN TO REDUCE BLOCKAGE FLOW AREAS
COMMON /GEOMET/
1  NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2  LENGTHB ,LOCBLOK ,PINDIA ,PINPICH ,CLADT ,
3  PINNO(14) ,AREA(14,41) ,EQUARIA(14) ,
4  ARATIO(14) ,CRATIO(14) ,BUPAREA(14) ,
5  BYDCWNA(14) ,SIGMA(14) ,KB(14) ,
6  IB(14) ,JB(41) ,DUM ,
7  AXLOC(41)
COMMON /BLOC/
1  BFLOWA(14) ,DEQB(14) ,
2  NOBLUKD ,NOCONT ,NOEXP ,
3  ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON /GENL/
1  ITRUBLE ,M ,N ,JTRUBLE(10) ,PCRIT
2  ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,LQHYD
3  ,PEAKPOW ,DIVL ,PP ,BLUP ,GSUBC
4  ,XNOEXP ,XNOCONT ,STAGINP ,PINCHES ,DELTAP
COMMON /POWR/
1  POWNORM(41) ,POWLIN(41) ,QPERDIV(14,41) ,
2  POWVAR(14) ,TOUT ,TIN ,
3  VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1  P(14,41) ,U(14,41) ,FSUBC ,
2  FSUBG ,FSUBH ,REYNOS(14,41) ,
3  FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4  W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5  AKSUBE(14) ,PINC(41) ,TRY(14,3) ,
6  C(14) ,POUT ,WCRY ,
7  ,RK(13,41) ,RCONST ,RCONSTL ,
8  ,WMAX(14) ,ENTH ,BETA
C  STOP 10 IS IN BLOCKO, STOP 11 IN
C  JTRUBLE(1), PRINT IN SUBROUTINE SETUP
DIMENSION PSAVE(14)
WRITE(6,50)
CALL PUTMEON
10 CALL SETUP
CALL ENERGY
CALL PROPS
NSTAGE=1
300 IF (NSTAGE .GT. ISTAGE) GO TO 999
NSET =20
IF (NSTAGE .EQ. ISTAGE) NSET = 50
C  CHANGE BLOCKAGE FLOW AREAS
WRITE(6,52)
CALL BLOKIT
C  PRESERVE CONTINUITY
WRITE(6,53)
CALL CONTIN(2, NOCHAN, 2, NODIV)
C  CALCULATE UPSTREAM BYPASS FLOW DISTRIBUTIONS
NDO = 0
NIT = 0
500 NDO = NDO + 1
IF (NDO .GT. NSET) GO TO 900
NIT = NIT + 1
IF (NIT .NE. 3) GO TO 550

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```

      CALL ENERGY
      CALL PROPS
      NIT = 0
550  CONTINUE
      WRITE(6,54)
      CALL UPPIT
      NUP = LOCBLOK + LENGTH3
      DO 600 MJ = 2,NOCHAN
      PSAVE(MJ) = P(MJ,NUP)
600  CONTINUE
      WRITE(6,56)
      CALL DOWNIT
      PCRY = .05
      IF (NSTAGE .GE. NSTAGE) PCRY = .002
      DO 700 MJ = 2,NOCHAN
      PCHECK = ABS((P(MJ,NUP)-PSAVE(MJ))/P(MJ,NUP))
      IF (PCHECK .GT. PCRY) GO TO 500
      IF (NDO .LT. 3) GO TO 500
700  CONTINUE
      UFRAC = (U(NUMBLOK+1,NODIV)-U(NUMBLOK,NODIV)) / U(NUMBLOK+1,NODIV)
      IF (UFRAC .GT. 0.) ENTH = ENTH*.95
      GO TO 950
900  WRITE(6,901) NSTAGE
901  FORMAT (* BOMBED - 100 MANY UPSTREAM / DOWNSTREAM ITERATIONS AT NS
      NSTAGE = *,1I3)
      CALL OUT2(ITRUBLE)
      STOP 21
950  CONTINUE
      WRITE(6,623) ENTH
623  FORMAT (* ENTH = * , 1F8.2)
      WRITE(6,951) NSTAGE
      NSTAGE = NSTAGE + 1
951  FORMAT (* CALL OUT2 FROM MAIN ,NSTAGE= *,1I3)
      IF (JTRUBLE(10) .EQ. 0) GO TO 990
      CALL OUT2(1)
990  CONTINUE
      GO TO 300
999  CONTINUE
      WRITE(6,55)
      PRINT OUTPUT
      CALL OUT2(3)
      GO TO 2000
2000 CONTINUE
      STOP 20
      50 FORMAT (25H CALL PUTNEON FROM MAIN      )
      52 FORMAT (25H CALL BLOKIT FROM MAIN      )
      53 FORMAT (25H CALL CONTIN FROM MAIN      )
      54 FORMAT (25H CALL UPPIT FROM MAIN      )
      55 FORMAT (25H CALL OUT2 FROM MAIN      )
      56 FORMAT (25H CALL DOWNIT FROM MAIN      )
      END

```

```

COMMON
/GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGTHB ,LOCBLOK ,PINDIA ,PINPLCH ,CLADT ,
3 PINNG(14) ,AREA(14,41) ,EQUARIA(14) ,
4 ARATIO(14) ,CRATIO(14) ,SUPAREA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,KB(14) ,
6 IB(14) ,JB(41) ,DUM ,
7 AXLOC(41)
COMMON
/BLOC/
1 BFLOWA(14) ,DEQB(14) ,
2 NOBLOKD ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON
/GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,PCRIT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKPQW ,DIVL ,PF ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCONT ,STAGINP ,PINCHES ,DELTAP
COMMON
/POWR/
1 POWNORM(41) ,POWLIN(41) ,QPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,TIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON
/FLOW/
1 P(14,41) ,U(14,41) ,REYNOC ,
2 FSUBG ,FSUBH ,REYNOS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,TFY(14,3) ,
6 C(14) ,POUT ,WCRY ,
7 ,RK(13,41) ,RCONST ,RCONSTL
8 ,WMAX(14) ,ENTH ,BETA

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C      EXPLANATION      OF VARIABLE NAMES
C      GEOMETRY
C      NOCHAN      IS      NUMBER OF CHANNELS (MAX OF 12)
C      PLENGTH      IS      BUNDLE LENGTH IN INCHES
C      LENGTHA      IS      ACTIVE FUEL LENGTH (IN)
C      NODIV      IS      NUMBER OF AXIAL DIVISIONS (POINTS - 1 )
C      NUMBLOK      IS      NUMBER OF BLOCKED CHANNELS
C      LENGTHB      IS      BLOCKED LENGTH (FIXED PT.) IN DIVISIONS
C      LOCBLOK      IS      LOCATION OF UPSTREAM END OF BLOCKAGE
C      PINDIA      IS      PIN DIA IN INCHES
C      PINPICH      IS      PIN PITCH (IN)
C      CLADT      IS      CLADDING THICKNESS (IN)
C      FSUBC      IS      FACTOR FOR CROSSFLOW MOMENTUM (LT OR LT 1.0)
C      BLOCKAGE DATA
C      BFLOWA(14)      IS      BLOCKAGE FLOW AREA /UNIT CHANNEL (IN**2)
C      DEQB(14)      IS      BLOCKAGE FLOW EQUIV HYD DIA (IN.)
C      IStage      IS      NO. OF BLOCKAGE STAGES (USUALLY 10)
C      NOBLOKD      IS      NO. OF CHANNEL DIAS FOR BYPASS FLOW
C                        LENGTH (20 TO 40)
C      NOEXP      IS      NO. OF EXPANSION LOSSES PER DIVISION (MIN 0)
C      NOCONT      IS      NO. OF CONTRACTION LOSS PER DIVISION (MIN 0)
C      ISYM      IS      0 FOR SYMMETRY AXIS IN CHANNEL
C                        1 FOR SYMMETRY AXIS IN PIN
C      POWER, FLOW AND PRESSURE DETERMINATIONS
C      ITRUBLE      IS      1 FOR WRITING OF PERTINENT VARIABLES
C      POWNORM(41)      IS      NORMALIZED POWER AT INLET TO EACH DIVISION

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C      (NORMALIZED TO PEAK , MAX OF 1.0 )
C      PEAKPOW      IS      PEAK POWER IN KW/FT
C      POWVAR(14)   IS      CHANNEL POWER FRACTION OF EQUIV CHANNEL TWO
C      TOUT         IS      NORMAL CHANNEL OUTLET TEMP (DEG F)
C      TIN          IS      INLET TEMP (DEG F)
C      VARPROP      IS      VARIABLE PROPERTIES OPTION IF .GT. 0.
C      POUT         IS      OUTLET PRESSURE IN PSI (USUALLY 5.0)
C      FSUBG        IS      VELOCITY FACTOR FOR CROSSFLOW SLIGHTLY LT 1
C      FSUBH        IS      MASS FLOW FACTOR FOR CROSSFLOW SLIGHTLY LT 1
C      ALTHOUGH 12 EQUIVALENT FLOW CHANNELS ARE CALCULATED, ARRAYS ARE
C      SET UP FOR 14 CHANNELS TO ALLOW ONE FORM EACH OF THE EQUATIONS
C      OF MASS CONTINUITY , MOMENTUM , ENERGY .
C      THEREFORE ONLY CHANNELS 2 THROUGH 13 ARE USED
1  FORMAT(8F11.0)
2  FORMAT(8I10 )
3  FORMAT(8A10)
4  FORMAT(99H1)CALCULATIONS FOR RESULTS OF LOCAL FLOW BLOCKAGE *****
1 *****
5  FORMAT(1H1,////25X8A10,/25X8A10,/25X8A10,////)
6  FORMAT(42H0 ***** GEOMETRY ***** )
7  FORMAT(42H0 ***** BLOCKAGE INFO ***** )
8  FORMAT(42H1 ***** POWER, FLOW, ETC. ***** )
9  FORMAT(24H0PIN LENGTH (IN) ,1F7.3)
10 FORMAT(24H0PIN DIAMETER (IN) ,1F7.3)
11 FORMAT(24H0PIN PITCH (IN) ,1F7.3)
12 FORMAT(24H0CLADDING THICKNESS (IN),1F7.3)
13 FORMAT(24H0NUMBER OF CHANNELS ,1I10 )
14 FORMAT(24H0ACTIVE FUEL INCREMENTS ,1I10 )
15 FORMAT(24H0NO. AXIAL DIVISIONS ,1I10 )
16 FORMAT(24H0NO. BLOCKED CHANNELS ,1I10 )
17 FORMAT(24H0BLOCKAGE LENGTH,INCS ,1I10 )
18 FORMAT(24H0BLOCKAGE LOCATION,INCS ,1I10 )
    READ(11 ,3) (HEAD(I),I=1,24)
    READ(11,1) (PLENGTH, PINDIA, PINPICH, CLADT )
    READ(11,2) (NOCHAN ,LENGTHA ,NODIV ,NUMBLOK ,LENGTHB
1 ,LOCHLUK )
    READ(11,1) (BFLOWA(I),I=2,13)
    READ(11,1) (DEQB(I) ,I=2,13)
    READ(11,2) (ISTAGE ,NOBLOK ,NOEXP ,NOCUNT ,ISYM )
    READ(11,2) (ITRUBLE ,(JTRUBLE(N),N=1,1J))
    READ(11,1) (POWNORM(I),I=1,41)
    READ(11,1) (PEAKPOW ,TOUT ,TIN ,VARPROP ,FOUT
1 ,FSUBC ,FSUBG ,FSUBH )
    READ(11,1) (POWVAR(I),I=2,13 )
    READ (11,1) PCRIT, RCONST, RCONSTL, ENTH, WCRY ,BETA
    WRITE(6,5) (HEAD(I),I=1,24)
    WRITE(6,6)
    WRITE(6,9) (PLENGTH )
    WRITE(6,10) (PINDIA )
    WRITE(6,11) (PINPICH )
    WRITE(6,12) (CLADT )
    WRITE(6,13) (NOCHAN )
    WRITE(6,14) (LENGTHA )
    WRITE(6,15) (NODIV )
    WRITE(6,16) (NUMBLOK )
    WRITE(6,17) (LENGTHB )
    WRITE(6,18) (LOCHLUK )
    WRITE(6, 7)

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WRITE(6,20)      (BFLOWA(I),I=2,13)
WRITE(6,21)      (DEQB(I),I=2,13)
WRITE(6,22)      (ISTAGE)
WRITE(6,23)      (NOBLOKD)
IF (RCONST.GT. 0.) WRITE(6,40) RCONST, RCONSTL
WRITE (6,41) ENTH
WRITE (6,42) BETA
20 FORMAT(3GH0BLOCKAGE FLO AREA /UNIT CHANL,1F7.6 ,11(/30X,1F7.6 ))
21 FORMAT(3GH0BLOCKAGE EQUIV DIA/UNIT CHANL,1F7.6 ,11(/30X,1F7.6 ))
22 FORMAT(3GH0NO. OF STEPS TO FINAL BLOKAGE,1I10 )
23 FORMAT(3GH0NO. OF CHAN DIAS BYPASS FLOW ,1I10 )
24 FORMAT(3GH0NO. EXPANSION LOSSES / DIVSN ,1I10 )
25 FORMAT(3GH0NO. CONTRACTN LOSSES / DIVSN ,1I10 )
26 FORMAT(3GH00 FOR CHANNEL,1 FOR PIN SYM ,1I10 )
28 FORMAT(3GH0RADIAL POWER VARIATION FACTOR,1F7.3 ,11(/30X,1F7.3 ))
27 FORMAT(3GH0NORMALIZED AXIAL POWER DIST. , 5(/20X,10F7.3 )
29 FORMAT(3GH0PEAK POWER NORMAL CHAN KW/FT ,1F7.3 )
30 FORMAT(/,* OUTLET TEMP DEG F * ,8X ,1F9.3 )
31 FORMAT(3GH0INLET TEMP DEG F ,1F7.3 )
32 FORMAT(3GH01.0 FOR VARIABLE PROPERTIES ,1F7.3 )
33 FORMAT(3GH0OUTLET PRESSURE, PSI ,1F7.3 )
34 FORMAT(3GH0FACTOR FOR CROSSFLOW MOM FLUX,1F7.3 )
35 FORMAT(3GH0FACTOR FOR CROSSFLOW VELOCITY,1F7.3 )
36 FORMAT(3GH0FACTOR FOR CROSSFLOW MASS FLO,1F7.3 )
40 FORMAT (3GH0CONSTANT CROSSFLOW K EQUAL TO, 2F9.2)
41 FORMAT(3GH POWER ON CROSSFLOW DISTR. ,1F7.3)
42 FORMAT (3GH TURB MIXING COEFFICIENT ,1F7.3)
WRITE(6,24)      (NOEXP)
WRITE(6,25)      (NOCONT)
WRITE(6,26)      (ISYM)
WRITE(6,8)
WRITE(6,27)      (POWNORM(I),I=1,41)
WRITE(6,28)      (POWVAR(I),I=2,13)
WRITE(6,29)      (PEAKPOW)
WRITE(6,30)      (TOUT)
WRITE(6,31)      (TIN)
WRITE(6,32)      (VARFRUP)
WRITE(6,33)      (POUT)
WRITE(6,34)      (FSLBC)
WRITE(6,35)      (FSLBG)
WRITE(6,36)      (FSUBH)
IF (NODIV.LT. 41) GO TO 100
WRITE(6,99)
99 FORMAT(49H0 *** ERROR *** EXCESSIVE NO. AXIAL DIVISIONS )
100 NUMBLOK =NUMBLOK +1
    NOCHAN =NOCHAN +1
    RETURN
    END

```

```

SUBROUTINE OUT2(INTRU3L)
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGIHB ,LOCBLOK ,PINDIA ,PINPICH ,CLADI ,
3 PINNO(14) ,AREA(14,41) ,EQUARIA(14) ,
4 ARATIO(14) ,CRATIO(14) ,BUPAKEA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,KB(14) ,
6 IB(14) ,JB(41) ,DUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DEQB(14) ,
2 NOBLOKD ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,PCRIT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKPOW ,DIVL ,PF ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCONT ,STAGINP ,PINCHES ,DELTAP
COMMON /POWR/
1 POWNORM(41) ,POWLIN(41) ,QPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,TIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNUS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,POUT ,WCRY ,
7 ,RK(13,41) ,RCONST ,RCONSTL ,
8 ,WMAX(14) ,ENTH ,BETA
DIMENSION NSHOW(14,41) ,TEMPO(14,41)
C INITIALIZE
  IZER = 0
  ZER = 0.
  DO 5 M=1,14
  DO 4 N=1,41
    TEMPO(M,N) = ZER
    NSHOW(M,N) = IZER
  4 CONTINUE
  5 CONTINUE
C SET UP BLOCKAGE MAP
  MSTOP = NOCHAN-1
  DO 110 MM=2,MSTOP
  DO 105 NN=1,NODIV
    IF (IB(MM) .EQ. 0) GO TO 104
    IF (JB(NN) .EQ. 0) GO TO 103
  104 NSHOW(MM,NN) = 0
    GO TO 105
  103 NSHOW(MM,NN) = 1
  105 CONTINUE
  110 CONTINUE
C BLOCKAGE MAP
  9 WRITE(6,10)
  10 FORMAT(1H1, • BLOCKAGE MAP,SUBROUTINE OUT2*,// • AX LOC 1
    1 2 3 4 5 6 7 8 9
    2 10 11 12 *)

```



```

        NSTOP = NODIV+1
        DO 15 NN = 1,NSTOP
          JN = NODIV +2 -NN
          WRITE(6,20) (JN,(NSHOW(MM,JN),MM=2,13))
15      CONTINUE
20      FORMAT(4X,I2,5X,12(I1,7X))
C      PRINT VALUES OF PRESSURE
C      VALUES IN PSI
        MSTOP = NOCHAN
        NSTOP = NODIV+1
        DO 220 MM=2,MSTOP
          DO 210 NN=1,NSTOP
            TEMPO(MM,NN) = P(MM,NN)/144.0
210      CONTINUE
220      CONTINUE
          WRITE(6,50)
          50      FORMAT(20H1PRESSURES IN PSI      )
          WRITE(6,51)
          51      FORMAT(/,* AX LOC      1      2      3      4      5      6
1          7      8      9      10      11      12 *)
            NSTOP= NODIV+1
            DO 310 NN = 1, NSTOP
              N = NODIV +2 -NN
              WRITE(6,52) (N,(TEMPO(MM,N),MM=2,MSTOP))
310      CONTINUE
          52      FORMAT(3X,I2,3X,12(1F6.2,2X))
C      WRITE VELOCITIES
          WRITE(6,53)
          53      FORMAT(20H1VELOCITIES IN FT/SEC )
          WRITE(6,51)
          DO 410 NN=1,NSTOP
            N = NODIV +2 -NN
            WRITE(6,52) (N,(U(MM,N),MM=2,MSTOP))
410      CONTINUE
          MSTOP = NOCHAN - 1
C      WRITE CROSSFLOWS PER UNIT CHANNEL
          75      FORMAT (1,* AX LOC      1      2      3      4      5
1          6      7      8      9      10      11      12*)
            WRITE(6,54)
            WRITE(6,75)
          54      FORMAT(30H1CROSSFLOWS PER UNIT CHANNEL )
          DO 460 NN=1,NSTOP
            N = NODIV +2 -NN
            WRITE(6,71) (N,(W(MM,N),MM=2,MSTOP))
          71      FORMAT(3X,I2,3X,12(1E9.2))
460      CONTINUE
          WRITE(6,72) (WMAX(MM),MM=2,MSTOP)
          72      FORMAT(* MAXIMUM DOWNSTREAM CROSSFLOW VALUES *//,8X,12(1E9.2))
C      SET TEMPO TO EQUIVALENT CROSSFLOWS
          MEND = MSTOP
          DO 480 M=2,MEND
            DO 470 N=1,NSTOP
              TEMPO(M,N) = W(M,N) * CRATIO(M)
470      CONTINUE
480      CONTINUE
C      PRINT THESE
          WRITE(6,55)
          WRITE(6,75)

```

