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MECHANICAL CONSEQUENCES
OF HYPOTHETICAL CORE
DISRUPTIVE ACCIDENTS

HANFORD ENGINEERING DEVELOPMENT LABORATORY

UNITED STATES ATOMIC ENERGY COMMISSION

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MECHANICAL CONSEQUENCES OF HYPOTHETICAL CORE DISRUPTIVE ACCIDENTS

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April 19, 1971

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MECHANICAL CONSEQUENCES OF HYPOTHETICAL CORE DISRUPTIVE ACCIDENTS

PREFACE

The FFTF design is being evaluated against a hypothetical core disruptive accident capable of releasing theoretically available work in the range of low hundreds of MW-secs. Analyses establishing this energy release level are given in another report⁽¹⁾. That report provides data on thermal energy generated in the core under accident conditions and its conversion to theoretically available work by interaction of molten fuel with reactor core sodium. A bubble of supercritical sodium vapor is postulated to form, which subsequently expands, performing work in the process. The pressure-volume expansion characteristics of this theoretically available work energy form the basic input to the analysis of potential mechanical damage to the primary system, which is the subject of this report.

The FFTF reactor plant is being designed to contain the consequences of hypothetical accidents in a manner which permits the disabled core to be effectively cooled by the primary cooling system. This report provides estimates of the loads imposed on the primary boundaries by a hypothetical core disruptive accident characterized by a theoretical work energy of 150 MW-sec. Calculations are also presented in relation to design margins being investigated at an energy level of 350 MW-sec. In those cases, where detail design of components is not sufficiently advanced to permit evaluation, the results given here provide data for design specifications.

Design of systems and components affected by hypothetical accidents will meet the requirement that the allowable collapse load (or instability load) is not exceeded under predicted loadings.

(1) D.E. Simpson, A.E. Waltar, A. Padilla, Jr., "Assessment of Magnitude of Hypothetical Accidents for the FFTF", HEDL-TME 71-34.

I. SUMMARY AND CONCLUSIONS

This report provides a description of the potential consequences of hypothetical core disruptive accidents on the primary coolant boundary. Further, it treats the effects of the postulated accidents on containment structures in the event of energy releases beyond the primary coolant boundary. The objective of this treatment of the accident mechanisms described herein is to demonstrate the capability of FFTF design features, both inherent in the primary boundary and in containment structures to effectively isolate limiting severe accidents from the environs of the facility. The basic input upon which this mechanical assessment is made is taken from hypothetical accident energy release information developed for the FFTF reactor⁽²⁾. The output from this report in the form of containment vessel pressure and leakage transients has been used in radiological evaluations for the site⁽³⁾.

Detailed analyses of the reactor vessel and primary boundary have been performed in order to identify accident induced loads. The systems are designed to withstand these loads without loss of required capability. The basic functional requirement is that the reactor coolant system retain the capability to cool the core under any accident conditions, including those hypothetical conditions involving disassembly and melting of the core. Post Accident Heat Removal analyses, described in detail in another report⁽⁴⁾, indicate a high probability that fuel debris resulting from a core disruptive accident can be cooled within the reactor vessel by natural convection circulation of the primary coolant sodium. Therefore, this report will identify loads on primary loop components, and show that the loops will be available to remove core decay energy.

(2) *ibid.*

(3) C.L. Fies, Radiological Evaluation of a Hypothetical Core Disruptive Accident, HEDL-TME 71-33, February 1971.

(4) Post-accident Heat Removal in LMFBFRs - In-Vessel Consideration, ANL/RAS 71-12, March 1971

The initial mechanical effects of this class of accidents are first experienced by the reactor vessel and vessel cover, followed by progression of these effects to the coolant loops. This report (and summary) will treat these events in that order. A final topic discussed is the containment structure analysis.

In a typical core disruptive accident, thermal energy desposited in the fuel is viewed as causing a rapid heating of a relatively small volume of sodium. The result is the formation of a vapor bubble at high pressure and temperature within the core barrel. The initial bubble pressure acts upon the reactor core support plate, pushing the vessel downwards and deforming the vessel wall radially. The bubble pressure is rapidly reduced by bubble expansion which accelerates liquid sodium above the core upwards. The slug of sodium, after traversing the cover gas space, impacts the reactor cover. The cover and upper vessel walls are accelerated by the impact pressure. The cover is restrained by a system of stretch bolts and the bolts and upper vessel wall may undergo elastic and plastic deformation, thus absorbing work energy.

The foregoing sequence of events has been analyzed with the following results. The downward acceleration of the vessel is arrested by the reactor vessel support structure. The vessel support structure will undergo less than 1.6" of downward movement. This is equivalent to less than 2.6% strain in the preliminary design, and thus is well below ultimate strain. Deformation of the reactor vessel wall has been estimated at 0.1% near the core barrel and 4% near the top of the vessel. Since ultimate strain is more than 25%, it is concluded that the vessel walls will not fail. The foregoing conclusions are based on hypothetical accidents involving 150 MW-secs of theoretical work. For purposes of assessing sensitivity to higher work energies, a value of 350 MW-sec was selected for evaluation. The corresponding maximum vessel wall strain at this higher level was less than 6%.