```

55 FORMAT(30H1CROSSFLOWS PER EQUIV CHANNEL )
DO 490 NN=1,NSTOP
N = NODIV +2 -NN
WRITE(6,71) (N, (TEMPO(MM,N),MM=2,MEND))
490 CONTINUE
MSTOP = NOCHAN
DO 491 MM=2,NOCHAN
TEMPO(MM,1) = TIN
DO 493 N = 2,NODIV
C TEMPO IS TEMPERATURE
TEMPO(MM,N) = TEMPO(MM,N-1) + ( (H(MM,N)-H(MM,N-1))/CPBARS(MM,N))
493 CONTINUE
491 CONTINUE
WRITE (6,57)
57 FORMAT(25H1FLUID TEMPERATURES )
DO 495 NN=1,NSTOP
N = NODIV +2 -NN
WRITE (6,58) (N, (TEMPO(MM,N),MM=2,MSTOP))
495 CONTINUE
58 FORMAT(3X,I2,3X,I2(1F7.0,2X))
DUMAREA = (3.14159*PINDIA*DIVL/144.)
DO 2491 MM=2,NOCHAN
DO 2490 N =1,NODIV
C CHANGE TEMPO TO CLADDING TEMPERATURE
TEMPO(MM,N) = TEMPO(MM,N) + (QPERDIV(MM,N)/(PINNO(MM)*DUMAREA
1 *4.17))
2490 CONTINUE
2491 CONTINUE
WRITE (6,2057)
2057 FORMAT(25H1CLADDING TEMPERATURES )
DO 2495 NN=1,NSTOP
N = NODIV +2 -NN
WRITE (6,53) (N, (TEMPO(MM,N),MM=2,MSTOP))
2495 CONTINUE
C WRITE DENSITIES
WRITE(6,50)
WRITE(6,51)
56 FORMAT(20H1FLUID DENSITIES )
DO 510 NN=1,NSTOP
N = NODIV +2 -NN
WRITE(6,52) (N, (RHOS(MM,N),MM=2,MSTOP))
510 CONTINUE
C WRITE PERTINENT INFO IN CASE OF TROUBLE
IF(NTRUBL.EQ.0) GO TO 2000
MSTOP = NOCHAN + 1
DO 800 MM=1,MSTOP
WRITE(6,60) MM
60 FORMAT(30H1TROUBLESHOOTING, CHANNEL NO. , I15 )
DO 790 N=1,NSTOP
NN = NODIV +2 - N
CALL PIKL(5HAXLOC,AXLOC(NN), 10HQPERDIV(N),QPERDIV(MM,NN),
1 6HREYNOS,REYNOS(MM,NN), 9HFRIC FACT,FRICS(MM,NN) ,
2 5HCSUBP,CPBARS(MM,NN), 9HENTHALPY , H(MM,NN) ,
3 9HFLOW A-EA,AREA(MM,NN),10HRAD V SIGN,AKEY(MM,NN) ,
4 7HHYD DIA,DEQ(MM,NN) )
790 CONTINUE
800 CONTINUE
2000 RETURN
END

```

```

SUBROUTINE BLOKIT
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGTHB ,LOCBLOK ,PINDIA ,PINPICH ,CLAD1 ,
3 PINNO(14) ,AREA(14,41) ,EQUAREA(14) ,
4 ARATIO(14) ,CRATIO(14) ,BUPAREA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,KB(14) ,
6 IR(14) ,JB(41) ,JUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DECB(14) ,
2 NOBLOK ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,PORIT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKFOW ,DIVL ,PF ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCUNT ,STAGINP ,PINCHES ,DELTA
COMMON /POWER/
1 POWNORM(41) ,POWLIN(41) ,UPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,TIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBS ,FSUBH ,REYNOS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,POUT ,WCY ,
7 ,RK(13,41) ,RCONST ,RCONSTL ,
8 ,WMAX(14) ,ENTH ,BETA
C THIS SUBROUTINE INCREASES THE BLOCKED CHANNEL AXIAL RESISTANCES
C IN STAGES
C FAREA IS 2.0 TIMES UNIT CHANNEL AREA
C LL = LOCBLOK + LENGTHB -1
C DO 30 M = 2,NUMBLOK
C ADDN IS NO. OF ADDITIONAL CIRCULAR FLOW CHANNELS
C ADDN = FINALN
C CIRCNO IS NO. OF PRESENT CIRCULAR FLOW CHANNELS FOR NSTAGE
C XNSTAGE = NSTAGE
C XISTAGE = ISTAGE
C DELA = ((FAREA/2.0) - (BFLOWA(M)/144.0)) *
1 (1.0 - (1.0 - (XNSTAGE/XISTAGE))**2)
C FINALN = (4.0*BFLOWA(M)/(3.14159*(DECB(M)**2.0))) - 1.0
C CIRCNO = (FINALN*(XNSTAGE/XISTAGE)) + 1.0
C A = (FAREA/2.0) - DELA
C CALL PIKL(1HM,1,9HBFLOWA(M),BFLOWA(M),6HFINALN,FINALN
1 ,6HCIRCNO,CIRCNO,9HAREAM(L),AREAM(L)
2 ,7HHYD DIA,DEQ(M,L),9HDELA,DELA
3 ,9HXISTAGE,XISTAGE,9HXNSTAGE,XNSTAGE)
C DO 20 N = LOCBLOK,LL
C AREA(M,N) = A * ARATIO(M)
C DEQ(M,N) = (4.0 * A / (3.14159 * CIRCNO))**.5
20 CONTINUE
30 CONTINUE
C CALCULATE CONTRACTION AND EXPANSION COEFFICIENTS
C DO 50 M = 2,NUMBLOK

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```

      SIGMA(M) = AREA(M,LOCBLOK)/EQUARIA(M)
      AKSUBC(M) = .53 - (.4*SIGMA(M))
      AKSUBE(M) = 1. - SIGMA(M)
50  CONTINUE
      IF (ITRUBLE .EQ. 0) GO TO 200
      WRITE(6,60)
60  FORMAT(25H TROUBLESHOOT IN BLOKIT )
      WRITE(6,61)
61  FORMAT(30H AT AXIAL LOCATION OF BLOCKAGE )
      L = LOCBLOK
      DO 70 M=1,NOCHAN
      CALL PIKL(1H,M ,9HBFLOWA(M) ,BFLOWA(M) ,6HFINALN ,FINALN
1      ,6HCIRCNO ,CIRCNO ,9HAREA(M,L) ,AREA(M,L)
2      ,7HHYD DIA,DEQ(M,L) ,9HAKSUBC(M) ,AKSUBC(M)
3      ,9HAKSUBE(M) ,AKSUBE(M) )
70  CONTINUE
200  RETURN
      END

```

```

SUBROUTINE SETUP
C THIS SUBROUTINE SETS UP FLOWS, PRESSURES, TEMPS, FLUID PROPERTIES
COMMON /GEOMET/
1 NOCFIN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGTHB ,LUCBLOK ,PINDIA ,PINPICH ,CLACT ,
3 PINNO(14) ,AREA(14,41) ,EQUAREA(14) ,
4 ARATIO(14) ,CRATIO(14) ,BUPAREA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,K3(14) ,
6 IB(14) ,JE(41) ,JUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DEQB(14) ,
2 NOBLOK ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,PCRT ,
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD ,
3 ,PEAKPOW ,DIVL ,PP ,BLUP ,GSUBC ,
4 ,XNOEXP ,XNOCONT ,STAGINP ,PINCHES ,DELIAP
COMMON /POWR/
1 POWNORM(41) ,POWLIN(41) ,QPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,TIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNOS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,IFY(14,3) ,
6 C(14) ,PCUT ,WCRY ,
7 ,RK(13,41) ,RCNST ,RCNSTL ,
8 ,WMAX(14) ,ENTH ,BETA
KTRUBLE = ITRUBLE
ITRUBLE = JTRUBLE(1)
GSUBC = 32.2
C AVG FLUID PROPERTIES (PROPERTIES FROM ANL 7523 )
TEMPF = .5*(TOUT + TIN)
TEMPR = TEMPF + 460.
TEMPK = ((TEMPF - 32.)/1.8) + 273.16
C CSUBP IN BTU/LEM - DEG F
CPBAR = .384352 - ((1.10599 / (10.0**4)) * TEMPR)
1 + ((3.41178 / (10.0**8)) * (TEMPR**2))
C DENSITY IN LBM/FT**3
RHOBAR = 59.566 - ((7.9504 / (10.0**3)) * TEMPF)
1 - ((.2672 / (10.0**6)) * (TEMPF**2))
2 + ((.6035 / (10.0**10)) * (TEMPF**3))
C VISCOSITY IN LBF-SEC/FT**2
AMULOG = 3.518 + (220.69/TEMPK) - (0.4925*ALOG10(TEMPK))
BARMU = (10.0**AMULOG)/47900.
IF(ITRUBLE.EQ.0) GO TO 3
CALL PIKL(5HTEMPF, TEMPF,5HTEMPR,TEMPR ,5HTEMPK,TEMPK ,
1 6HRHOBAR,RHOBAR ,6HVISCOS,BARMU )
3 XNOEXP = NOEXP
XNOCONT = NOCONT
C CALCULATE AXIAL LOCATION VALUES
XNO = NODIV
AXLOC(1) = 0.

```

```

C   AVG. POWER
   PAVE = 0.0
   DO 70 N=1,NODIV
   PAVE = PAVE + POWLIN(N)
70  CONTINUE
   XNODIV = NODIV
   PAVE = PAVE/XNODIV
C   FLOW IN L3M/SEC
C   FLOW SET FOR PROPER COOLANT DELTAT FOR CHANNEL NOCHAN
   J = NOCHAN
   FLOW = POWVAR(J)*(PAVE*PLENGTH)*(3413.04/3600.)
1   /((CPBAR * (TOUT - TIN)*12.0)
C   VELOCITY
C   AVG. CHANNEL
   FAREA =((((3.0*.5)/2.0)*(PINPICH**2))
1   -((3.14159/4.0)*(PINDIA**2)))/144.
   VEL = FLOW / (RHOBAR * FAREA)
C   CALCULATE PRESSURES AND INLET STAGNATION PRESSURE
C   DELTAP PER DIVISION
C   EQHYD IN FT
   EQHYD = (4.0 * (FAREA/2.0)) / ((3.14159/2.0) * PINDIA / 12.)
   IF (EQHYD.EQ. 0.)EQHYD = .00003
   REYNO = (RHOBAR * VEL * EQHYD) / (GSUBC * BARMU)
C   FRICTION FACTOR FOR FLOW INSIDE SMOOTH TUBES (BLASIUS FORMULA)
C   REYNO MUST BE ABOVE 10,000.
   FRICFAC = 0.316 / (REYNO **.250)
   XNODIV = NODIV
   DIVL = (PLENGTH / XNODIV)
   DELPDIV = (FRICFAC * RHOBAR * (VEL**2) * (DIVL/12.0)
1   / (2.0 * GSUBC * EQHYD) )
C   OUTLET PRESSURE IN LBF / FT**2
   POUT2 = POUT * 144.0
C   CALC. CHANNEL PRESSURES
   DO 80 M=2,NOCHAN
   NEND = NODIV + 1
   P(M,NEND) = POUT2
   DO 90 N=1,NODIV
   NN = NODIV +1 -N
   P(M,NN) = P(M,NN+1) + DELPDIV
90  CONTINUE
80  CONTINUE
C   STAGNATION PRESSURE AT INLET
   STAGINP = P(3,1) + (RHOBAR * (VEL**2) / (2.0 * GSUBC) )
C   STAG. P IN PSI
   PINCHES = STAGINP/144.
C   SET ALL VELOCITIES, REYNOS, FRICFACS, PROPS
   MIT = NODIV + 1
   DO 120 N=1,MIT
   DO 110 M=2,NOCHAN
   U(M,N) = VEL
   REYNOS(M,N) = REYNO
   FRICS(M,N) = FRICFAC
   CPBARS(M,N) = CPBAR
   RHOS(M,N) = RHOBAR
   AMUSE(M,N) = BARMU
   DEQ(M,N) = EQHYD
110 CONTINUE
120 CONTINUE

```

```

      NSTOP = NODIV + 1
      DO 5 NN=2,NSTOP
      AXLOC(NN) = AXLOC(NN-1) + (PLENGTH/XNO)
5    CONTINUE
C    CALCULATE LINEAR POWERS
C    HOW MANY VALUES OF POWER
      II = 0
      DO 10 I=1,NODIV
      IF(POWNORM(I) .GT. .00001) II = II + 1
10   CONTINUE
      IF (NODIV .EQ. II) GO TO 20
      WRITE(6,15)
15   FORMAT(76H INCORRECT NO. OF POWER VALUES FOR GIVEN NO. OF DIVISION
      1S, SUBROUTINE SETUP )
      STOP
20   DO 30 I=1,NODIV
      POWLIN(I)=PEAKPOW* (POWNORM(I) + POWNORM(I+1))*0.5
30   CONTINUE
C    CALCULATE CHANNEL HEAT RATE (BTU/SEC) - EQUIV CHANNEL
C    BASED ON THE FOLLOWING
C    CHANNEL NO.    PINS PER      EQUIV.      NO. CONNECTIONS
C                   EQ. CHANNEL  FLOW AREA  TO NEXT (I+1) CHANNEL
C                   2           1/12 (OR 1/4)  A/6 (OR A/2)  1/2
C                   3           1/4           A/2          1
C                   4           1/2           A            1
C                   5           1/2           A            2
C                   6           3/4           3A/2         3/2
C                   7           3/4           3A/2         3
C                   8           1            2A           2
C                   9           1            2A           4
C                   10          5/4           5A/2         5/2
C                   11          5/4           5A/2         5
C                   12          3/2           3A           3
C                   13          3/2           3A           6
C
C    WHERE A IS 1/2 (.86*P**2 - (PI/4) (DIA**2)
C              OR ONE-HALF FLOW AREA ASSOCIATED WITH ONE PIN
C    SET NO. PINS PER EQUIV. CHANNEL
      PINNO( 2)= 1.0/12.0
      PINNO( 3)= 1.0/4.0
      PINNO( 4)= 1.0/2.0
      PINNO( 5)= 1.0/2.0
      PINNO( 6)= 3.0/4.0
      PINNO( 7)= 3.0/4.0
      PINNO( 8)= 1.0
      PINNO( 9)= 1.0
      PINNO(10)= 5.0/4.0
      PINNO(11)= 5.0/4.0
      PINNO(12)= 3.0/2.0
      PINNO(13)= 3.0/2.0
      IF(ISYM .GT. 0) PINNO(2)=1.0/4.0
      DO 60 M=2,NUCHAN
      DO 50 N=1,NODIV
      QPEKDIV(M,N) = POWVAR(M) * POWLIN(N) * (PLENGTH/(12.0*XNO ))
1      * (3413.04/3600.) * PINNO(M)
50   CONTINUE
60   CONTINUE
C    CALCULATE FLOWS AND VELOCITIES

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```

MSTOP = NOCHAN + 1
IF (ITRUBLE .EQ. 0) GO TO 141
WRITE(6,125)
125 FORMAT(35H TROUBLESHOOT, SUBROUTINE SETUP )
NSTOP = NODIV + 1
CALL PIKL( 10H AVG. POW. ,PAVE , 10H AVG. FLOW ,FLOW
1 , 10H UNIT AREA ,FAREA , 10H VELOCITY ,VEL
2 , 10H EQ HYD DIA ,EQHYD , 10H REY NO. ,REYNO
3 , 10H FRIC FAC ,FRICFAC , 10H STAG IN P ,PINCHES )
WRITE(6,126)
126 FORMAT(1H0)
DO 140 M=1,MSTOP
DO 135 N=1,NSTOP
NN = NODIV +2 -N
CALL PIKL( 10H CHANNEL ,M , 10H MAX. LOC ,AXLOC(NN)
1 , 10H PRESSURE ,P(M,NN) , 10H VELOCITY ,U(M,NN)
2 , 10H REY. NOS. ,REYNOS(M,NN) , 10H FRIC. FAC. ,FRICS(1,
3 NN), 10H SP. HEAT ,CPBARS(M,NN) , 10H DENSITY ,RHOS(M,NN)
4 )
135 CONTINUE
140 CONTINUE
C SET FLOW AREAS PER EQUIV. CHANNEL, EQUARIA(M)
C NO. UNIT CHANNELS PER EQUIV. CHANNEL
141 ARATIO( 2) = 1. / 0.
ARATIO( 3) = 1. / 2.
ARATIO( 4) = 1.
ARATIO( 5) = 1.
ARATIO( 6) = 3. / 2.
ARATIO( 7) = 3. / 2.
ARATIO( 8) = 2.
ARATIO( 9) = 2.
ARATIO(10) = 5. / 2.
ARATIO(11) = 5. / 2.
ARATIO(12) = 3.
ARATIO(13) = 3.
IF (ISYM .GT. 0) ARATIO(2) = 1./2.
C EQUARIA IS EQUIV. CHANNEL AREA, (NOT UNIT)
DO 150 M = 2,NOCHAN
EQUARIA(M) = (FAREA /2.)* ARATIO(M)
150 CONTINUE
C SET AREAS
NSTOP = NODIV + 1
DO 180 M=2,NOCHAN
DO 170 N=1,NSTOP
AREA(M,N) = EQUARIA(M)
170 CONTINUE
180 CONTINUE
C SET NO. OF UNIT CHANNEL CROSSFLOW FLOW AREAS
CRATIO( 2) = 1. / 2.
CRATIO( 3) = 1.
CRATIO( 4) = 1.
CRATIO( 5) = 2.
CRATIO( 6) = 3. / 2.
CRATIO( 7) = 3.
CRATIO( 8) = 2.
CRATIO( 9) = 2.
CRATIO(10) = 5. / 2.
CRATIO(11) = 5.

```



```

      CRATIO(12) = 3.
      CRATIO(13) = 6.
C     CALCULATE BYPASS FLOW AREAS
C     UPSTREAM OF BLOCKAGE
C     CHANNEL DIA
      CDIA = (2.*PINPICH/(3.**.5)) - PINDIA
C     BYPASS LENGTH
      XNO = NOBLOK
      BLUP = CDIA * XNO
      NOCH = NOCHAN - 1
      DO 200 MM=2,NOCH
        BUPAREA(MM) = BLUP * (PINPICH - PINDIA) * CRATIO(MM) / 144.
200  CONTINUE
C     DOWNSTREAM OF BLOCKAGE
C     BYPASS FLOW AREA PER AXIAL DIVISION
      NOCH = NOCHAN - 1
      PP = PINPICH - PINDIA
      NOSTOP = NOCHAN - 1
      DO 220 MM=2,NOSTOP
        BYDOWNA(MM) = DIVL * PP * CRATIO(MM) / 144.
220  CONTINUE
      IF(ITRUBLE.EQ. 0) GO TO 250
      WRITE(6,126)
      MSTOP = NOCHAN + 1
      NSTOP = NODIV + 1
      DO 240 MM=1,MSIOP
        CALL PIKL( 10HCHANNEL ,MM , 10HEQUIV AREA,EQUAPIA(
1         MM) , 10HUP BY AREA,BUPAREA(MM) , 10HDOWN BY AREA,BYDOWNA(
2         MM) )
240  CONTINUE
      WRITE(6,125)
      DO 249 MM=1,MSIOP
      DO 245 N=1,NSTOP
        NN = NODIV +2 - N
        CALL PIKL( 10HCHANNEL ,MM , 10HAX. LOC ,AXLOC(NN)
1         , 10HEQUIV AREA,AREA(MM,NN) )
245  CONTINUE
249  CONTINUE
C     SET JB, IB
250  LL = LOGBLOK -2
      DO 300 N=1,LL
        IF(LL.LT. 1) GO TO 301
        JB(N) = -1
300  CONTINUE
301  JB(LL+1) = 1
      LL = LL + 2
      LLL = LOGBLOK + LENGTHB -1
      DO 310 N=LL,LLL
        JB(N) = 0
310  CONTINUE
      LLL = LLL + 1
      DO 330 N = LLL,NODIV
        JB(N) = 1
330  CONTINUE
C     IB
      DO 350 M=2,NUMBLOK
        IB(M) = 1
350  CONTINUE

```

```

C      SETUP ITERATION COEFFICIENTS, TRY= - PCHANGE / (2*V*DELV)
      LL = LOCBLOK - 1
      ALL= LL
      DO 400 M=2,NOCHAN
      TRY(M,1) = .167 * ALL * DIVL
      TRY(M,2) = .167 * (PLENGTH - (ALL * DIVL))
      TRY(M,3) = .167 * (PLENGTH - (ALL * DIVL)) * 5.0
400  CONTINUE
      IF (ITRUBLE .EQ. 0) GO TO 500
      WRITE(6,126)
      DO 405 N=1,NODIV
      CALL PIKL( 10H N ,N , 10H JB(N) ,JB(N) )
405  CONTINUE
      DO 410 M = 1,MSTOP
      CALL PIKL( 10H M ,M , 10H IB(M) ,IB(M)
1      , 10H TRY(M,1),TRY(M,1), 10H TRY(M,2) ,TRY(M,2)
2      , 10H TRY(M,3),TRY(M,3) )
410  CONTINUE
500  CONTINUE
      LLL = LOCBLOK + LENGTHB -1
      SET KEYS FOR CROSS-FLOW DIRECTIONS AND SET CROSS-FLOW RESISTANCES
      RESISTANCES ARE AS FOLLOWS
C      1.) FOR W .LE. U , K =(U/W)**2
C      2.) FOR W .GT. 0 BUT .LT. 10*U , K =(U/W)**.47
C      3.) FOR W .GT. 10*U , K = 0.34
      MSTOP = NOCHAN + 1
      LL = LOCBLOK -1
      DO 560 N=LL,LLL
      DO 550 M=1,NOSTOP
      AKEY(M,N) = 1.0
550  CONTINUE
560  CONTINUE
      LLL = LLL +1
      NSTOP = NODIV +1
      DO 580 N=LLL,NSTOP
      DO 570 M=1,MSTOP
      AKEY(M,N) = -1.0
570  CONTINUE
580  CONTINUE
C      CROSS-FLOW RESISTANCES AT ZEROth AND M+1 CHANNELS
      MSTOP = NOCHAN + 1
      NSTOP = NODIV + 1
      DO 610 M=1,MSTOP
      DO 600 N=1,NSTOP
      RK(M,N) = 1000.
      IF (RCONST .GT. 0.) RK(M,N)= RCONST
      IF(RCONST .EQ. 0.) GO TO 600
      IF(M .GT. (NUMBLOK-1)) GO TO 595
      GO TO 600
595  CONTINUE
      IF (M .GT. NUMBLOK) GO TO 599
      IF (N .GT. LOCBLOK) RK(M,N) = RCONSTL
      GO TO 600
599  RK(M,N) = RCONSTL
600  CONTINUE
610  CONTINUE
      IF(ITRUBLE .EQ. 0 ) GO TO 800
      DO 650 M=1,MSTOP

```

```
DO 640 N=1,NSTOP
NN = NODIV +2 -N
CALL PIKL( 10H M ,M , 10H N ,NN
1 , 10HC-F UIRGIN,AKEY(M,NN)
2 , 10HC-F RESIST,RK(M,NN))
640 CONTINUE
650 CONTINUE
8J0 ITRUBLE = KTRUBLE
RETURN
END
```

```

SUBROUTINE CONTIN(MGO, MSTOP, NGO, NSTOP)
C THIS SUBROUTINE CALCULATES MDOT AND U
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NUDIV ,NUMBLOK ,
2 LENGTHB ,LOCBLOK ,PINODIA ,PINPICH ,CLACT ,
3 PINNO(14) ,AREA(14,41) ,EQUARIA(14) ,
4 ARATIO(14) ,CRATIO(14) ,BUPAREA(14) ,
5 BYDCWNA(14) ,SIGMA(14) ,KB(14) ,
6 IB(14) ,JB(41) ,DUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DEQB(14) ,
2 NOBLUKD ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,PCRIT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKPOW ,DIVL ,PP ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCONT ,STAGINP ,PINCHES ,DELTAP
COMMON /POWR/
1 POWNORM(41) ,POWLIN(41) ,QPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,FIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNOS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,POUT ,WCRY ,
7 ,RK(13,41) ,RCONST ,RCONSTL ,
8 ,WMAX(14) ,ENTH ,BETA
C AREA IS EQUIV CHANNEL AREA, NOT UNIT CELL AREA
DIMENSION DOTM(14,41)
DO 200 MM=MGO,MSTOP
NN=NGO-1
DOTM(MM,NN)=RHOS(MM,NN)*AREA(MM,NN)*U(MM,NN)
LL=LOCBLOK
DO 100 NN=NGO,NSTOP
FACT=1.0
IF(NN.EQ. LL) FACT=BLUP/DIVL
C W S ARE ON BASIS OF FLOW PER LENGTH PER CROSSOVER, NOT TOTAL
DOTM(MM,NN)=DOTM(MM,NN-1)
1 + (W(MM-1,NN-1)*CRATIO(MM-1)*(DIVL/12.)*FACT)
2 - (W(MM,NN-1)*CRATIO(MM)*(DIVL/12.)*FACT)
U(MM,NN)=DOTM(MM,NN)/(RHOS(MM,NN)*AREA(MM,NN))
100 CONTINUE
200 CONTINUE
RETURN
END

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SUBROUTINE AXDELP(M ,NSTART,NSTOP)
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGTHB ,LOCBLOK ,PINDIA ,PINPICH ,CLACT ,
3 PINN (14) ,AREA(14,41) ,EQUAREA(14) ,
4 AKATIO(14) ,CRATIO(14) ,SUPAREA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,K3(14) ,
6 IB(14) ,JB(41) ,JUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DEQB(14) ,
2 NOBLOKU ,NOCUNT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DLQ(14,41)
COMMON /GENL/
1 ITRUBLE ,MUP ,N ,UTRUBLE(16) ,PCRT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKPOW ,DIVL ,PP ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCONT ,STAGINP ,PINCHES ,DELTAP
COMMON /POWR/
1 POWNORM(41) ,POWLIN(41) ,PERDIV(14,41) ,
2 POWVAR(14) ,TOU ,FIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNOS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,PCUT ,WCRY ,
7 ,RK(13,41) ,RCONST ,KCONSTL
8 ,WMAX(14) ,ENTH ,BETA
C DIRECT AXIAL PATH
PENT = 0.
DO 500 II = 1,41
PINC(II) = 0.
500 CONTINUE
DELTAP = 0.
LL = LOCBLOK - 1
DO 2000 N=NSTART,NSTOP
FACT = 1.0
IF(N.EQ. LL) FACT = BLUP/DIVL
IF (NSTART.EQ. 1) PENT = RHOS(N,1) * ABS(U(N,1)) * U(N,1) /
(2.*GSUBC)
1 PCOMP = ( (FRICS(M,N)*RHOS(M,N)*ABS(U(M,N))*U(M,N)*DIVL/12.)/
(2.*GSUBC*DLQ(M,N)) )
1 - ( ((RHOS(M,N+1)-RHOS(M,N))*ABS(U(M,N))*U(M,N)) /GSUBC)
2 PINC(N) = PINC(N) + PCOMP
IF (JB.LT. 0) GO TO 1200
IF (JB.GT. 0) GO TO 1085
IF (IB.GT. 0) GO TO 1100
C MOMENTUM EXCHANGE BUT NO EXP. OR CONT. LOSSES
C AKEY IS THE CROSSFLOW DIRECTION, +1 FOR RADIALLY OUTWARD
1085 PCOMP = (DIVL/(12.*2.)) * (
1 ((AKEY(M-1,N)+1.)*(W(M-1,N)*FACT*CRATIO(M-1)* ((2.*U(M,N))
2 -(FSUBC * U(M-1,N)) ))
3 -((AKEY(M,N)-1.0)*(W(M,N)*FACT*CRATIO(M) * ((2.*U(M,N))
4 -(FSUBC * U(M+1,N)) ))))

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5	+(AKEY(M,N)+1.0)*(W(M,N)*FACT*CRATIO(M)*U(M,N)*	87
6	(FSUBC - 2.0))	88
7	-(AKEY(M-1,N)-1.0)*(W(M-1,N)*FACT*CRATIO(M-1)*U(M,N)*	89
8	(FSUBC - 2.0))	90
	PINC(N) = PINC(N) + PCOMP	92
C	SKIP CONTRACTION AND EXPANSION LOSSES	94
	GO TO 1200	96
C	DO EXP AND CONT LOSSES FOR BLOCKAGE	98
1100	PCOMP = ((XNOCONT*AKSUBC(M)) + (XNOEXP*AKSUBE(M)))	100
1	*(ABS(U(M,N))*U(M,N) / (2.*GSUBC))	101
	PINC(N) = PINC(N) + PCOMP	102
1200	CONTINUE	103
	DELTAP = DELTAP + PINC(N)	104
2000	CONTINUE	105
C	ADD ENTRANCE LOSS IF REQD	107
	IF (INSTART .EQ. 1) DELTAP = DELTAP + PENT	109
	RETURN	111
	END	112

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SUBROUTINE RADDELP(MM,NSTART,NSTOP)
C THIS SUBROUTINE CALCULATES THE RADIAL PRESSURE DIFFERENCE ACROSS
C A NUMBER OF CHANNELS UPSTREAM OF A BLOCKAGE
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLK ,
2 LENGTHB ,LOCBLK ,FINDIA ,PINPICH ,CLADT ,
3 PINN (14) ,AREA (14,41) ,EQUARIA (14) ,
4 ARATIO (14) ,CRATIO (14) ,BUPAREA (14) ,
5 BYDOWNA (14) ,SIGMA (14) ,KB (14) ,
6 IB (14) ,JB (41) ,DUM ,
7 AXLOC (41)
COMMON /BLOC/
1 BFLOWA (14) ,DEQB (14) ,
2 NOBLK0 ,NOCONT ,NOEXF ,
3 ISYM ,BLOKDLG (14) ,DEQ (14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE (10) ,PCRT
2 ,HEAD (24) ,FAREA ,NSTAGL ,ISTAGE ,EGHYD
3 ,PEAKPOW ,DIVL ,PP ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCONT ,STAGINP ,PINCHES ,DELTAP
COMMON /POWR/
1 POWNORM (41) ,POWLIN (41) ,JPERDIV (14,41) ,
2 POWVAR (14) ,TOUT ,TIN ,
3 VARPROP ,CPBARS (14,41) ,H (14,41)
COMMON /FLOW/
1 P (14,41) ,U (14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNUS (14,41) ,
3 FRICS (14,41) ,RHOS (14,41) ,AMUSE (14,41) ,
4 W (13,41) ,AKEY (13,41) ,AKSUBC (14) ,
5 AKSUBE (14) ,PING (41) ,TRY (14,3) ,
6 C (14) ,POUT ,WCPY ,
7 ,RK (13,41) ,RCONST ,RCONSTL ,
8 ,WMAX (14) ,ENTH ,BETA
DELTAP = 0.
DO 500 MM = NSTART, NSTOP
C ASSUME RADIAL FLOWS ARE POSITIVE
AONE = (1./2.)* (CRATIO(MM-1) + CRATIO(MM))
ATWO = (1./2.)* (CRATIO(MM+1) + CRATIO(MM))
IF (AKEY(MM,NN) .LT. 0 ) WRITE(6,10)
10 FORMAT (44H RADIAL DIRECTION OF FLOW INCORRECT, RADDELP)
DELTAP = DELTAP
1 + ( W(MM-1,NN)*ABS(W(MM-1,NN)) + ( (FSUBG*FSUBH*((AONE/ATWO)-1.
2 )) + (RK(MM-1,NN)/2.0) ) / (RHOS(MM-1,NN)*GSUBC*((PF/12.)**2))
500 CONTINUE
RETURN
END

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SUBROUTINE UPPIT
THIS SUBROUTINE CALCULATES THE FLOWS UPSTREAM OF A BLOCKAGE
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGTHB ,LOCBLOK ,PINDIA ,PINPICH ,CLADT ,
3 PINNO(14) ,AREA(14,41) ,EQUARIA(14) ,
4 ARATIO(14) ,CRATIO(14) ,BUPAKEA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,KB(14) ,
6 IB(14) ,JB(41) ,DUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DEQB(14) ,
2 NOBLOKD ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,PCRT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKPOW ,DIVL ,PF ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCONT ,STAGINF ,PINCHES ,DELTAF
COMMON /POWER/
1 POWNORM(41) ,POWLIN(41) ,QPERDIV(14,+1) ,
2 POWVAR(14) ,TOUT ,TIN ,
3 VAKPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,RLYNOS(14,41) ,
3 FRICS(14,+1) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,POUT ,WCRY ,
7 ,RK(13,41) ,RCONST ,RCONSTL ,
8 ,WMAX(14) ,ENTH ,BETA
DIMENSION VAVG(14)
IF(NSTAGE .GT. 1) GO TO 5
DO 4 MDUM = 2, NOCHAN
IF (VAVG(MDUM) .EQ. 0.) VAVG(MDUM) = U(MDUM,NODIV)
4 CONTINUE
5 CONTINUE
NEND = LOCBLOK + LENGTHB
NHAF = NEND + ((NODIV - NEND)/2)
POUT2 = POUT * 144.0
KTRUBLE = ITRUBLE
ITRUBLE = JTRUBLE(2)
IF (LOCBLOK .GT. 1) GO TO 10
WRITE(6,9)
9 FORMAT(*NO UPSTREAM BYPASS FLOW *)
RETURN
C BYPASS FLOW PATH
10 MTOT = NOCHAN - 1
NN = LOCBLOK - 1
DO 201 MM = 2, MTOT
M = MM
KB(MM)=0
RADMASS = W(MM,NN) * BLUP *CRATIO(M) /12.
AXMASS = RHOS(M,LOCBLOK) * AREA(M,LOCBLOK) * U(M,LOCBLOK)
IF (RADMASS .GT. AXMASS) KB(MM)=1
201 CONTINUE

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      NIT = NOCHAN - 1
      ITIMES = 0
190  ITIMES = ITIMES + 1
      IDLE = 0
      M = NOCHAN
      NSTART = 1
      NSTOP = NODIV
      TRY(M,1) = TRY(M-1,1)
      TRY(M,2) = TRY(M-1,2)
191  CALL AXDELP(M,NSTART,NSTOP)
      IDLE = IDLE + 1
      IF (IDLE .LT. 10) GO TO 192
      WRITE (6,199)
199  FORMAT (* MORE THAN 10 ITERATIONS FOR M=NOCHAN, UPSREAR *)
      STOP 31
192  DV1 = (- (POUT2 - (STAGINP - DELTAP)) /
1      ((TRY(M,1) + TRY(M,2)) * ABS(U(M,1)) * 2.)) * 1.2
      U(M,1) = U(M,1) + DV1
      CALL CONTIN(NOCHAN,NOCHAN,2,NODIV)
      ACON = ABS(DV1)
      IF (ACON .LT. 0.10) GO TO 193
      IF (ITRUBLE .EQ. 0) GO TO 324
      WRITE (6,322)
322  FORMAT (* ADJUST NOCHAN VELOCITIES *)
      CALL PIKL(9HDV1 ,DV1)
      DO 323 N=1,NODIV
      CALL PIKL(9HN ,N ,9HU(NOCHAN) ,U(M,N))
323  CONTINUE
324  GO TO 191
193  CONTINUE
C    ADJUST PRESSURES IN M = NOCHAN
      CALL AXDELP(M, START,NSTOP)
      DO 195 NN = 1, NODIV
      N=NODIV + 1 - NN
      P(M,N) = P(M,N+1) + PINO(N)
      IF (ITRUBLE .EQ. 0) GO TO 195
      CALL PIKL(9HN ,N ,9HP(M,N) ,P(N,N))
195  CONTINUE
      DO 2000 MDUM = 2,NIT
      M = NIT - MDUM + 2
      IDO = 0
      ISW = 0
      IBY = 1
      MGO = M+1
      DO 325 MM=MGO,NOCHAN
      IF (KB(MM).EQ. 0) GO TO 330
      IBY = IBY + 1
325  CONTINUE
330  CONTINUE
      MSTOP = M + IBY
      LL = LOCBLOK - 1
      DLONE= RHOS(M,LL) * EQUARIA(M)
      DLTWO= RHOS(M,LL+1) * AREA(M,LL+1)
      DLTHRE= RHOS(M,LL) * SUPAREA(M)
100  IDO = IDO + 1
C    ITERATION COEFFICIENTS ARE TRY = -PCHANGE/(2.0 * ABS(V) * DELV)
C    UPSTREAM PRESSURE DIFF FOR PRESSURE AT BYPASS
150  NSTART = 1

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      NSTOP = LOCBLOK - 2
      CALL AXDELP(M,NSTART,NSTOP)
      P2UP = STAGINP - DELTAP
      IF (U(M,LOCBLOK) .EQ. 0.0) GO TO 500
      NSTART = LOCBLOK-1
      NSTOP = NODIV
      CALL AXDELP(M,NSTART,NSTOP)
      P2DOWN = POUT2+ DELTAP
500  NN = LOCBLOK - 1
      NSTART = M + 1
      NSTOP = MSTOP
      CALL RADDELP(NN,NSTART,NSTOP)
      P2BY = POUT2+ DELTAP
      NSTART = LOCBLOK - 1
      NSTOP = NODIV
      CALL AXDELP(MSTOP,NSTART,NSTOP)
      P2BY = P2BY + DELTAP
      VAVG(M)=.4* (((ABS(U(M,LOCBLOK))* (P2DOWN-P(M,NEND)))
1      + (ABS(U(M,NHALF)) * (P(M,NEND) -POUT2)))/(P2DOWN-POUT2))
2      +.6*VAVG(M)
      IF (M .GT. NUMBLOK ) VAVG(M)=U(M,NODIV)
      IF (ISW .GT. 0 ) GO TO 504
      SAVEA = P2UP
      SAVEB = P2DOWN
      SAVEC = P2BY
      ISW = 1
      GO TO 505
504  IF (U(M,1) .EQ. 0.) GO TO 506
      TRY(M,1)=((- (P2UP - SAVEA) / (2.0* ABS(U(M,1))*DV1))*3)
      1 +(.7*TRY(M,1))
506  IF (U(M,LOCBLOK) .EQ. 0.) GO TO 5060
      TRY(M,2)=(.3*(P2DOWN - SAVEB) / (2.0*VAVG(M) *DV2))
      1 +(.7*TRY(M,2))
5060 TRY(M,3)=(.3*(P2BY - SAVEC) / (2.0*ABS(U(MSTOP,NODIV))*DV3))
      1 +(.7*TRY(M,3))
      ISW = 0
505  CONTINUE
      IF (ITRUBLE .EQ. 0) GO TO 507
      CALL PIKL(9HTRY(M,1) ,TRY(M,1) ,9HTRY(M,2) ,TRY(M,2)
1      ,9HTRY(M,3) ,TRY(M,3) ,9HM ,M
2      ,9HP2UP ,P2UP ,9HP2DOWN ,P2DOWN
3      ,9HP2BY ,P2BY )
507  IF (U(M,1).EQ. 0.) GO TO 508
      IF (U(M,LOCBLOK) .EQ. 0.) GO TO 508
      ERROR = P2UP - P2DOWN
      ERROR = ABS(ERROR)
      IF (ERROR .GT. PCRT) GO TO 510
508  ERROR = P2UP - P2BY
      ERROR = ABS(ERROR)
      IF (ERROR .GT. PCRT) GO TO 510
      GO TO 1000
510  IF (IDO .LT. 1) GO TO 515
      WRITE (6,511) M
511  FORMAT(*SUBRTNE UPPIT - MORE THAN 10 PRESSURE ITERATIONS FOR M=*,
1      1I2 )
      GO TO 2000
515  CONTINUE
C    SKIP THIS IF AREA(M,LOCBLOK) = ZERO

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C IF (AREA(M,LOCBLOK) .EQ. 0.) GO TO 260
SOLVE FOR VELOCITY CHANGES DV1,DV2,DV3
AC1 = (P2DOWN - P2BY) / (2.*TRY(M,3)*ABS(U(MSTOP,NO DIV)))
AC2 = (TRY(M,2) * VAVG(M))/
1 (TRY(M,3) * ABS(U(MSTOP,NO DIV)))
AC3 = -(P2DOWN - P2UP) / (2.*TRY(M,1)*ABS(U(M,1)))
AC4 = -(TRY(M,2) * VAVG(M))/
1 (TRY(M,1) * ABS(U(M,1)))
DV2 = ((DLONE * AC3) - (DLTHREE * AC1)) /
1 (DLTWO - (AC4 * DLONE) + (DLTHREE * AC2))
DV3 = AC1 + (AC2 * DV2)
DV1 = AC3 + (AC4 * DV2)
GO TO 230
200 DV2 = 0.
AC5 = (P2UP - P2BY) / (2.*TRY(M,3)*ABS(U(MSTOP,NO DIV)))
AC6 = -(TRY(M,1)*ABS(U(M,1)))/(TRY(M,3)*ABS(U(MSTOP,NO DIV)))
DV1 = 1.1*(DLTHREE * AC5)/(DLONE - (DLTHREE*AC6))
DV3 = AC5 + (AC6*DV1)
230 LEND = LOCBLOK + LENGTHB - 1
IF (ITRUBLE .EQ. 0) GO TO 255
CALL PIKL(9HDV1 ,DV1 ,9HDV2 ,DV2
1 ,9HDV3 ,DV3 ,9HGOBL ,GOBL )
C RADIAL FLOW CHANGE UPSTREAM OF BLOCKAGE
C W S ARE ON A BASIS OF FLOW PER UNIT LENGTH PER CROSS CHANNEL
C BUT NOT EQUIVALENT CROSS CHANNEL (IE, TOTAL CROSS FLOW)
255 LL = LOCBLOK -
MEND = MSTOP - 1
DO 270 MUM = M,MEND
W(MUM,LL) = W(MUM,LL) + (DV3 * DLTHREE * CRATIO(M) /
1 ((BLUP/12.)*CRATIO(MUM)))
C CHECK AKEYS
AKEY(MUM,LL) = +1.0
IF (W(MUM,LL) .LT. 0.) AKEY(M,LL) = -1.0
IF (ITRUBLE .EQ. 0) GO TO 270
CALL PIKL(9HW(,LL) ,W(MUM,LL) ,9HN ,LL )
270 CONTINUE
C CHANGE VELOCITIES
C AXIAL DIRECTION
NNN = LOCBLOK -1
VELSAVE = U(M,LOCBLOK)
U(M,1) = U(M,1) + (DV1 * RHUS(M,NNN)/RHOS(M,1))
CALL CONTIN(M,M,2,NO DIV)
DV2 = U(M,LOCBLOK) - VELSAVE
CALL CONTIN(MSTOP,MSTOP,2,NO DIV)
IF (ITRUBLE .EQ. 0) GO TO 280
CALL PIKL(5HM ,M ,5HMSSTOP ,MSTOP)
DO 254 NN =1,NO DIV
CALL PIKL(
1 ,9HU(M,N) ,U(M,NN) ,9HU(MSTOP,N) ,NN
2 , )
254 CONTINUE
C DETERMINE CROSSFLOW RESISTANCES
280 NGOO= LOCBLOK -1
NSTP = NGOO
MGOO= 2
MSTP = NOCHAN -1
C TEMPORARY *****
C CALL RESIST (MGOO,MSTP,NGOO,NSTP)

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      GO TO 100
1000 CONTINUE
C      CALCULATE THE PRESSURE DISTRIBUTIONS IN THE CHANNELS FOR WHICH
C      THE VELOCITIES HAVE BEEN ADJUSTED
      IF (AREA(M,LOCBLOK) .EQ. 0) GO TO 1300
      CALL AXDELP (M,1,NODIV)
      DO 1100 NN = 1,NODIV
      N = NODIV - NN + 1
      P(M,N) = P(M,N+1) + PINC(N)
1100 CONTINUE
      CALL AXDELP(MSTOP,1,NODIV )
      DO 1250 NN = 1,NODIV
      N = NODIV - NN + 1
      P(MSTOP,N) = P(MSTOP,N+1) + PINC(N)
1250 CONTINUE
      GO TO 1900
C      FOR AREA(M,LOCBLOK) EQ ZERO
1300 CALL AXDELP(MSTOP,1,NODIV)
      DO 1350 NN=1,NODIV
      N=NODIV - NN + 1
      P(MSTOP,N) = P(MSTOP,N+1) + PINC(N)
1350 CONTINUE
      LL = LOCBLOK - 1
      LLL= LL-1
      NSTART = M
      CALL KADDELP(LL,NSTART,MSSTOP)
      P(M,LL) = P(MSTOP,LL) + DELTAP
      CALL AXDELP(M,1,LLL)
      DO 1400 NN =1,LL
      N = LOCBLOK - NN
      P(M,N) = P(M,N+1) + PINC(N)
1400 CONTINUE
1900 IF (ITRUBLE .EQ. 0) GO TO 2000
      CALL PIKL(5HM ,1,5HMSTOP,MSSTOP )
      DO 1360 NN=1,NODIV
      N = NODIV -NN +1
1360 CALL PIKL(9HN ,N
1 ,9HP(M,N) ,P(N,N) ,9HP(MSTOP,N) ,P(MSTOP,N))
2000 CONTINUE
      IF (ITIMES .LT. 5 ) GO TO 190
      ITRUBLE = KTRUBLE
      RETURN
      END

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SUBROUTINE DOWNIT
THIS SUBROUTINE CALCULATES THE FLOW DISTRIBUTION
DOWNSTREAM OF A BLOCKAGE
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGTHB ,LOCBLOK ,PINDIA ,PINPICH ,CLADT ,
3 PINNO(14) ,AREA(14,41) ,EQUAREA(14) ,
4 CRATIO(14) ,CRATIO(14) ,RUPAREA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,KB(14) ,
6 IB(14) ,JB(41) ,DUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DLOB(14) ,
2 NOBLOK ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEQ(14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,PCRT
2 ,HEAD(24) ,FAREA ,NSTAGL ,ISTAGE ,EQHYD
3 ,PEAKPOW ,DIVL ,PP ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCONT ,STAGINF ,PINCHES ,DELTAP
COMMON /POWER/
1 POWNORM(41) ,POWLIN(41) ,JPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,TIN ,
3 VARFPOP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNUS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKLY(13,41) ,AKSUBC(14) ,
5 AKSUBE(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,PGUT ,WCRY ,
7 ,RK(13,41) ,RCONSI ,RCUNSL ,
8 ,WMAX(14) ,ENTH ,BETA
DIMENSION SMA(14) ,BIGA(14) ,WOLD(14)
1 ,SMB(14) ,BIGB(14) ,CONSI(14)
2 ,SMC(14) ,BIGC(14) ,PNEW(4)
3 ,SMD(14) ,BIGD(14) ,DW(4)
4 ,SME(14) ,BIGS(14) ,BP(14)
5 ,CP(14)
6 ,SP(14)
7 ,WSAVE(41)

ZER = 0.
NTIMES = J
NUP = LOCBLOK + LENGTHB
DELL = NODIV - NUP + 1
NOELL = DELL
NK = NUP
XNUP = NUP
KUP = NUP + 1
C RESISTANCES BASED ON VELOCITIES AT NUP + (.5*DELL)
50 DO 75 M = 2,NOCHAN
MDUM = M+1
IF (AKEY(M,NK) .GT. ZER) MDUM=M
AONE = (1./2.) * (CRATIO(M-1) + CRATIO(M))
ATWO = (1./2.) * (CRATIO(M+1) + CRATIO(M))
IF (AKEY(M,NK) .LT. ZER) AA = AONE
IF (AKEY(M,NK) .LT. ZER) AONE = ATWO

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      IF (AKEY(M,NK) .LT. ZER) ATWO = AA
      CONST(M) = (RHJS(MDUM,NK)*GSUBC*((PP/12.)**2.))/
1      ( (FSUBG * FSUBH * ((AONE/ATWO) - 1.))
2      +RK(M,NK)/2. )
75 CONTINUE
C   OBTAIN CONSTANTS FOR
C       P = SMA + SMB*W(M-1) + SMC*W(M-1)**2
C       SMD*W(M) + SME*W(M)**2
C   DO NOT CHANGE ***
80 DO 200 M=2,NOCHAN
   CALL AXDELP(M,NK ,NODIV)
   PZERO = DELTAP + (POUT * 1+4.)
   PZERO = P(M,NUP)
   DELW1 = .001
   DELW2 = .001
   MTEST = M
   IF (M .EQ. NOCHAN) MTEST = M-1
   IF (WMAX(MTEST) .EQ. ZER) GO TO 90
   DELW1 = ABS(.1*WMAX(M-1) )
   DELW2 = ABS(.1*WMAX(M) )
90 DW(1)= DELW1
   DW(2)=-DELW1
   DW(3)= DELW2
   DW(4)=-DELW2
   MLIM = NOCHAN
   JGO = 1
   IF(M .EQ. 2) JGO = 3
   JSTOP = 4
   IF (M .EQ. MLIM) JSTOP =2
   DO 110 J=JGO,JSTOP
   MVAL = M-1
   IF (J .GT. 2) MVAL = M
   STOREW = WMAX(MVAL)
C   ***** TEMPORARY *****
C   IF (MVAL .EQ. NUMBLOK ) ENTH = 1.0
   DO 100 NN = NUP,NODIV
   WSAVE(NN) = W(MVAL,NN)
   DELX = NN- NUP + 1
   DELXP = DELX - 1.
   WCON = -(STOREW + DW(J)) * DELL / (ENTH + 1.)
   W(MVAL,NN) = WCON*((1.-DELX/DELL)**(ENTH+1.)
1   -(1.-DELXP/DELL)**(ENTH+1.) )
   AKEY(MVAL,NN) = -1.0
   IF (W(MVAL,NUP) .GT. ZER) AKEY(MVAL,NN) = 1.0
100 CONTINUE
   CALL CONTIN (M,M,KUP,NODIV)
   CALL AXDELP (M,NK ,NODIV)
   PNEW(J) = DELTAP +(POUT * 1+4.0)
   DO 105 NN = NUP,NODIV
   W(MVAL,NN) = WSAVE(NN)
   AKEY(MVAL,NN) = -1.0
   IF (W(MVAL,NUP) .GT. ZER) AKEY(MVAL,NN) = 1.0
   CALL CONTIN (M,M,KUP,NODIV)
105 CONTINUE
110 CONTINUE
   PK = PNEW(1)
   PL = PNEW(2)
   PO = PZERO

```

```

      PM = PNEW(3)
      PN = PNEW(4)
      IF (ITRUBLE.EQ. 0) GO TO 115
      CALL PIKL(6HPK ,PK,6HPL ,PL,6HPO ,PO
1      ,6HPM ,PM,6HPN ,FN,6HDELW1 ,DELW1
2      ,6HDELW2 ,DELW2 )
115 IF (DELW1 .EQ. ZER) GO TO 120
      SMC(M)=.5*(PK -(2.*PO) +PL)/(DELW1**2)
      SMB(M) =((PK-PL) / (2.*DELW1))-((SMC(M) * WMAX(M-1) )*2.0)
120 IF (M .EQ. 2) SMC(M) = ZER
      IF (M .EQ. 2) SMB(M) = ZER
      IF (DELW2 .EQ. ZER) GO TO 130
      SME(M)=.5*(PM -(2.*PO) +PN)/(DELW2**2)
      SMD(M) =((PM-PN) / (2.*DELW2))-((SME(M) * WMAX(M) )*2.0)
130 I = NOCHAN
      IF (M .EQ. I) SME(M) = ZER
      IF (M .EQ. I) SMD(M) = ZER
      SMA(M) = PO - ( (SMB(M)*WMAX(M-1) )+(SMC(M)*(WMAX(M-1) )**2)
1      + (SMD(M)*WMAX(M) ) + (SME(M)*(WMAX(M) **2)) )
200 CONTINUE
      IF (ITRUBLE .EQ. 0) GO TO 206
      DO 205 M = 2,NOCHAN
      CALL PIKL(6HM ,M ,6HSHA(M) ,SMA(M)
1      ,6HSM3(M) ,SMB(M) ,6HSMC(M) ,SMC(M)
2      ,6HSMO(M) ,SMD(M) ,6HSM2(M) ,SME(M) )
205 CONTINUE
206 CONTINUE
C CALCULATE COEFFICIENTS FOR W-CROSSFLOW MATRIX
      JTIES = J
210 JTIES = JTIES + 1
      IF (JTIES .GT. 50) STOP 23
      MEND = NOCHAN - 1
      DO 300 M = 2,MEND
      BIGA(M) = SMB(M) + (SMC(M) * WMAX(M-1) )
      BIGB(M) = SMD(M) + (SME(M) * WMAX(M) ) -SMB(M+1) - (SMC(M+1) *
1      WMAX(M) ) - (ABS(WMAX(M) )/ CONST(1) )
      BIGC(M) =-SMD(M+1) - (SME(M+1)*WMAX(M+1) )
      BIGS(M) =-SMA(M) + SMA(M+1)
      IF (ITRUBLE .EQ. 0) GO TO 300
      CALL PIKL(9HM ,M ,9HBIGA(M) ,BIGA(M)
1      ,9HBIGB(M) ,BIGB(M) ,9HBIGC(M) ,BIGC(M)
2      ,9HBIGS(M) ,BIGS(M) )
300 CONTINUE
C STORE VALUES OF WMAX(M)
      DO 350 M= 2,MEND
      WOLD(M) = WMAX(M)
350 CONTINUE
C CONVERT MATRIX
      BP(2) = 1.0
      CP(2) = BIGC(2)/BIGB(2)
      SP(2) = BIGS(2)/BIGB(2)
      IF(MEND .EQ. 2) GO TO 500
      DO 500 M =3,MEND
      BP(M) = (BIGB(M)/BIGA(M)) - CP(M-1)
      CP(M) = BIGC(M)/(BIGA(M) * BP(M))
      SP(M) =((BIGS(M)/ BIGA(M)) - SP(M-1))/BP(M)
C 500 NOW TAKEN TO BE 1.0
500 CONTINUE

```

```

550 WMAX(MEND) = SP(MEND)
   IF(MEND .EQ. 2) GO TO 600
   MAX = NOCHAN - 3
   DO 575 MM = 1,MAX
   M = MEND - MM
   WMAX(M) = SP(M) - (CP(M)*WMAX(M+1) )
575 CONTINUE
600 CONTINUE
C   W DISTRIBUTION
   DO 700 M= 2,MEND
C   ***** TEMPORARY *****
C   IF (M .EQ. NUMBLOK) ENTH = 1.
   WMAX(M) = (.4*WOLD(M)) + (.6*WMAX(M) )
   DO 650 N= NUP,NODIV
   DELX = N - NUP + 1
   DELXP = DELX - 1.
   WCON = -WMAX(M)*DELL/(ENTH+1.)
   W(M,N)=WCON*((1.-DELX/DELL)**(ENTH+1.)
1      -(1.-DELXP/DELL)**(ENTH+1.) )
   AKEY(M,N) = - 1.0
   IF(W(M,N) .GT. ZER) AKEY(M,N) = 1.0
650 CONTINUE
   IF (ITRUBLE .EQ. 0) GO TO 700
   CALL PIKL(9HM      ,M      ,9HWOLD(M)      ,WOLD(M)
1      ,9HW(M,NUP)    ,W(M,NUP) ,9HBP(M)      ,BP(M)
2      ,9HCP(M)      ,CP(M)    ,9HSP(M)      ,SP(M) )
700 CONTINUE
   CALL CONTIN(2,NOCHAN,KUP,NODIV)
C   PRESERVE CONTINUITY AND CHANGE PRESSURE DISTRIBUTIONS
   DO 750 M=2,NOCHAN
   CALL AXDELP (M,NUP,NODIV)
   DO 740 I = NUP,NODIV
   N = NODIV + NUP - I
   P(M,N) = P(M,N+1) + PINC(N)
740 CONTINUE
750 CONTINUE
C   COMPARE WITH PREVIOUS W VALUES (WOLD(M))
   IF (WCRY .EQ. ZER) WCRY =0.10
   DO 800 M= 2,MEND
   FRACW = (ABS(WMAX(M) ) - ABS(WOLD(M)) ) / ABS(WMAX(M) )
   IF (FRACW .GT. WCRY ) GO TO 210
800 CONTINUE
C   CHANGE CROSSFLOW RESISTANCES AND REITERATE
C   ***** TEMPORARY*****
   IF (RCONST .GT. ZER) GO TO 900
C   CALL RESIST(2,MEND,NUP,NODIV)
   NTIMES = NTIMES + 1
   IF (NTIMES .GT. 5 ) GO TO 900
   GO TO 50
900 CONTINUE
   RETURN
   END

```



```

SUBROUTINE ENERGY
THIS SUBROUTINE CALCULATES THE ENTHALPY DISTRIBUTION IN
THE COOLANT CHANNELS
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENG HB ,LOCBLOK ,PINDIA ,PINPICH ,CLADI ,
3 PINNO(14) ,AREA(14,41) ,EQUARIA(14) ,
4 ARATIO(14) ,CRATIO(14) ,BUPAREA(14) ,
5 BYDOWNA(14) ,SIGMA(14) ,KB(14) ,
6 IB(14) ,JB(41) ,JUM ,
7 AXLOC(41)

COMMON /BLOC/
1 3FLOWA(14) ,DLUR(14) ,
2 NOBLOK ,NOCNT ,NOEXP ,
3 ISYM ,BLOCKDEG(14) ,DEQ(14,41)

COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(14) ,PCRT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKPOW ,DIVL ,PF ,BLUP ,GSUBC
4 ,XNOEXP ,XNOCNT ,STAGINP ,PINCHES ,DELIAP

COMMON /POWER/
1 POWNORM(41) ,POWLIN(41) ,QPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,FIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)

COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNOS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,ARUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBL(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,PGUT ,WCRY ,
7 ,RK(13,41) ,RCNST ,RCNSTL ,
8 ,WMAX(14) ,ENTH ,BETA

CALCULATE ENTHALPIES
DIMENSION HNEW(14) ,DOTH(14) ,HRCN=W(14)
JIT = NODIV-1
DO 300 N=1,JIT
IF(N.EQ.(LOCBLOK-1)) GO TO 150
IF(ABS(W(NJHBLOK,N)) .LT. 0.0001) GO TO 70
IF(N.GE.(LOCBLOK + LENGTHB)) GO TO 150
70 DO 75 M=2,NOCHAN
IF(M.EQ.2) W(M-1,N)=0.
IF(M.EQ.NOCHAN) W(M,N)=0.
WPI=BETA*.5*(RHOS(M-1,N)*U(M-1,N)+RHOS(M,N)*U(M,N))*PP/12.
WPO=BETA*.5*(RHOS(M,N)*U(M,N)+RHOS(M+1,N)*U(M+1,N))*PP/12.
WDELHI=WPI*CRATIO(M-1)*(H(M-1,N)-H(M,N))*DIVL/12.
WDELHO=WPO*CRATIO(M)*(H(M+1,N)-H(M,N))*DIVL/12.
IF(M.EQ.2) WDELHI=0.
IF(M.EQ.NOCHAN) WDELHO=0.
WDELH=WDELHI+WDELHO
IF(JB(N).EQ.0) WDELH=0.
DOTF=RHOS(M,N)*AREA(M,N)*U(M,N)
H(M,N+1)=H(M,N)+(QPERDIV(M,N)+WDELH)/DOTF
75 CONTINUE
GO TO 300

```

```

150 CONTINUE
DO 100 M=2,NOCHAN
  IF (M.EQ. 2) W(M-1,N)=0.
  IF (M.EQ. NOCHAN) W(M,N)=0.
  HNEW(M) = H(M,N)
  DOTM(M) = RHOS(M,N)*AREA(M,N)*U(M,N)
100 CONTINUE
  IF(N.GE. LOCBLOK) GO TO 200
  FACT = BLUP
  A=2.878
  XL=0.
  DO 130 NTIM=1,50
    XTIM = 50-NTIM
    XDIV = (BLUP/12.)*10.**(-6.)*XTIM**A
    XL = XL + XDIV
  DO 120 M=2,NOCHAN
    FLO=DOTM(M)
    DOTM(M)= DOTM(M) + ((W(M-1,N)*CRATIO(M-1))-(W(M,N)*CRATIO(M)))*
      1 XDIV
    HRNEW(M)= ( -W(M,N)*CRATIO(M)*XDIV*HNEW(M) +FLO*HNEW(M)
      1 +W(M-1,N)*CRATIO(M-1)*XDIV*HN=W(M-1)
      2 +QPERDIV(M,N) *XDIV/(DIVL/12.))/DOTM(M)
119 IF(NTIM.EQ. 50) H(M,N+1)=HRNEW(M)
120 CONTINUE
  DO 125 M=2,NOCHAN
    HNEW(M) = HRNEW(M)
125 CONTINUE
130 CONTINUE
  GO TO 300
200 CONTINUE
  DO 260 NTIM=1,50
    IF(N.EQ. (LOCBLK+LENGTHB)) GO TO 230
    XDIV = DIVL/600.
    XFRAC = 1.3/50.
    GO TO 240
230 XTIM = NTIM
    XDIV = (DIVL/12.)*10.**(-6.)*XTIM**A
    XFRAC= XDIV/(DIVL/12.)
240 DO 250 MM=2,NOCHAN
    M = NOCHAN + 2 - MM
    FLO=DOTM(M)
    DOTM(M) = DOTM(M) + ((W(M-1,N)*CRATIO(M-1))-(W(M,N)*CRATIO(M)))*
      1 XDIV
    APART= W(M-1,N)*CRATIO(M-1)*XDIV*HNEW(M)
    BPART= -W(M,N)*CRATIO(M)*XDIV*HNEW(M+1) +FLO*HNEW(M)
    CPART= +QPERDIV(M,N)* XFRAC
    DPART=
    HRNEW(M) = (APART+BPART+CPART+DPART)/DOTM(M)
249 IF(NTIM.EQ. 51) H(M,N+1) = HRNEW(M)
250 CONTINUE
  DO 255 M=2,NOCHAN
    HNEW(M) = HRNEW(M)
255 CONTINUE
260 CONTINUE
  IF (ITRUBLE.EQ. 0) GO TO 300
  CALL PIKL(7HM ,M ,7HN ,N
      1 ,7HH(1,N) ,H(1,N) ,7HH(2,N) ,H(2,N)
      2 ,7HH(3,N) ,H(3,N) ,7HH(4,N) ,H(4,N)

```

```
      3      ,9HH(NOCHAN)      ,H(NOCHAN,N)  
      4      ,9HH NOCHN+1      ,H(JIT,N) )  
300 CONTINUE  
    RETURN  
    END
```

```

SUBROUTINE PROPS
C THIS SUBROUTINE CALCULATES THE FRICTION FACTORS, REYNOLDS NUMBERS
C AND, IF REQUIRED, THE CHANGES IN COOLANT PROPERTIES
COMMON /GEOMET/
1 NOCHAN ,PLENGTH ,LENGTHA ,NODIV ,NUMBLOK ,
2 LENGTHB ,LOCBLOK ,PINDIA ,PINPICH ,CLADT ,
3 PINNG(14) ,AREA(14,41) ,EQUARIA(14) ,
4 ARATIO(14) ,CRATIO(14) ,BUPAREA(14) ,
5 BYDOANA(14) ,SIGMA(14) ,KB(14) ,
6 IB(14) ,JE(41) ,DUM ,
7 AXLOC(41)
COMMON /BLOC/
1 BFLOWA(14) ,DEQB(14) ,
2 NOBLOKD ,NOCONT ,NOEXP ,
3 ISYM ,BLOKDEG(14) ,DEG(14,41)
COMMON /GENL/
1 ITRUBLE ,M ,N ,JTRUBLE(10) ,FCRIT
2 ,HEAD(24) ,FAREA ,NSTAGE ,ISTAGE ,EQHYD
3 ,PEAKPOW ,DIVL ,PP ,BLUP ,GSUBC
4 ,XNOEXP ,XNCCONT ,STAGINP ,PINCHES ,DELTAIP
COMMON /POW/
1 POWNORM(41) ,POWLIN(41) ,UPERDIV(14,41) ,
2 POWVAR(14) ,TOUT ,TIN ,
3 VARPROP ,CPBARS(14,41) ,H(14,41)
COMMON /FLOW/
1 P(14,41) ,U(14,41) ,FSUBC ,
2 FSUBG ,FSUBH ,REYNOS(14,41) ,
3 FRICS(14,41) ,RHOS(14,41) ,AMUSE(14,41) ,
4 W(13,41) ,AKEY(13,41) ,AKSUBC(14) ,
5 AKSUBC(14) ,PINC(41) ,TRY(14,3) ,
6 C(14) ,POUT ,WCRY ,
7 ,RK(13,41) ,RCONST ,RCONSTL ,
8 ,WMAX(14) ,ENTH ,BETA
ZER = 0.
IF (VARPROP .EQ. ZER) GO TO 500
C PROPERTIES ARE CALCULATED AS IN SUBROUTINE SETUP
DO 300 N=1,NODIV
DO 300 M=2,NOCHAN
TEMPF = (H(M,N)/CPBARS(M,N)) + TIN
TEMPR = TEMPF + 460.
TEMPK = ((TEMPF - 32.0)/1.8) + 273.16
C SPECIFIC HEAT
CPBARS(M,N) = 0.384352 - ((1.16599 / (10.0**4)) * TEMPR)
+ (3.41178 / (10.0**8)) * (TEMPR**2)
1
C DENSITY
RHOS(M,N) = 59.566 - ((7.9504 / (10.0**3)) * TEMPF)
1 - ((.2672 / (10.0**6)) * (TEMPF **2))
2 + ((.6035 / (10.0**10)) * (TEMPF **3))
C VISCOSITY
AMULOG = 0.516 + (220.65 / TEMPK) - (0.4925 * ALOG10(TEMPK))
AMUSE(M,N) = (10.0 ** AMULOG) / 47900.0
300 CONTINUE
400 CONTINUE
IQUIT = NODIV + 1
DO 450 M=2,NOCHAN
RHOS(M,IQUIT) = RHOS(M,NODIV)
450 CONTINUE

```

```

500 CONTINUE
DO 800 N=1,NODIV
DO 700 M=2,NOCHAN
REYNOS(M,N) = RHOS(M,N) * U(M,N) * DEQ(M,N) / (GSUBC * AMUSE(M,N))
REYNOS(M,N) = ABS(REYNOS(M,N))
FRICS(M,N) = .316/REYNOS(M,N)**.250
700 CONTINUE
800 CONTINUE
JIT = NOCHAN +1
IF (ITRUBLE .EQ. 0) GO TO 900
DO 875 M=1,JIT
CALL PIKL(7HM ,M)
DO 850 N=1,IQUIT
CALL PIKL(7HN ,N ,7HSP. HT.,CPBARS(M,N)
1 ,7HDENSITY,RHOS(M,N),7HVISCONS ,AMUSE(M,N)
2 ,7HREYNO ,REYNOS(M,N) ,7HFRIC F ,FRICS(M,N) )
850 CONTINUE
875 CONTINUE
900 CONTINUE
RETURN
END

```

APPENDIX B
Model for Sodium Boiling With Superheat

1. General

A calculational model was developed to determine the effects of localized pockets of superheated sodium flashing to vapor within a fuel pin bundle. The objective has been to determine the history of the resulting bubble or void envelope and the associated fuel pin temperatures.

The process of bubble growth in the initial phase of the event and double slug expulsion in the latter phase is determined by solving the coupled energy and momentum equations. The energy and momentum equations have been coupled to the extent that the bubble size affects the condensation rate and the heat transfer rate to or from the liquid (or solid surfaces) within the bubble envelope. The bubble size or envelope is determined by solving the momentum equation. It has been assumed that a liquid film of relatively "cold" sodium is left on some fraction of the fuel pins within the bubble envelope at any one instant. This fraction of the total fuel pin area within the envelope has been assumed to be available for condensation of the hot vapor. The remaining fraction of the fuel pin surface area within the bubble envelope has been assumed to be contacted by the originally "hot" sodium liquid—this appears to be reasonable since some of the liquid should be carried along with the expanding vapor bubble.

The temperature of the vapor within the bubble has been taken to be equal to that of the "hot" liquid at any given instant. The bubble pressure is then equal to the saturation pressure corresponding to the "hot" liquid sodium temperature. In the calculation of "hot" liquid sodium temperature, a heat balance has been employed to account for the latent heat of vaporization of the sodium, the energy transferred by vapor condensation, and the energy transferred to or from the available fuel pin surfaces. An accounting of the "hot" liquid mass is made, and the bubble pressure is set to the "cold" (or normal) sodium vapor pressure corresponding to this temperature once the entire mass of hot liquid has flashed to vapor.

The bubble growth and collapse phases are followed by solving (1) the equation for spherical bubble growth and (2) the momentum equations for upper and lower liquid slugs once the bubble grows to the

size of the internal dimension of the fuel assembly wrapper can. It has been assumed that, during the phase of spherical bubble growth, the bubble is carried along with the surrounding coolant. This is true to the extent that the upper and lower bubble interfaces (two separate hemispherical interfaces) are given initial axial velocities equal to the average coolant velocity through the bundle. The fluid pressure at the wall of the wrapper can during spherical bubble growth is determined by the dynamics of the upper and lower liquid slugs. The boundary condition on these upper and lower slugs simply involves constant values of inlet and outlet fluid pressures.

The thermodynamic properties of sodium are taken from the equations given in ANL-7323.¹

2. Thermal Model

Figure B-1 is a schematic diagram illustrating the phenomena accounted for in the thermal model. Only one cladding temperature and one fuel temperature are calculated; this corresponds to the cladding and associated fuel that are first contacted by the hot sodium as the vapor bubble starts its growth. As the vapor bubble envelopes additional cladding and fuel, this mass addition effect is not reflected in the previously calculated temperatures; if properly accounted for, this effect would lower the average calculated temperature of the cladding and fuel masses. Our approach ensures that conservative values for sodium, cladding, and fuel temperatures are obtained.

The hot sodium liquid temperature at any instant is obtained by

$$T_{\text{sodium}} = T_{\text{initial}} - \frac{(V_B/v)\bar{h}_{fg} + m_{\text{cond}}\bar{h}_{fg} + {}_sQ_c}{\bar{m}_s(C_p)_s}. \quad (\text{B-1})$$

This equation accounts for the energy required to vaporize the sodium vapor in the bubble at any instant and the sodium that has been vaporized and subsequently condensed in the liquid film. A simplifying assumption is that the average latent heats of vaporization for the masses in the bubble and the mass that is condensed may be taken to be the time-averaged value up to the present calculated time. The sodium-to-cladding heat transfer is the integral of the rate as given by

$$\dot{q} = hA(T_s - T_c), \quad (B-2)$$

where the area for heat transfer is taken to be some fraction (input to the model) of the fuel pin surface area within the envelope of the vapor bubble. A similar expression is used for the heat transfer rate between the cladding and the fuel. The determination of fuel temperature also accounts for energy generation within the fuel.

3. Bubble and Slug Dynamics

The bubble growth is determined by integrating the interfacial accelerations with respect to time for an initial phase of spherical bubble growth and a subsequent phase of double slug expulsion. The interfacial acceleration for the spherical bubble growth is determined by an expression stemming from the specific momentum and continuity expressions for this geometry. The development of this expression can be seen in articles by Schlechtendahl² and Reynolds.³ The expression is as follows:

$$\ddot{R} = \frac{(P_B - P_W)r_W g_c}{\rho_\ell R(r_W - R)} - \frac{\dot{R}^2}{2} \left(\frac{3}{R} - \frac{r_W^2 + r_W R + R^2}{r_W^3} \right) + \frac{f}{6} \frac{\dot{R} |\dot{R}| (R^3 - r_W^3)}{D_{eB} (r_W - R) r_W^2}, \quad (B-3)$$

where the term involving the friction factor (f) and equivalent hydraulic diameter (D_{eB}) has been added to account for frictional effects.

The fluid pressure at the wrapper can wall adjacent to the spherical bubble appears in equation B-3. This pressure is taken to be a function of the constraint provided by the upper and lower slugs of sodium along with the upper and lower plenum fluid pressures. The wall pressure (P_W) may be obtained from the slug momentum equation as

$$P_W = \frac{\rho_\ell L}{g_c} \ddot{x} + f \rho_\ell \frac{\dot{x} |\dot{x}| L}{2 g_c D_{eS}} + \rho_\ell \frac{g}{g_c} L + P_L \quad (B-4)$$

for the wall pressure corresponding to the upper slug constraint. In the model there are actually two separate hemispherical bubbles, each with its associated acceleration, velocity, and displacements as well as wall pressure. The bubble pressures are identical, however. The slug accelerations, velocities, and lengths for the spherical bubble growth phase are obtained by considering mass conservation and are given by equation B-5:

$$\begin{aligned}x &= 4 \pi R^3 / (3 \sqrt{3} D_{can}^2). \\ \dot{x} &= dx/dt = 4 \pi R^2 \dot{R} / (\sqrt{3} D_{can}^2). \\ \ddot{x} &= d\dot{x}/dt = 4 \pi R (2\dot{R}^2 + R\ddot{R}) / (\sqrt{3} D_{can}^2).\end{aligned}\tag{B-5}$$

The slug length is obtained by calculating the difference between the original slug length and the bubble-slug interface displacement, or

$$L = L_{initial} - \left(x + \int_0^t V_{initial\ fluid} dt \right).\tag{B-6}$$

The spherical bubble growth phase ceases under one of two conditions; i. e. , (1) if the magnitude of the bubble radius approaches that of the effective fuel assembly wrapper can radius, or (2) if the bubble starts to collapse at any time. Under these conditions, the behavior of the upper and lower slugs determines the bubble behavior. The expression for the acceleration of the upper slug is as follows:

$$\ddot{x} = -\frac{g_c (P_L - P_B)}{\rho_l L} - f \frac{\dot{x}|\dot{x}|}{2D_{es}} - g.\tag{B-7}$$

The acceleration is integrated twice to obtain the slug's displacement with time.

4. Nomenclature

- $(C_p)_s$ = liquid sodium specific heat.
 D_{can} = wrapper can distance across inside flats.
 D_{eB} = equivalent hydraulic diameter for spherical bubble growth friction.
 D_{es} = equivalent hydraulic diameter for slug friction.
 f = friction factor.
 g = acceleration due to gravity.
 g_c = gravitational conversion factor.
 h = sodium film heat transfer coefficient.
 $\bar{h}_{fg}$ = average latent heat of vaporization for sodium.
 L = liquid slug length.
 m_{cond} = mass of condensed sodium.
 $\bar{m}_s$ = average mass of hot sodium.
 P_B = bubble pressure.
 P_W = fluid pressure at the wrapper can wall.
 P_L = plenum pressure.
 sQ_c = energy transferred from sodium to cladding.
 $\dot{q}$ = heat transfer rate from sodium to cladding.
 $R, \dot{R}, \ddot{R}$ = vapor bubble and slug interface displacement, velocity, and acceleration.
 r_W = fuel assembly wrapper can radius.
 T = temperature.
 t = time.
 V_B = vapor bubble volume.
 $V_{initial\ fluid}$ = initial fluid velocity.
 v = sodium vapor specific volume.
 $x, \dot{x}, \ddot{x}$ = slug displacement, velocity, and acceleration.
 ρ_l = sodium liquid density.

5. References — Appendix B

- ¹ G. H. Golden and J. V. Tokar, Thermophysical Properties of Sodium, ANL-7323, Argonne National Laboratory, May 1967.
- ² E. G. Schlechtendahl, "Theoretical Investigations on Sodium Boiling in Fast Reactors," Nuclear Science and Engineering, 41 (1970).
- ³ A. B. Reynolds, et al., Generation and Effects of Pressure Transients From Violent Boiling — First Progress Report, University of Virginia, Nov. 1, 1969.

Figure B-1. Thermal Model Schematic Diagram

Figure B-2. Two Configurations for Bubble Growth (or Collapse)

APPENDIX C
Phenomena of Violent Molten Fuel-
Sodium Interactions

1. Introduction

1.1. General

A literature survey of the data on explosive vapor formation was initiated under the Accident Analysis and Safety System Design Study to determine the significance of a local explosive vapor formation on the safety of a Liquid Metal Fast Breeder Reactor (LMFBR). This appendix presents the conclusions drawn from the literature on the possible mechanisms leading to and the magnitudes of pressures resulting from the violent interaction of molten fuel and sodium in the LMFBR.

If molten fast reactor fuel is dispersed into subcooled liquid sodium, locally high pressures can occur as a result of rapid energy transfer to the sodium. The high rates of energy transfer result from the large heat transfer surface area due to molten fuel fragmentation and from the large temperature difference between the fuel and the sodium. A steam explosion (which is not considered a chemical process) could have caused the destructive pressure pulse in the SPERT ID test because the destructive pressure pulse occurred after the power had returned to a normal level. Experimental results¹ and preliminary calculations² for the SPERT ID test indicate that the destructive pressure pulse was due to thermal and hydraulic effects.

An analysis of the effect of the thermal expansion of a thin film of water heated by conduction from fuel plates³ showed that, if the rate of temperature rise through the critical region (near the sodium critical temperature and pressure) is large, and if there is a large amount of surface area, then the heated water may produce a high rate of volume change and (consequently) a severe pressure transient. An inpile sodium loop experiment⁴ is expected to produce the rapid transfer of energy from molten, fragmented fuel to sodium, thereby producing sodium vapor at pressures and temperatures above the critical point.

Sodium autoclave experiments⁵ in the TREAT reactor have indicated that some characteristics of the test specimens, test apparatus, or test procedure prevented the violent sodium-molten fuel interaction. This absence of a vapor explosion may be due to the presence of fission gas, which might effectively prevent extensive fuel and sodium contact in some way.

1. 2. Chemical Reaction

In the experimental studies of the high-temperature interaction of fuel and cladding material with liquid sodium (conducted by Swift and Baker of Argonne National Laboratory⁸) no chemical reaction was evident. This was based on the findings showing that fragmented material displayed a shiny metallic appearance. In the SPERT-I destructive accident the maximum temperature reached was about 1200C; therefore, it appears that a chemical reaction could not have produced the 300 μ s rise time destructive pressure pulse.⁹

1. 3. Sodium Boiling and Superheating

Sodium can undergo considerably higher superheating in contact with small particles of molten fuel than in the boiling process. Highly superheated sodium is unstable and can flash to vapor to cause a large expansion. An abrupt volume change in sodium occurs upon boiling.

1. 4. Brief History of Molten Fuel-Sodium Interaction Incidents and Experiments

The major experiments covered in the literature survey are discussed briefly for completeness. An intentional transient was imposed on the BORAX-I reactor, resulting in a destructive pressure pulse as high as 6000 psi and probably higher than 10,000 psi.²²

Long¹¹ performed 880 tests, discharging 50 pounds of molten aluminum through various-sized holes into a steel container partially filled with water at 55 to 78F. Tests were conducted in which the metal temperature and the water height were changed. Tests were also performed with the sides and bottom of the container coated with grease or paint.

Karplus²⁵ verified the pressure amplification phenomenon experimentally by producing bubbles in a water-filled transparent shock tube with wire heaters. The pressure-time history in the boiling water was recorded with pressure gages on a cathode ray oscilloscope.

The SL-1 accident²³ produced a water-hammer pressure pulse of about 10,000 psi, which resulted from a 20-inch withdrawal of the central control rod.

An experiment at TRW (STL)⁹ modeled the heated surface-to-water volume ratio of the BORAX-I core with a one-dimensional water loading column. A heated stainless steel element (0.25 inch in diameter, 2 inches long, 0.002-inch wall) was mounted coaxially near the bottom of a vertical, water-filled pipe 3/8-inch in diameter by 46 inches long. A transient pressure resulted from a temperature rise of 700C in 150 microseconds produced by resistance heating. Experiments at TRW (STL)¹ modeled the SPERT ID condition to investigate rapid transient boiling. Two capsule experiments¹ with hydrostatic head above the reactor core were performed to investigate the production of high-pressure pulses in water by rapid thermal transients on a solid surface. A shock tube experiment²⁴ recorded (on an oscilloscope) the pressure history resulting from the water impact on the molten aluminum. The molten aluminum was held in a 1-inch-ID, zirconium-lined, molybdenum crucible.

SPERT ID²¹ was an open tank, aluminum plate, transient test reactor; it was fully enriched and was moderated and cooled with light water. A series of self-terminating power excursions with decreasing periods were performed; this resulted in a destructive transient. The sharp destructive pressure pulse (in the range of 3000 to 4000 psi), which destroyed the core and expelled an 80-foot-high plume of water, occurred 15 milliseconds after peak power of 2300 MW with an indicated period of 3.2 milliseconds.

Three different types of laboratory experiments conducted at Argonne National Laboratory have been used to study fragmentation transient heat transfer and the dispersal of molten metals into water and sodium. The first series of experiments was performed with a swinging arm connected to hot spheres moving through water and sodium to determine the transient heat transfer from small hot particles. The second series of experiments used small samples of metal, heated and dropped into liquid sodium and water in an argon glove box. The metal temperature, the sodium temperature, and the particular metal used were varied to observe the chemical and physical effects that would occur in a fast reactor meltdown accident. The third type of experiment was used to simulate the conditions of an excursion accident using electrical heaters of a TREAT-type piston autoclave.

Experimental studies at the University of Houston have studied the heat transfer and fragmentation process associated with a vapor explosion. Heat transfer rates from 0.5-inch-diameter spheres were measured as the spheres moved through a pool of sodium at atmospheric pressure and 572 and 842F.¹⁹ Various amounts of different metals were melted and dropped into a container of water and liquid nitrogen to study fragmentation.²⁷ Information on fragmentation at the onset of transition temperature was obtained by inserting a heated 0.5-inch-diameter silver sphere into a water-filled tube.²⁸

An experimental study of the metal/water thermal reaction was conducted at the University of Kansas with several metals heated above their melting points and dropped into water to cause fragmentation of the metals. The metal drops burst outward, bursting because of internal pressure.¹²

In experimental studies on molten UO_2 projected into sodium at 220 and 250C the French have studied the mechanisms for fragmentation and the resulting particle sizes of the fragmented UO_2 .^{13, 26}

Experimental studies of the effects of fission and/or fill gases within the fuel region have been conducted using a sodium autoclave in the TREAT reactor.⁵

An experimental investigation to analyze the pressure pulses produced in a narrow water column has been conducted at the University of Tokyo.²⁰ The temperature rate was changed from 3000 to 25,000 C/s on the heater surface located at the bottom of a vertical water column which had a free surface at the top.

2. Mechanisms for Fragmentation

2.1. General

Four possible mechanisms for fragmentation of molten fuel are the results of (1) trapping and superheating (without expansion) a small quantity of sodium between a molten glob of metal and a solid boundary, (2) the violent growth and collapse of sodium vapor bubbles in the transition and nucleate boiling regions around molten fuel, (3) trapped sodium inside molten metal with a solid shell formed on the outside of the molten fuel, and (4) dynamic mechanical interaction between the two fluids (shearing and impact forces, for example).

2.2. Trapping and Superheating

Trapping and superheating of a small quantity of sodium without expansion between a molten glob of metal and a solid boundary creates a preliminary trapping explosion that results in fragmentation of the molten metal. Long-term tests of the SPERT¹⁰ core and tests conducted by the Alcoa Research Laboratories¹¹ indicate that fuel dispersal was a result of a minor steam explosion arising from the entrapment and superheating of a small quantity of water by molten fuel. Coating the sides and bottom of a container with grease or paint prevented the thin layer of water from being trapped and thus eliminated the preliminary triggering explosion.¹¹

2.3. Violent Growth and Collapse of Sodium Vapor

The heat transfer from molten metal to sodium as the molten metal is quenched is initially by film boiling, changing to transition boiling, then to nucleate boiling when sodium is below its critical temperature, and then by convective heat transfer to the liquid.

Approximately 60 experiments with a heated silver sphere in a water-filled tube were conducted by Witte, Stephens, and Henningson;²⁸ the results showed that if the particles were molten at the point where violent oscillations in the liquid began, they would tend to fragment by the forces of vapor collapse.

The glove box experiments at ANL⁸ determined that if sodium is above its critical temperature, and if the molten metal solidifies before the sodium temperature is lowered to a violent boiling region, then fragmentation probably would not occur. Structural materials will fragment on cooling in molten sodium but will not fragment on cooling in water because solidification occurs in a nonviolent film boiling region, and the metals are fully solidified before reaching the temperature of violent boiling in water.

Temperature records in the SPERT ID core give strong evidence of vapor-blanket growth and collapse, and experiments at TRW (STL) under SPERT ID conditions¹ have shown that rapid transient boiling involves vapor-blanket growth with about 15 milliseconds before collapse.

2. 4. Sodium Trapped Inside a Shell of Molten Metal

A study of the metal/water reaction at the University of Kansas showed the existence of internal pressure in the molten metal with high-speed pictures.¹² The proposed mechanism of internal pressure assumes that the molten metal drop forms a solid shell because of rapid heat transfer when it contacts water. Some of the sodium liquid is trapped inside the shell. The trapped liquid is vaporized rapidly, creating a large internal pressure, which breaks the shell and disperses the molten metal. Similar results were found experimentally when the French²⁶ dropped 1 gram of UO_2 droplets at about 2900C into 10 grams of sodium at 250C. They observed that the fragmentation was due to bursting droplets of UO_2 surrounding a sodium core. The sodium-center UO_2 shell arrangement formed on contact between the UO_2 and the sodium.

The examination¹³ of molten UO_2 particles ejected into sodium under pressure at the Grenoble Research Center showed that the droplets deform into the shape of upside-down bells when they make contact with the liquid sodium, and the descent of the droplet causes entrapment of sodium. This condition may lead to the dispersion of molten UO_2 .

2. 5. Dynamic Forces Causing Fragmentation

Large impacting forces of molten fuel with sodium or with a solid object can lead to breakup of the molten fuel. Forces exerted by a steady flow of sodium liquid and vapor tend to subdivide the fuel into small particles as it melts. The more rapid the coolant flow the smaller the particles will be.⁶ The results of preliminary experiments indicate that the breakup of droplets into smaller drops is a function of the Weber number (ratio of external drag forces to surface tension forces) for liquid metal drops in water.¹⁴ Hesson and Ivins¹⁴ postulated that fragmentation may occur as a result of dynamic effects, since a liquid that is injected or falls into a second liquid or gas tends to form into droplets.

3. Factors Affecting Large Pressure Changes

3. 1. General

The generation of a large coolant vapor pressure by energy addition from fragmented molten fuel is a complex function of the various

thermal, physical, and dynamic properties of the sodium, the fuel, and the surrounding channel. Calculations have shown that the pressure peaks resulting from a molten fuel-sodium interaction are highly dependent on the following:¹⁵

1. Mass ratio of molten fuel to sodium.
2. Ratio of heat transfer area to pressure relief area (geometry of mixed fuel and sodium).
3. Fuel particle size distribution.
4. Nature of constraint.
5. Heat transfer rate from fuel to sodium.
6. Initial fuel and sodium temperatures.
7. Initial sodium void content.
8. Fuel and sodium thermophysical properties.
9. Particle movement and coolant flow rate.
10. Exterior pressure and the mass of surrounding sodium.
11. Rate of fuel dispersion.
12. Vapor blanketing.

3.2. Molten Fuel/Sodium Mass Ratio

A thin film of sodium around the molten fuel particles is heated rapidly. If the molten fuel/sodium mass ratio is large, then more of the bulk sodium is near the molten fuel particles, which raises the bulk sodium temperature rapidly and causes a rapid rise in pressure.

Long discharged 50 pounds of molten aluminum into water through various-sized holes at the Alcoa Research Laboratory; his results show that an explosion depends on the size of the molten aluminum stream entering the water.¹¹ For any given size tap hole, a certain amount of molten aluminum was required to produce an explosion, but additional amounts did not seem to increase its violence.

3.3. Ratio of Heat Transfer Area to Pressure Relief Area

It was determined experimentally by TRW (STL)⁹ that a higher ratio of heated surface area to pressure relief surface area will produce a higher pressure, but the compressibility of water reduces the pressure. This ratio was about 10 in SPERT I and about 18 in BORAX I on the assumption that pressure relief occurred radially outward in the reactors.

3.4. Fuel Particle Size Distribution

Molten fuel fragmentation increases the heat transfer surface area that can result in the generation of high pressure. The extent of fragmentation seems to increase with the temperature of the molten metal. More experimental data are needed for molten fuel ejected into sodium at or above the 1000-MW LMFBR operating temperature to determine the particle size distribution for accident conditions.

3.5. Nature of Constraint

The acoustic constraint remains for a time equal to that for sound waves to travel to an unheated free surface and return since the pressure is decreased by the returning pressure pulse. Pressure waves reflected from solid surfaces are compression waves that act as additional constraints.¹⁶

No pressures of destructive magnitude were observed in experiments at TRW (STL)¹ in which the hydrostatic head above the reactor core was modeled so that a free surface was available for pressure relief.

3.6. Heat Transfer Rate

The rate of heat transferred from molten fuel to sodium is increased by fragmentation because fragmentation results in a large increase in the heat transfer area. The controlling resistance to the flow of heat from large hot spheres to sodium is the conduction of heat from the interior of the sphere to its surface; the controlling resistance to the flow of heat from small hot spheres to sodium is the conduction of heat from the surface of the sphere across the surface film to the sodium.¹⁶

3.7. Initial Fuel and Sodium Temperature

It was determined experimentally at Argonne National Laboratory¹⁷ that solid materials were quenched by the sodium when dropped, but molten materials suffered some degree of fragmentation when dropped, depending on the initial temperature of the sample and the particular material used. The effect of higher sodium temperature is a more thorough dispersion in the case of molten stainless steel.

The results of the SPERT ID destructive test core¹ showed that a destructive pressure pulse occurred when about one-third of the core was molten, but a non-destructive pressure pulse occurred when there was only slight fuel plate melting.

Long's experiments with molten aluminum in water conducted at the Alcoa Research Laboratory¹¹ showed that the tendency toward explosions could be decreased by lowering the metal temperature or increasing the water depth.

3. 8. Initial Sodium Void or Non-Condensable Gas Content

An air bubble introduced into the 100-microsecond rise-time, water-filled, fixed-volume, capsule experiment at TRW (STL)¹ caused vapor-blanketing of the heated element after 150 microseconds; sustained pressurization of the capsule was not possible. This experiment was in analogy to the partially voided SPERT ID core.

An explanation for the small pressure pulse that resulted from flashing of superheated sodium in the sodium autoclave tests S-3 and S-4²⁹ is that small gas bubbles present in the tube caused rapid vapor-blanketing of the dispersed molten fuel and decreased the rapid transfer of heat to the sodium.

3. 9. Fuel and Sodium Thermophysical Properties

The degree of temperature rise of fuel above its melting point influences the extent of fragmentation; other physical properties, such as surface tension, viscosity, and thermal conductivity are likely to be important factors in the extent of particle fragmentation.¹⁸ An explanation for molten metal fragmentation was developed in which primary attention is given to the fluid's boiling characteristics. The differences in quenching molten metal in sodium and water must be due to differences in the physical properties of liquid sodium and water.¹⁷

Witte and Cox dropped molten metals into water and liquid nitrogen at the University of Houston.²⁷ Their preliminary results indicate that fragmentation is strongly dependent on the cooling characteristics of the quenching liquid.

3.10. Particle Movement and Coolant Flow Rate

It was shown experimentally,¹⁹ by moving hot 1/2-inch-diameter spheres through a pool of subcooled liquid sodium, that large amounts of subcooling in the liquid sodium and the motion of the sphere tends to prevent the formation of vapor at the surface of high-temperature spheres. The movement of molten fuel particles allows the heat to be removed from the sphere more quickly, which would tend to increase the pressure; however, the movement of the particles also increases the amount of sodium the particles contact, which would tend to decrease the pressure.

3.11. Exterior Pressure and Mass of Surrounding Sodium Displaced

A very rapid volume expansion is required to produce a high-pressure pulse. Expansion and heating of the coolant are competing mechanisms, since expanding the coolant reduces the pressure and heating the coolant increases the pressure. There is experimental evidence that violent boiling of sodium could produce a vapor explosion that would rapidly expel the remaining coolant from the core.⁵

Rapidly heating a vertical water column at the bottom with a free surface at the top produced two pressure patterns:²⁰ an initial increase to a peak pressure that is influenced by the heat rate and then a series of gradually damping pressure pulses. The maximum pressure in the second stage is greater than the peak pressure in the first stage. The pressure changes in this experiment are similar to the pressure changes observed in the BORAX and SPERT experiments.

3.12. Rate of Fuel Dispersion

The rate and manner of dispersing molten fuel into the coolant determine the pressure generated. Larger pressures are generated by faster dispersion rates. Fuel dispersal can greatly increase the void volume and the pressure.⁹ The dispersal of small molten fuel particles creates a sudden transfer of heat, and the rapid movement prevents vapor formation, which can lead to a serious steam explosion.

3.13. Vapor-Blanketing

The results of experiments using a sodium autoclave in the TREAT reactor have indicated that the fission and/or fill gas contained within the fuel region may blanket the molten fuel particles from the sodium and prevent the rapid transfer of heat energy.⁵ Gas vapor-blanketing of molten fuel expelled into sodium may prevent a violent molten fuel-sodium interaction.

A vapor-blanket effect appeared to impede heat transfer after the water reached 300C and to limit the pressure in fixed-volume capsule experiments conducted at TRW (STL)¹ to about half the critical pressure of water, even though thermal transients in the solid surfaces were well above the 374C critical water temperature.

In capsule runs placed in the reflector of the KEWB reactor, the surface of the aluminum disc did not melt, and most of the heat had not been transferred to the water when vapor-blanketing terminated the initial pressure pulse and the transfer of heat.¹

4. Sequence of Events From Molten Fuel-Sodium Contact Through Vapor Explosion

High-speed motion pictures of the sequence of events during the quenching and fragmentation of stainless steel and sodium were made by Baker and Swift at Argonne National Laboratory.⁸ The sequence of events showed that the parabolic depression in the sodium deepens over a period of 5 milliseconds after a molten stainless steel pellet enters the liquid sodium. A sodium vapor cloud forms and presumably results in fragmentation. The small molten particles transfer heat rapidly to a small localized portion of the sodium. A resulting vapor expansion causes ejection of some of the sodium and some of the hot steel particles, beginning at about 5.5 milliseconds. The pressure, peaking at about 7 milliseconds, has completely subsided at about 12 milliseconds. A transducer external to the SPERT ID core recorded a maximum pressure pulse of 2800 psig with a 150-microsecond rise time followed by a series of pulses lasting for 25 milliseconds.²¹

In the experiment conducted by TRW (STL)⁹ that modeled the BORAX I core with linear constraint, the void volume increases after vapor-blanketing, which returns the sodium liquid in contact with the heated surface.

A damped series of such impact pressure pulses with intervening vacuum occurs. The impact pulse width was determined to be 1.5 milliseconds in a particular experiment, which is the round-trip acoustic propagation time in the water column.

5. Pressure Magnitude

The pressure pulse was between 3000 and 4000 psi in SPERT I and as high as 6000 psi (and probably higher than 10,000 psi) in BORAX I.^{22, 10} There is no direct evidence for a comparable pressure pulse in the SL-I reactor, but a water-hammer pressure pulse of about 10,000 psi did occur.²³ These pressures could not be produced in the presence of water vapor and liquid since the pressures were larger than the critical pressure of water (3190 psig).

A shock tube experiment²⁴ has produced rapid pulses sufficiently large enough to account for the SPERT ID destructive pressure. In the experiments, water was dropped on a pool of molten aluminum, and a steam explosion occurred.

Karplus has given the theory and experimental verification of the pressure amplification phenomena from an external pressure in a vapor-liquid mixture.²⁵ This pressure amplification phenomenon is believed to have played a major role in the formation of the delayed destructive pressure in the SPERT ID core.¹ Pressure amplification is apparently a result of the water-hammer effect associated with bubble collapse. In the SPERT accident, the bubble collapse occurred in steam voids in the coolant outside of the core; the driving pressure for collapse of these voids was supplied by an expanding vapor bubble within the core.

6. Conclusions

This literature survey and evaluation concludes that large sodium vapor explosions are a result of molten metal fragmentation. Fragmentation increases the heat transfer area of the molten metal to the pressure relief surface area. To produce pressures in excess of the critical pressure of sodium, a thin film of sodium is heated rapidly by conduction without a vapor blanket forming before the sodium is superheated. The time from contact of molten metal with sodium until the vapor explosion has completely subsided is very short.

Fragmentation of molten metal occurs (1) when small quantities of sodium are trapped and superheated between surfaces of molten metal and a solid object, (2) when molten metal contacts sodium at the beginning of the violent growth and collapse of sodium vapor bubbles in the transition and nucleate boiling regions, (3) when sodium is trapped inside molten metal with a solid shell outside, (4) or by large dynamic mechanical forces between molten metal and flowing or impacting sodium.

Large pressure changes result from (1) a high ratio of heated surface area to pressure relief surface area, (2) a high mass ratio of molten fuel to sodium, (3) a high heat transfer rate (which is a result of small particle size distribution and large initial temperature difference between molten metal and sodium), (4) dispersion of molten metal at a rapid rate, (5) and large exterior pressures (which keep liquid in contact with the molten metal longer). Large pressure changes can be eliminated, even with the favorable conditions given above, if there is an initial sizable void near the heated surface and if vapor blanketing of the molten metal occurs without collapse before large pressures occur.

7. References — Appendix C

- ¹ Kinetic Studies of Heterogeneous Water Reactors, 1965 Summary Report, STL-372-30, TRW/Space Technology Laboratory, Dec. 1965.
- ² Chemical Engineering Division Semiannual Report, January - June 1964, ANL-6900, Argonne National Laboratory, pp 279, 280.
- ³ A Mechanism for Severe Pressure Transients in Heterogeneous Reactor Power Excursions, STL-6212, Part V, TRW/Space Technology Laboratory (1962).
- ⁴ Schumar, Barts, et al., Fuel Element Failure Propagation Program Plan, ANL/MET-01, Argonne, Aug. 1969.
- ⁵ C. P. Cabell, Monthly Informal Technical Progress Report, BNWL-1217, Battelle, Sept. 1969, pp 10.1 - 10.7.

- ⁶ A. Stothoplos, A. Goldman, and J. Herbsf, Conditions Leading to Fuel Melting and Reactor Scram, NTC 1016-7, Part 2, Docket 50016-18, July 1969.
- ⁷ Project Report, Studies of Safety Test Facilities, PA-10, Argonne.
- ⁸ D. L. Swift and L. Baker, Jr., "Experimental Studies of the High-Temperature Interaction of Fuel and Cladding Materials With Liquid Sodium," Conference on Safety, Fuels, and Core Design in Large Fast Power Reactors, ANL-7120, Argonne, Oct. 11 - 14, 1965, pp 839 - 847.
- ⁹ Kinetic Studies of Heterogeneous Water Reactors, Quarterly Progress Report, STL-6306, TRW/Space Technology Laboratory, July 1963, pp 1 - 17.
- ¹⁰ R. W. Miller, et al., "Experimental Results and Damage Effects of Destructive Tests," ANS Transactions, 6, No. 2 (1963) pp 138, 139.
- ¹¹ G. Long, "Explosions of Molten Aluminum in Water — Cause and Prevention," Metal Progress, May 1957, pp 107 - 112.
- ¹² F. E. Brauer, N. W. Green, and R. B. Mesler, "Metal/Water Explosions," Nuclear Science & Engineering, 31, No. 3, Mar. 1968, pp 551 - 554.
- ¹³ S. J. Stachura, Fuel-Coolant Interactions, AEC-Euratom Fast Reactor Exchange Program, Report No. 17 for March - April 1969, pp 20 - 27.
- ¹⁴ J. C. Hesson and R. O. Ivins, Interaction of Fuel, Cladding, and Coolant — Theoretical Studies, ANL-7425, Argonne, July 1967, p 160.
- ¹⁵ 1000-MWe LMFBR Accident Analysis and Safety System Design Study, Quarterly Technical Progress Report for October - December 1969, Babcock & Wilcox, pp 3 - 8.
- ¹⁶ D. R. Armstrong and J. C. Hesson, Fuel-Sodium Interaction and Heat Transfer Leading to Pressure Generation, Reactor

- Development Program Progress Report, ANL-7595, Argonne, July 1969, pp 122 - 127.
- ¹⁷ D. Swift and J. Paulik, Fuel-Coolant and Cladding-Coolant Interaction Studies, Chemical Engineering Division Semiannual Report for July - December 1965, ANL-7125, Argonne, pp 187 - 193.
 - ¹⁸ D. Swift and P. Krause, Fuel-Coolant and Cladding-Coolant Interaction Studies, Chemical Engineering Division Semiannual Report for January - June 1965, ANL-7055, Argonne, pp 199 - 208.
 - ¹⁹ L. C. Witte, L. Baker, Jr., and D. R. Haworth, "Heat Transfer From Spheres Into Subcooled Liquid Sodium During Forced Convection," Journal of Heat Transfer, Trans. ASME, Nov. 1968, pp 394 - 398.
 - ²⁰ H. Uchida and T. Yamura, "Analysis of Pressure Pulses Produced in a Water Channel by Rapid Heating," Nuclear Safety, 11, No. 1, January - February 1970, pp 12 - 19.
 - ²¹ R. W. Miller, et al., Report on the SPERT I Destructive Test Program on an Aluminum Plate-Type, Water Moderated Reactor, IDO-16883, June 1964.
 - ²² J. R. Dietrich, Experimental Investigation of the Self-Limitation of Power During Reactor Transients in a Subcooled Water-Moderated Reactor — BORAX-I Experiments — 1954, AECD-3668 (1955).
 - ²³ J. F. Kunze, Full Report of SL-1 Recovery Operation for May 1961 - July 1962, IDO-19311 (1962).
 - ²⁴ L. G. Neal, L. B. Wentz, and R. W. Wright, Kinetic Studies of Heterogeneous Water Reactors, Quarterly Progress Report for April - June 1965, STL-372-22, TRW/Space Technology Laboratory, July 1965.
 - ²⁵ H. B. Karplus, Propagation of Pressure Waves in a Mixture of Water and Steam, ARF 4132-12, Jan. 1961.
 - ²⁶ AEC-Euratom Fast Reactor Exchange Program, 4th Quarterly Report, "Fast Neutrons Project," EURFNR-658 (1968), pp 33, 34.

- ²⁷ L. C. Witte and J. E. Cox, The Vapor Explosion — Heat Transfer and Fragmentation, Technical Progress Report, ORO-3936-2, Jan. 1970.
- ²⁸ L. C. Witte, J. W. Stevens, and P. J. Henningson, "The Effect of Subcooling on the Onset of Transition Boiling," ANS Transactions, 12, No. 2, Dec. 1 - 4, 1969, p 806.
- ²⁹ J. J. Barghusen, In-Pile Experiments With UO_2 in Piston Autoclave, Reactor Development Program Progress Report, ANL-7632, Argonne, Oct. 1969, pp 141, 142.

APPENDIX D
Equations for Multiple-Assembly
Response Model

MIMIC SOURCE-LANGUAGE PROGRAM
 VERSION 1 10/01/68 MOD LEVEL 0000

DELETE

CON(F01B,F13B,F1B,F12B,F2B,F26B)
 CON(F3B,F35B,F36B,F63,F64B,F68B)
 CON(F5B,F57B,F58B,F43,F7B,F8B)
 CON(AM)
 CON(DKDX,VOL)
 CON(M0,M1,M2,M3,M4,M5)
 CON(M6,M7,M8)
 CON(K1,K2,K3,K4,K5,K6)
 CON(K7,K8)
 CON(K01,K13,K12,K26,K35,K36)
 CON(K64,K68,K57,K58)
 CON(DX01M,DX13M,DZ12M,DX26M,DX35M,DZ36M)
 CON(DZ64M,DX68M,DX57M,DZ58M)
 CON(K01M,K13M,K12M,K26M,K35M,K36M)
 CON(K64M,K68M,K57M,K58M)
 CON(BETA,THETA)
 CON(AG,AB,AC,AD,AE,AJ)
 CON(AK,AH)
 CON(L01,L13,L12,L26,L35,L36)
 CON(L57,L58,L64,L68)
 CON(DX01Y,DX13Y,DZ12Y,DX26Y,DX35Y,DZ36Y)
 CON(DZ64Y,DX68Y,DX57Y,DZ58Y)
 CON(DX1Y,DA2Y,DX3Y,DA4Y,DX5Y,DA6Y)
 CON(DX7Y,DA8Y)
 CON(DX01R,DX13R,DZ12R,DX26R,DX35R,DZ36R)
 CON(DZ64R,DX68R,DX57R,DZ58R)
 CON(DX1R,DA2R,DX3R,DA4R,DX5R,DA6R)
 CON(DX7R,DA8R)
 CON(OT,TFIN,DTMIN)
 CON(G)
 CON(OTF,OTF1,OTF3,OTF5)
 CON(F0,F0A)

C K=STIFFNESS COEFFICIENT (LBS/FT) .
 C KT=BENDING K OF T .
 C KST=CRUSHING K BETWEEN S AND T -PER IN. OF CAN LENGTH .
 C KSTM=CRUSHING K BETWEEN S AND T -COMPACT MASS .
 C MT=MASS AT T . (SLUGS)
 C DTXT=VELOCITY OF T IN X DIRECTION (FT./SEC) .
 C DXT=DISPLACEMENT OF T IN X DIRECTION (FT) .
 C DXTSY=DISPLACEMENT BETWEEN T AND S IN X DIRECTION TO REACH Y(FT)
 C DXSY=DISPLACEMENT OF S IN X DIRECTION TO REACH Y (FT) .
 C F=INPUT FORCE (LBS) .
 C TDF=APPLICATION TIME OF F (SEC) .
 C FT=FORCE AT T (LBS) .
 C FTS=FORCE BETWEEN T AND S (LBS) .
 C FTSF=FORCE BETWEEN S AND T DUE TO FLUID MOTION . (LBS) .
 C T=0,1,2,... =POSITION IDENTIFICATION .
 C S=0,1,2,... =POSITION IDENTIFICATION .
 C LTS=LENGTH BEING CRUSHED
 C X=X,A,Z = DIRECTION .
 C G=GAP BETWEEN CANS (FT)
 C THETA=ANGLE BETWEEN X AND A OR BETWEEN A AND Z (RAD) .
 C BETA =ANGLE BETWEEN X AND Z (RAD) .
 C Y=Y,R,M .
 C Y=DISPLACEMENT TO ATTAIN YIELD STRESS .
 C R=RUPTURE DISPLACEMENT .
 C M=DISPLACEMENT TO CAUSE CONTACT OF MASSES .

C	AH	INITIAL AREA 0.041 SQ.FT
C	AG	ASSEMBLY LENGTH 8.33 FT
C	AB	COEFFICIENT
C	AC	ASSEMBLY WIDTH ACROSS FLATS 0.538 FT
C	AD	SODIUM DENSITY 50.45 LBS/CU.FT
C	AE	GRAVITATIONAL CONSTANT 32.2
C	AJ	FRICTION FACTOR 0.04
C	AK	EQUIVILENT DIA. 0.013 FT
	DTMAX	LSW(T-DTF1,.00001,.001)
	F	LSW(T-DTF,F0*T/DTF,FA)
	FA	LSW(T-DTF1,(DTF1-T)*F0/(DTF1-DTF),FB)
	FB	LSW(T-DTF3,(T-DTF1)*F0A/(DTF3-DTF1),FC)
	FC	LSW(T-DTF5,(DTF5-T)*F0A/(DTF5-DTF3),0.)
	IMPLS	INT(F,0.)
	NRG	INT(F*DTX0*6.,0.)
	A0X	(F-F01+B01*NP+C01)/(M0+B01*(1.-N2P))
	DTX0	LIN(INT(A0X,0.),DX0,-.25,1.)
	DX0	INT(DTX0,0.)
	A1X	NP+N2P*A0X
	DTX1	INT(A1X,0.)
	DX1	INT(DTX1,0.)
	CX1	ABS(DX1)
	A2A	LP+L2P*A1X
	DTA2	INT(A2A,0.)
	DA2	INT(DTA2,0.)
	CA2	ABS(DA2)
	A3X	K3P+KP*A2A+K2P*A1X
	DTX3	INT(A3X,0.)
	DX3	INT(DTX3,0.)
	CX3	ABS(DX3)
	A6A	H3P+HP*A3X+H2P*A2A
	DTA6	INT(A6A,0.)
	DA6	INT(DTA6,0.)
	CA6	ABS(DA6)
	A4A	CP+C2P*A6A
	JTA4	INT(A4A,0.)
	DA4	INT(DTA4,0.)
	CA4	ABS(DA4)
	A5X	I3P+IP*A2A+I2P*A3X
	DTX5	INT(A5X,0.)
	DX5	INT(DTX5,0.)
	CX5	ABS(DX5)
	A7X	BP+B2P*A5X
	JTX7	INT(A7X,0.)
	DX7	INT(DTX7,0.)
	CX7	ABS(DX7)
	A8A	AP+A2P*A5X+A3P*A6A
	DTA8	INT(A8A,0.)
	JA8	INT(DTA8,0.)
	CA8	ABS(DA8)
	A3A	PWR(L01,3.)*DV01*ABS(DV01)*A3
	N3P	M1+B01+B13+2.*CB*B12*(CB-L2P*CT)-B13*(KP*L2P+K2P)
	N2P	B01/N3P
	N4P	2.*CB*(F12-B12*LP*CT-C12)+F1
	NP	(F01-C01-F13+C13+B13*(K3P+KP*LP)-N4P)/N3P
	D3A	PWR(L12,3.)*DV12*ABS(DV12)*D3
	L3P	M2+(B12+B26*(1.-KP*HP-H2P))*CT*CT
	L2P	(B12*CB+B26*K2P*HP*CT)*CT/L3P
	LP	((F12-C12-F26+C26+B26*(H3P+K3P*HP)*CT)*CT-F2)/L3P
	C2A	PWR(L13,3.)*DV13*ABS(DV13)*C2
	J2P	M3+B13+B35+2.*CB*B36*(CB-HP*CT)-B35*I2P
	JP	F13-C13-F35+C35-2.*C3*(F36-C36-B36*H3P*CT)-F3+B35*I3P
	K2P	B13/J2P
	KP	(B35*IP+2.*B36*CB*H2P*CT)/J2P
	K3P	JP/J2P
	E7A	PWR(L26,3.)*DV26*ABS(DV26)*E7

I6A PWR(L36,3.)*DV36*ABS(DV36)*I6
 O4P Mo+(B36+B26+B64+B68)*CT*CT
 J3P B26*CT*CT
 O2P B36*CT*CB
 UP (F36-C36+F26-C26-F64+C64-F68+C68)*CT-F6
 E5P O4P-B64*CT+C2P*CT-B68*CT*A3P*CT
 E4P B68*CT*A2P/E5P*CT
 E3P O3P/E5P
 E2P O2P/E5P
 EP (JP+B64*CT*CP*CT+B68*CT*AP*CT)/E5P
 H4P 1.-E4P*G3P
 H2P E3P/H4P
 HP (E2P+E4P*G2P)/H4P
 H3P (EP+E4P*GP)/H4P
 H5A PWR(L35,3.)*DV35*ABS(DV35)*H5
 G4P M5+B35+B57+2.*CB*B58*(CB-A2P*CT)-B57*B2P
 G3P 2.*B58*A3P*CT/G4P*CB
 G2P B35/G4P
 GP (F35-C35-F57+B57*BP+C57-G5P)/G4P
 G5P 2.*CB*(F58-B58*AP*CT-C58)+F5
 I2P G2P+G3P*HP
 IP G3P*H2P
 I3P GP+G3P*H3P
 NA PWR(L64,3.)*DV64*ABS(DV64)*N
 C3P M4+B64*CT*CT
 C2P B64*CT/C3P*CT
 CP ((F64-C64)*CT-F4-F413F)/C3P
 P7A PWR(L57,3.)*DV57*ABS(DV57)*P7
 B3P M7+B57
 B2P B57/B3P
 BP (F57-C57-F7-F79F)/B3P
 R8A PWR(L68,3.)*DV68*ABS(DV68)*R8
 J8A PWR(L58,3.)*DV58*ABS(DV58)*Q8
 A4P M8+(B58+B68)*CT*CT
 A3P B68*CT/A4P*CT
 A2P B58*CT/A4P*CB
 AP ((F58-C58+F68-C68)*CT-F8-F810F)/A4P
 B01 AA*(L01/(2.*A01)+(AG-L01)/AH)*L01*L01*A3
 AA A3/12.*AC*AC*AD/AE
 A01 AH-AC/SQR(3.)*DX01
 AH2 AH*AH
 C01 AI*((1.+AJ*L01/AK)/(A01*A01)+AJ/AK*(AG-L01)/AH2)*A3A
 AI AA/(2.*SQR(3.))*AC
 DV01 DTX1-DTX0
 UX01 DX0-DX1
 CX01 ABS(DX01)
 A3 FSW(A8,1.,1.,0.)
 A8 INT(A9,0.)
 A9 FSW(DX01R-CX01,1.,0.,0.)
 B13 AA*(L13/(2.*A13)+(AG-L13)/AH)*L13*L13*C2
 A13 AH-AC/SQR(3.)*(DX13-G)/2.
 DV13 DTX3-DTX1
 DX13 JX1-DX3
 C13 AI*((1.+AJ*L13/AK)/(A13*A13)+AJ/AK*(AG-L13)/AH2)*C2A
 B12 AA*(L12/(2.*A12)+(AG-L12)/AH)*L12*L12*03
 DV12 DTA2*CT-DTX1*CB
 DZ12 DZ1-DZ2
 C12 AI*((1.+AJ*L12/AK)/(A12*A12)+AJ/AK*(AG-L12)/AH2)*D3A
 A12 AH-AC/SQR(3.)*(DZ12-G)/2.
 3 FSW(B5,1.,1.,0.)
 B5 INT(B6,0.)
 B6 FSW(DX1R-CX1,1.,0.,0.)
 C2 FSW(C6,1.,1.,0.)
 C6 INT(C7,0.)
 C7 FSW(DX13R+G-ABS(DX1-DX3),1.,0.,0.)
 D3 FSW(D7,1.,1.,0.)

D7 INT(D8,0.)
 D8 FSW(DZ12R+G-ABS(DZ1-DZ2),1.,0.,0.)
 B26 $AA*(L26/(2.*A26)+(AG-L26)/AH)*L26*L26*E7$
 A26 $AH-AC/SQR(3.)*(DX26-G)/2.$
 C26 $AI*((1.+AJ*L26/AK)/(A26*A26)+AJ/AK*(AG-L26)/AH2)*E7A$
 JV26 (DTA6-DTA2)*CT
 DX26 DX2-DX6
 D9 FSW(E3,1.,1.,0.)
 E3 INT(E4,0.)
 E4 FSW(DA2R-CA2,1.,0.,0.)
 E7 FSW(G5,1.,1.,0.)
 G5 INT(G6,0.)
 G6 FSW(DX26R+G-ABS(DX2-DX6),1.,0.,0.)
 B35 $AA*(L35/(2.*A35)+(AG-L35)/AH)*L35*L35*H5$
 A35 $AH-AC/SQR(3.)*(DX35-G)/2.$
 C35 $AI*((1.+AJ*L35/AK)/(A35*A35)+AJ/AK*(AG-L35)/AH2)*H5A$
 DV35 DTX5-DTX3
 DX35 DX3-DX5
 B36 $AA*(L36/(2.*A36)+(AG-L36)/AH)*L36*L36*I6$
 A36 $AH-AC/SQR(3.)*(DX36-G)/2.$
 C36 $AI*((1.+AJ*L36/AK)/(A36*A36)+AJ/AK*(AG-L36)/AH2)*I6A$
 DV36 DTA6*CT-DTX3*CB
 DZ36 DZ3-DZ6
 G7 FSW(H1,1.,1.,0.)
 H1 INT(H2,0.)
 H2 FSW(DX3R-CX3,1.,0.,0.)
 H5 FSW(I2,1.,1.,0.)
 I2 INT(I3,0.)
 I3 FSW(DX35R+G-ABS(DX3-DX5),1.,0.,0.)
 I6 FSW(J3,1.,1.,0.)
 J3 INT(J4,0.)
 J4 FSW(DZ36R+G-ABS(DZ3-DZ6),1.,0.,0.)
 B57 $AA*(L57/(2.*A57)+(AG-L57)/AH)*L57*L57*P7$
 A57 $AH-AC/SQR(3.)*(DX57-G)/2.$
 C57 $AI*((1.+AJ*L57/AK)/(A57*A57)+AJ/AK*(AG-L57)/AH2)*P7A$
 DV57 DTX7-DTX5
 DX57 DX5-DX7
 B58 $AA*(L58/(2.*A58)+(AG-L58)/AH)*L58*L58*Q8$
 A58 $AH-AC/SQR(3.)*(DX58-G)/2.$
 C58 $AI*((1.+AJ*L58/AK)/(A58*A58)+AJ/AK*(AG-L58)/AH2)*Q8A$
 DV58 DTA8*CT-DTX5*CB
 DZ58 DZ5-DZ8
 J5 FSW(L,1.,1.,0.)
 L INT(L0,0.)
 L0 FSW(DX5R-CX5,1.,0.,0.)
 P7 FSW(Q4,1.,1.,0.)
 Q4 INT(Q5,0.)
 Q5 FSW(DX57R+G-ABS(DX5-DX7),1.,0.,0.)
 Q8 FSW(R4,1.,1.,0.)
 R4 INT(R5,0.)
 R5 FSW(DZ58R+G-ABS(DZ5-DZ8),1.,0.,0.)
 B64 $AA*(L64/(2.*A64)+(AG-L64)/AH)*L64*L64*N$
 A64 $AH-AC/SQR(3.)*(DX64-G)/2.$
 C64 $AI*((1.+AJ*L64/AK)/(A64*A64)+AJ/AK*(AG-L64)/AH2)*NA$
 JV64 (DTA4-DTA6)*CT
 DZ64 DZ6-DZ4
 B68 $AA*(L68/(2.*A68)+(AG-L68)/AH)*L68*L68*R8$
 A68 $AH-AC/SQR(3.)*(DX68-G)/2.$
 C68 $AI*((1.+AJ*L68/AK)/(A68*A68)+AJ/AK*(AG-L68)/AH2)*R8A$
 DV68 (DTA8-DTA6)*CT
 DX68 DX6-DX8
 L1 FSW(L6,1.,1.,0.)
 L6 INT(L7,0.)
 L7 FSW(DA6R-CA6,1.,0.,0.)
 N FSW(N7,1.,1.,0.)
 N7 INT(N8,0.)

	N8	FSW(DZ64R+G-ABS(DZ6-DZ4),1.,0.,0.)
	R8	FSW(S4,1.,1.,0.)
	S4	INT(S5,0.)
	S5	FSW(DX68R+G-ABS(DX6-DX8),1.,0.,0.)
	F410F	L64*UTA4*ABS(UTA4)*AL
	AL	AM*AC/AE*AD/2.
	N9	FSW(F3,1.,1.,0.)
	P3	INT(P4,0.)
	P4	FSW(DA4R-CA4,1.,0.,0.)
	F79F	L57*DTX7*ABS(DTX7)*AL
	S6	FSW(U,1.,1.,0.)
	U	INT(U6,0.)
	U0	FSW(DX7R-CX7,1.,0.,0.)
	F810F	L68*UTA8*ABS(UTA8)*AL
	U1	FSW(U5,1.,1.,0.)
	U5	INT(U6,0.)
	U6	FSW(DA8R-CA8,1.,0.,0.)
	F01	FSW(DX01M-DX01,F01D,0.,F01C)
	F13	FSW(DX13M-DX13,F13D,0.,F13C)
	F12	FSW(DZ12M-DZ12,F12D,0.,F12C)
	F26	FSW(DX26M-DX26,F26D,0.,F26C)
	F35	FSW(DX35M-DX35,F35D,0.,F35C)
	F36	FSW(DZ36M-DZ36,F36D,0.,F36C)
	F64	FSW(DZ64M-DZ64,F64D,0.,F64C)
	F63	FSW(DX63M-DX63,F63D,0.,F63C)
	F57	FSW(DX57M-DX57,F57D,0.,F57C)
	F58	FSW(DZ58M-DZ58,F58D,0.,F58C)
	F01C	K01*(DX01-F01A)*A3*L01*12.
	F01D	K01M*(DX01-DX01M)*L01*12.
A2	F01A	LSW(DX01Y-DX01,DX01-DX01Y,0.)
	A2	FSW(F11B-DX01,A4,A4,FALSE)
	A4	FSW(DV01,TRUE,TRUE,FALSE)
A2	F013	DX01
	F13C	FSW(DX13-G-F13A,0.,0.,B7)
	B7	K13*(DX13-G-F13A)*C2*L13*12.
	F13D	K13M*(DX13-DX13M)*L13*12.
B2	F13A	LSW(DX13Y+G-DX13,DX13-DX13Y-G,0.)
	B2	FSW(F13B-DX13,B3,B3,FALSE)
	B3	FSW(DV13,TRUE,TRUE,FALSE)
B2	F13B	DX13
	F1	K1*(DX1-F1A)*B
30	F1A	LSW(DX1Y-DX1,DX1-DX1Y,0.)
	B1	FSW(F1B-DX1,B1,B1,FALSE)
	B1	FSW(DTX1,FALSE,TRUE,TRUE)
30	F1B	DX1
	F12C	FSW(DZ12-G-F12A,0.,0.,C8)
	C8	K12*(DZ12-G-F12A)*D3*L12*12.
	F12D	K12M*(DZ12-DZ12M)*L12*12.
C0	F12A	LSW(DZ12Y+G-DZ12,DZ12-DZ12Y-G,0.)
	C0	FSW(F12B-DZ12,C1,C1,FALSE)
	C1	FSW(DV12,TRUE,TRUE,FALSE)
C0	F12B	DZ12
	F2	K2*(DA2-F2A)*D9
C5	F2A	LSW(DA2Y-DA2,DA2-DA2Y,0.)
	C5	FSW(F2B-DA2,C9,C9,FALSE)
	C9	FSW(DTA2,FALSE,TRUE,TRUE)
C5	F2B	DA2
	F26C	FSW(DX26-G-F26A,0.,0.,D)
	D	K26*(DX26-G-F26A)*E7*L26*12.
	F26D	K26M*(DX26-DX26M)*L26*12.
U2	F26A	LSW(DX26Y+G-DX26,DX26-DX26Y-G,0.)
	D2	FSW(F26B-DX26,D4,D4,FALSE)
	U4	FSW(DV26,TRUE,TRUE,FALSE)
U2	F26B	DX26
	F3	K3*(DX3-F3A)*G7
C	F3A	LSW(DX3Y-DX3,DX3-DX3Y,0.)

	E	FSW(F38-DX3,E0,E0,FALSE)
	E0	FSW(OTX3,FALSE,TRUE,TRUE)
E	F38	DX3
	F35C	FSW(DX35-G-F35A,0.,0.,E1)
	E1	K35*(DX35-G-F35A)*H5*L35*12.
	F35D	K35M*(DX35-DX35M)*L35*12.
E5	F35A	LSW(DX35Y+G-DX35,DX35-DX35Y-G,0.)
	E5	FSW(F35B-DX35,E6,E6,FALSE)
	E6	FSW(DV35,TRUE,TRUE,FALSE)
E5	F35B	DX35
	F36C	FSW(DZ36-G-F36A,0.,0.,G0)
	G0	K36*(DZ36-G-F36A)*I6*L36*12.
	F36D	K36M*(DZ36-DZ36M)*L36*12.
G2	F36A	LSW(DZ36Y+G-DZ36,DZ36-DZ36Y-G,0.)
	G2	FSW(F36B-DZ36,G3,G3,FALSE)
	G3	FSW(DV36,TRUE,TRUE,FALSE)
G2	F36B	DZ36
	F6	K6*(DA6-F6A)*L1
I9	F6A	LSW(DA6Y-DA6,DA6-DA6Y,0.)
	I9	FSW(F6B-DA6,J,J,FALSE)
	J	FSW(OTA6,FALSE,TRUE,TRUE)
I9	F6B	DA6
	F64C	FSW(DZ64-G-F64A,0.,0.,J0)
	J0	K64*(DZ64-G-F64A)*N*L64*12.
	F64D	K64M*(DZ64-DZ64M)*L64*12.
J2	F64A	LSW(DZ64Y+G-DZ64,DZ64-DZ64Y-G,0.)
	J2	FSW(F64B-DZ64,J6,J6,FALSE)
	J6	FSW(DV64,TRUE,TRUE,FALSE)
J2	F64B	DZ64
	F68C	FSW(DX68-G-F68A,0.,0.,J9)
	J9	K68*(DX68-G-F68A)*R8*L68*12.
	F68D	K68M*(DX68-DX68M)*L68*12.
L3	F68A	LSW(DX68Y+G-DX68,DX68-DX68Y-G,0.)
	L3	FSW(F68B-DX68,L4,L4,FALSE)
	L4	FSW(DV68,TRUE,TRUE,FALSE)
L3	F68B	DX68
	F5	K5*(DX5-F5A)*J5
H	F5A	LSW(DX5Y-DX5,DX5-DX5Y,0.)
	H	FSW(F5B-DX5,H3,H3,FALSE)
	H3	FSW(OTX5,FALSE,TRUE,TRUE)
H	F5B	DX5
	F57C	FSW(DX57-G-F57A,0.,0.,H4)
	H4	K57*(DX57-G-F57A)*P7*L57*12.
	F57D	K57M*(DX57-DX57M)*L57*12.
H7	F57A	LSW(DX57Y+G-DX57,DX57-DX57Y-G,0.)
	H7	FSW(F57B-DX57,H8,H8,FALSE)
	H8	FSW(DV57,TRUE,TRUE,FALSE)
H7	F57B	DX57
	F58C	FSW(DZ58-G-F58A,0.,0.,I0)
	I0	K58*(DZ58-G-F58A)*G8*L58*12.
	F58D	K58M*(DZ58-DZ58M)*L58*12.
I4	F58A	LSW(DZ58Y+G-DZ58,DZ58-DZ58Y-G,0.)
	I4	FSW(F58B-DZ58,I5,I5,FALSE)
	I5	FSW(DV58,TRUE,TRUE,FALSE)
I4	F58B	DZ58
	F4	K4*(DA4-F4A)*N9
L9	F4A	LSW(DA4Y-DA4,DA4-DA4Y,0.)
	L9	FSW(F4B-DA4,N0,N0,FALSE)
	N0	FSW(OTA4,FALSE,TRUE,TRUE)
L9	F4B	DA4
	F7	K7*(DX7-F7A)*S6
N1	F7A	LSW(DX7Y-DX7,DX7-DX7Y,0.)
	N1	FSW(F7B-DX7,N2,N2,FALSE)
	N2	FSW(OTX7,FALSE,TRUE,TRUE)
N1	F7B	DX7
	F8	K8*(DA8-F8A)*U1

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N3      F8A      LSW(DA8Y-DA8,DA8-DA8Y,0.)
N3      N3       FSW(F8B-DA8,N4,N4,FALSE)
N4      N4       FSW(OTA8,FALSE,TRUE,TRUE)
N3      F8B      DA8
CB      CB       COS(BETA)
CT      CT       COS(THETA)
DZ1     DX1*CB
DZ2     DX2
DX2     DA2*CT
JX4     DA4*CT
DZ3     DX3*CB
DZ6     DX6
DX6     DA6*CT
DZ4     DX4
DA5     DX5*CT
DZ5     DX5*CB
DZ7     DX7*CB
DZ8     DX8
DX8     DA8*CT
DK1     (DX1+DX3+DX5+DX7)*(1.+2.*CB)
DK2     (DX2+DX4+DX6+DX8)*2.
DK      61.0*DKDX*(DK1+DK2)*VOL
        FIN(T,TFIN)
        OUT( )
        OUT(F1B,F2B,F3B,F4B,F5B,T)
        OUT(F6B,F7B,F8B,NRG,DK,IMPLS)
        OUT(F11B,F13B,F12B,F26B,F35B,F36B)
        OUT(F64B,F68B,F57B,F58B,DTX1,A0X)
        END

```

APPENDIX E
Fuel-Coolant Interaction Model

1. Introduction

The MFCDYN model consists of (1) a mixing region (length ℓ_1) where hot molten fuel is assumed to disperse instantaneously into small particles and transfers heat to sodium, and (2) an unheated column of sodium (length ℓ_2).

Two approximations are made in evaluating the time effects of sodium-molten fuel interactions. The first is the acoustic approximation, which is followed by the inertial approximation.

1.1. Acoustic Approximation

The expansion of the heated volume produces compression (acoustic) waves in the unheated coolant. As this wave travels upward, it compresses layers of unheated liquid sodium. The liquid is assumed to be compressible, and the compressibility effects are dominant up to the time the wave first reflects back from the nearest reflecting surface. Further reflections of the wave are neglected. Hence, the approximation is valid only for the period of time up to $2(\ell_1 + \ell_2)/c_\infty$ where c_∞ is the speed of sound in the unheated sodium. Nonlinear propagation velocity and convective motion are also neglected. The "acoustic-constraint-dominated" period of the sodium-molten fuel interaction is much shorter than the inertial-dominated effect.

1.2. Inertial Approximation

This is a long-term effect. The liquid surrounding the "heated zone," which is the liquid column of height ℓ_2 here, is assumed to be incompressible. The expansion of the heated volume is resisted by the friction and inertia of the surrounding coolant. This approximation is valid for times greater than $2(\ell_1 + \ell_2)/c_\infty$.

The inertial constraint period could be further subdivided into two regions—one where the liquid in the heated volume has partially

vaporized and the other when it has fully vaporized. It is possible for the fluid to remain in either of these regions for a considerable length of time, depending chiefly on the ratio of sodium and fuel mass.

2. Mathematical Formulations and Governing Equations

Consider a volume of molten fuel-coolant mixture of height l_1 . The molten fuel is assumed to disperse instantaneously into small particles that provide a large area for heat transfer. The high rate of heat transfer causes rapid heating of the coolant, and the pressure rises in the mixing zone. As the pressure rises, the mixing zone tends to expand against the external constraints. The pressure and expansion effects are determined by the interaction between two competing processes: the increase in pressure due to heating of sodium and the expansion of sodium, which reduces pressure.

It is assumed that the temperature of sodium in the mixing zone is uniform and that no heat is transferred across the mixing zone boundary. The pressure in the mixing zone is given by

$$P = P(V, T) \quad (\text{Equation of state})$$

or

$$dP = \left(\frac{\partial P}{\partial V} \right)_T dV + \left(\frac{\partial P}{\partial T} \right)_V dT.$$

The rate of pressure change is

$$\frac{dP}{dt} = -\frac{1}{\beta V} \frac{dV}{dt} + \frac{\alpha}{\beta} \frac{dT}{dt}, \quad (\text{E-1})$$

where β is the compressibility and α is the coefficient of thermal expansion.

Once dV/dt and dT/dt are determined, dP/dt is found from equation B-1, if α and β are known. In order to determine dV/dt and dT/dt , we must look at the energy and momentum equations for each of the constraint types mentioned before.

2. 1. Equations for Acoustic Constraint

It is assumed that the coolant column of length ℓ_2 above the mixing zone is compressible. From the theory of compressible flow, the momentum equation is as follows:

$$\begin{aligned} P(t) - P_{\infty} &= \rho_{\infty} c_{\infty} \frac{dZ(t)}{dt}; \quad Z(0) = \ell_1 \\ &= \frac{\rho_{\infty} c_{\infty}}{A} \frac{dV(t)}{dt} \end{aligned} \quad (E-2)$$

where

$P(t)$ = pressure in mixing zone,

P_{∞} = initial pressure throughout,

$\rho_{\infty}, c_{\infty}$ = density, sonic velocity of unheated Na in coolant column,

$Z(t)$ = axial location of interface,

$V(t)$ = volume of mixing zone,

A = constant area of cross section of the channel.

The initial pressures predicted by equation E-2 are valid up to a time $2(\ell_1 + \ell_2)/c_{\infty}$. Thus,

$$\frac{dV(t)}{dt} = \frac{A}{\rho_{\infty} c_{\infty}} P(t) - P_{\infty}. \quad (E-3)$$

The first law of thermodynamics is used to evaluate the rate of temperature rise:

$$dQ = \delta W + dU \quad (E-4)$$

where Q , W , and U have the usual meaning. Assuming that $\delta W = pdV$ for a quasi-steady change in volume dV ,

$$dU = dH - pdV - Vdp,$$

so

$$dQ = dH - Udp.$$

It can be shown that

$$dH = mC_p dT + V(1 - \alpha T)dp.$$

Then,

$$\frac{dQ}{dt} = mC_p \frac{dT}{dt} - V\alpha T \frac{dp}{dt},$$

whence

$$\frac{dT}{dt} = \frac{1}{mC_p} \left(V\alpha T \frac{dp}{dt} + \dot{Q} \right) \quad (E-5)$$

where

$$\dot{Q} = dQ/dt = h \cdot A_1 [T_{\text{fuel}}(t) - T(t)] \quad (E-6)$$

and

$$-\frac{dT_{\text{fuel}}(t)}{dt} = \frac{1}{m_{\text{fuel}} C_{p \text{ fuel}}} \cdot \frac{dQ}{dt}. \quad (E-7)$$

h is the heat transfer coefficient between the fuel particles and the sodium, and A_1 is the area of heat transfer. A_1 is given by

$$A_1 = N_1(4\pi R_1^2)$$

where N_1 = number of fuel particles,
 R_1 = radius of each particle.

$$N_1 = \frac{\text{Volume of fuel}}{4/3 \pi R_1^3}.$$

Equations E-1, E-3, E-5, E-6, and E-7 form a complete set of ordinary differential equations which are numerically integrated using the initial conditions to give $P(t)$, $T(t)$, $V(t)$, and $T_{\text{fuel}}(t)$ for the acoustic constraint regime.

2. 1. 1. Effect of Noncondensable Gas on Acoustic Constraint

If a noncondensable gas (such as fission gas) is initially present in the mixing zone, it can affect the process of pressure generation by preventing compression waves from developing in the liquid column above the mixing zone until the gas is sufficiently pressurized. This is because the gas is initially much more compressible than the coolant column.

If V_g is the initial volume of fission or noncondensable gas, then equation E-3 becomes

$$\frac{dV(t)}{dt} = \frac{A}{\rho_{\infty} c_{\infty}} \Delta P - \frac{dV'_g}{dt}.$$

Equation E-1 then becomes

$$\frac{dp}{dt} = \frac{\beta V}{\beta V + \beta_g V_g} \left(-\frac{A}{\beta V} \frac{\Delta P}{\rho_{\infty} c_{\infty}} + \frac{a}{\beta} \frac{dT}{dt} \right). \quad (E-8)$$

Using equation E-5,

$$\frac{dT}{dt} = \frac{1}{(aV/\Sigma\beta V) - (mC_p/TVa)} \left(\frac{A(\Delta P)}{\rho_{\infty} c_{\infty} \Sigma\beta V} - \frac{A_1 \dot{Q}}{TVa} \right), \quad (E-9)$$

where β_g is the compressibility of the fission gas and $\Sigma\beta V = \beta V + \beta_g V_g$; V_g is the instantaneous volume of fission gas.

2.2. Equations for Inertial Constraint

The inertial constraint is further divided into two regimes: the two-phase and the vapor regions. It can leave the two-phase region in a number of ways, re-enter from the vapor phase, leave again, re-enter once more, and so on (see P-V diagram).

2.2.1. Two-Phase Region

In the acoustic constraint regime, once the liquid expansion overtakes the effect of heat addition, the pressure begins to decrease, and it starts to vaporize (at point C in the P-V diagram). In the two-phase calculations it is more convenient to formulate equations so that enthalpy is retained. Then we have

$$\frac{dH}{dt} = \frac{VdP}{dt} + \dot{Q}. \quad (E-10)$$

$$\dot{Q} = hA_1[T_{\text{fuel}}(t) - T(t)];$$

$$-\frac{dT_{\text{fuel}}}{dt} = \frac{1}{m_{\text{fuel}}c_{\text{fuel}}} \dot{Q}.$$

From the momentum equation we have

$$m\ddot{Z}(t) = \Sigma \text{ force}; \quad Z = V/A,$$

or

$$\frac{\rho}{A} (\ell_1 - Z) \frac{d^2V}{dt^2} = [P(t) - P_f(t) - P_{\text{stat}}(t) - P_{\text{cover}}(t) - P_{\text{baffle}}(t)] \quad (\text{E-11})$$

where

- Z = length of heated volume at any instant,
- ρ = density of unheated coolant slug,
- P_f = friction pressure drop,
- P_{stat} = static head pressure drop,
- P_{cover} = cover gas pressure,
- P_{baffle} = pressure drop in baffle at channel exit.

Section 3 of this appendix describes the evaluation of these factors at any instant.

If P is assumed, the new volume (V) can be obtained from equation E-11, and enthalpy can be found using E-10. It is then possible to obtain P , T , and x as described below (where x is the mass fraction of sodium vapor, or quality). The new pressure is compared with the assumed pressure and iterated until the two are within a prescribed limit.

The following equations were used to obtain the pressure (P), temperature (T), and quality (x) from the enthalpy (H) and the sodium specific volume (v).

$$H = xH_v + (1 - x)H_\ell. \quad (\text{E-12})$$

$$H_v = H_v(T); \quad H_\ell = H_\ell(T).$$

H_v and H_ℓ are the enthalpies of saturated vapor and liquid, respectively, and both are known functions of temperature. In addition,

$$v = xv_v + (1 - x)v_\ell. \quad (\text{E-13})$$

$$v_v = v_v(T); \quad v_l = v_l(T).$$

v_v and v_l are the specific volumes of saturated vapor and liquid, respectively, and both are known functions of temperature. Once H and v are known, T and x can be obtained by iterating to satisfy equations E-12 and E-13. This was done in the subroutine CTEMP. After T and x are found, equation E-14 gives the corresponding pressure:

$$P = P(T). \quad (E-14)$$

The equation of state and the thermodynamic and transport properties were obtained from reports by Golden¹ and Booth.²

2.2.2. Vapor Phase

The Himpan equation of state³ (E-15) was used for the vapor phase since its predictions were believed⁴ to be more accurate, especially at very high temperatures and pressures.

$$P + \frac{A}{(V - B)(TV - C)} (V - D) = RT \quad (E-15)$$

where A , B , C , and D are empirical constants and R is the gas constant. The equation can also be written as

$$P = \frac{RT}{V - D} - \frac{A}{(V - B)(TV - C)}, \quad (E-16)$$

so that

$$\begin{aligned} \frac{dp}{dt} = & \left[\frac{R}{V - D} + \frac{AV}{(V - B)(TV - C)^2} \right] \frac{dT}{dt} \\ & + \left[\frac{AT}{(V - B)(TV - C)^2} - \frac{RT}{(V - D)^2} + \frac{A}{(TV - C)(V - B)^2} \right] \frac{dV}{dt}. \end{aligned} \quad (E-17)$$

Comparing E-17 with equation E-1, we find

$$\frac{\alpha}{\beta}_{\text{vapor}} = \frac{R}{V - D} + \frac{AV}{(V - B)(TV - C)^2} \quad (E-18)$$

and

$$-\frac{1}{\beta V} = \frac{AT}{(V-B)(TV-C)^2} - \frac{RT}{(V-D)^2} + \frac{A}{(TV-C)(V-B)^2}. \quad (E-19)$$

The momentum equation is similar to equation E-11.

$$\frac{\rho}{A} (\ell_1 - Z) \frac{d^2V}{dt^2} = [P(t) - P_f(t) - P_{stat}(t) - P_{cover}(t) - P_{baffle}(t)]. \quad (E-20)$$

Since $mC_v = mC_p - (TVa^2/\beta)$, the first law of thermodynamics gives

$$\frac{dT}{dt} = \frac{QA_1 - T(a/\beta)(dV/dt)}{mC_p - (TVa^2/\beta)}; \quad (E-21)$$

$$mC_v = mC_p - (TVa^2/\beta).$$

We also have

$$\dot{Q} = hA_1[T_{fuel}(t) - T(t)]$$

and

$$\frac{-dT_{fuel}}{dt} = \frac{1}{m_{fuel}C_{fuel}} \dot{Q}.$$

If a pressure (p) at time $t + dt$ is assumed, it is possible to evaluate V and T from equations E-18 and E-19. Using E-16, we can determine the new pressure (P) and iterate until the assumed pressure and that from equation E-16 are within a prescribed limit.

One of the major difficulties in evaluating equation E-21 is determining a/β , a , and C_p and their variation with P and T . The variation of a/β and a with pressure and temperature can be found using equations E-18 and E-19. The specific heat of the sodium vapor may be determined as follows:

or

$$\left(\frac{\partial C_p}{\partial P}\right)_T = -T \left(\frac{\partial^2 v}{\partial T^2}\right)_P \quad (\text{E-22})$$

The specific heat of vapor (C_p) decreases with increasing temperature and increases with increasing pressure. It is undefined at the critical point. From equation E-22, if the change in C_p at constant temperature is $(dC_p)_T$ and the change in pressure (B to C in sketch) at constant temperature is dp_T , we have the following:

$$\left(dC_p\right)_T = -T \left(\frac{\partial^2 v}{\partial T^2}\right)_P dp_T$$

or

$$C_p(p_2, T_2) - C_{p_0}(p_1, T_2) = - \int_{p_1}^{p_2} T \left(\frac{\partial^2 v}{\partial T^2}\right)_P dp_T \quad (\text{E-23})$$

It is convenient to choose p_1 at a very low pressure. To evaluate $C_{p_0}(p_1, T_2)$, C_{p_0} can be plotted as a function of temperature (Figure E-1) at a low pressure (1 atmosphere here). With $C_{p_0}(p_1, T_2)$ from the T-S diagram (for any temperature T_2) it is possible to evaluate C_p and T_2 and a higher pressure (p_2) using equation E-23.

The correct evaluation of $C_p(p, T)$ is very important in determining the new pressure and temperature accurately. For example, if the perfect gas law were used, then $dC_p = 0$, and considerable errors would be made in evaluating the pressure and temperature, especially at high pressures where the perfect gas equation deviates considerably from the Himpan equation.

In both the acoustic and inertial constraint regimes, work (W) is calculated using equation E-24.

$$W(t + \Delta t) = W(t) + \bar{P} [V(t + \Delta t) - V(t)], \quad (E-24)$$

where $\bar{P}$ is the average pressure within the time step.

3. Pressure Drop Expressions

The macroscopic momentum balance equation may be written as

$$\frac{\rho}{A} (\ell_1 - Z) \frac{d^2 V}{dt^2} = P(t) - P_f(t) - P_{\text{stat}}(t) - P_{\text{cover}}(t) - P_{\text{baffle}}(t).$$

$P_f(t)$ is the pressure drop due to friction; it was evaluated as follows: Let the steady-state frictional pressure drop be P_1 at a velocity u_1 . The pressure drop per unit length is P_1/ℓ_1 . Then, at a velocity $\dot{Z}$ the friction pressure drop is

$$P'_f(t) = \left\{ \frac{P_1}{\ell_1} [\ell_1 - Z(t)] \right\} \frac{\dot{Z} |\dot{Z}|}{u_1^2}.$$

However, as velocity changes, the friction factor varies, and that is taken into account by $f = k/Re^{0.25}$

Since ρ and A are assumed to be constant,

$$P_f = P'_f (u_1 / \dot{Z})^{0.25}.$$

The variation in hydrostatic pressure is given by $P_{\text{static}} = \rho[\ell_1 - Z(t)]$, and $Z(t)$ is the height of the interface between the mixing zone and the coolant slug. If the slug is injected into a space above the volume,

$V_{\text{cover},o}$ having cover gas at $P_{\text{cover},o}$, then the cover gas pressure at any instant is

$$P_{\text{cover}} = \frac{P_{\text{cover},o} V_{\text{cover},o}}{V_{\text{cover},o} - V'}$$

where $P_{\text{cover},o}$ and $V_{\text{cover},o}$ are the original pressure and volume of cover gas and V' is the change in cover gas volume.

If there are any baffles in the path of coolant flow, the pressure drop in the baffle is

$$P_{\text{baffle}} = \frac{K_p \dot{Z} |\dot{Z}|}{2g_c}$$

where K is the resistance coefficient for flow.

4. References — Appendix E

- ¹ G. H. Golden and J. V. Tokar, Thermophysical Properties of Sodium, ANL-7323, Argonne National Laboratory (1967).
- ² D. L. Booth, The Thermodynamic Properties of UO_2 and Sodium, UKAEA, TRG Report 1871(R/X), 1969.
- ³ J. Himpan, "Die Definitive form der Thermischen Zustandsgleichung Nebst Ihren Stoffkonstanten von über 100 Verschiedenen Stoffen," Monats. Chem., 86 (1955), p 259.
- ⁴ J. P. Stone, et al., High-Temperature Properties of Sodium, "NRL Report 6241, Sept. 24, 1965.

Figure E-1. Variation of Base Specific Heat With Temperature at 1 Atmosphere