The sodium slug impact with the vessel cover has been analyzed in detail. Effective pressure induced in the cover by the restraining action of the stretch bolts is approximately 410 psi for 5" of bolt deformation. The collapse limit of the vessel head based upon a conservative model is approximately 550 psi and it is concluded that the head will not fragment even under 350 MW-sec loads. When evaluated against the latter theoretical energy, bolt strain is approximately 10% as compared with an expected material ductility of 25%. Thus a large margin to failure is available in current bolt design and the cover will not become a missile.

The cover pressure history for postulated accidents has been further analyzed to determine loads on the components which penetrate the vessel head. These components include instrument trees, In-vessel Handling Machine, control rod drives, flux monitor, and refueling ports. The components have different mass/area ratios than the vessel cover itself, and thus potential differential accelerations occur at the onset of cover motion, and potential differential decelerations develop when the cover is stopped by the stretch bolts. The loads were determined for use in equipment design specifications so that cover integrity will be maintained by design, and a stringent leakage requirement will be met by plug sealing systems during and after a hypothetical accident.

The hydrodynamic analysis of core pressures has been used to compute pressure-time histories at the reactor vessel outlet nozzles. Pressure pulses at the outlet nozzles were used as input to the analysis of loads on primary loop components. It is shown that plastic deformation of upstream piping will result in attenuation of the pulses such that the first loop component affected, the outlet isolation valve, will be exposed to a pressure currently estimated at approximately 400 psi. Further reduction in pressure occurs until the primary pump inlet is reached where pressure is estimated at 380 psi. The free surface primary pumps contain a sodium-argon interface which acts to dissipate pressure pulses in gas compression. Loads in the intermediate heat exchangers

are influenced by the pump configuration, but probably dominated by pressure pulses originating at the reactor vessel inlet nozzles. These pressures are lower than outlet nozzle pressures owing to attenuation within the vessel by the core support plate. The inlet leg of the piping is much stiffer than the outlet leg and essentially no yielding is predicted to occur in the inlet leg piping from the pressure pulses. The pressure at the IHX is currently estimated at 450 psi. The foregoing pressure pulse values for components are based on a 150 MW-sec release and have been provided for equipment design specifications.

Based on analyses of the reactor vessel, vessel head and primary loop components, it is concluded that although plastic deformation may occur, the primary boundary is designed to retain a capability to remove core decay energy following a hypothetical core disassembly accident of 150 MW-sec theoretical work.

The structures directly affected by a hypothetical core disruptive accident releasing sodium, the reactor cavity and pipeways, are designed for 35 psig. Sodium expulsion is expected to occur with limited dispersion; the process would approximate a pool-type release, and result in pressures of 5 to 10 psig. On the basis of limiting calculations which assume the sodium released as a fine spray, maximum calculated pressure for these spaces was 25 psig; thus a margin of nearly 30% is available relative to design pressure. The peak containment vessel pressure accompanying the accident was computed to be 0.6 psig in another limiting type calculation, as compared to a design pressure of 10 psig. (Higher pressures in the containment vessel result from sodium fires, the subject of another report⁽⁵⁾; however, those pressures do not involve off-site radiological effects.)

While this present assessment is based upon features at a preliminary stage of design, it is not anticipated that further changes as final design progresses will materially affect the conclusions presented herein.

(5) P.R. Shire, J.P. Hale, "Sodium Fire Analyses for the Fast Flux Test Facility", HEDL-TME 71-31, February 12, 1971.

II. PRIMARY BOUNDARY ANALYSIS

This section provides analyses of the response of the reactor vessel, vessel cover, and primary loop components to a hypothetical core disruptive accident. Elements of the boundary which may receive significant loads are identified, and the margins available in the design to withstand the loads are assessed. A view of some of the main features which will be considered in this section is given in Figure 2-1. The reactor vessel is supported by a structure consisting of a slotted conical skirt shown in the sectional sketch. The vessel support structure has a length of approximately 58 inches. The reactor itself has an internal diameter of 20'-3" and will be constructed of type 304 stainless steel with a 2-3/8" wall thickness and a thermal liner of 1" thickness. Analyses have been made of vessel wall strains in the vicinity of the core support plate and near the upper vessel flange. Pressure-time history at the inlet and outlet nozzles has been analyzed for evaluation of loads on primary loop components. The primary outlet piping will be fabricated from stainless steel with a diameter of 28" and 3/8" wall thickness. Piping from the primary pump discharge to reactor inlet is 16" in diameter, with wall thickness of 3/8". Analyses have been performed using properties of both type 304 and 316 stainless steel; the final choice will not affect conclusions drawn from the analyses.

The reactor vessel cover consists of a 21" thick carbon steel slab clad with type 304 stainless steel. Thermal and neutron shielding is attached to the underside of the cover providing a total thickness of 49 inches and mass of approximately 1.6 million lbs, including the weight of head supported components. The 25 ft diameter cover is attached to the reactor cavity structure through the vessel support ring by a system of stretch bolts. The stretch bolts are approximately 4" in diameter and 90" long. The vessel head is designed with the capability of accommodating up to 90 bolts.

FIGURE 2-1. *Vessel and Cover Features*

The foregoing features represent current design at the time these analyses were made. The effects of design changes will be assessed as they occur to ascertain that essential conclusions on system functional capabilities are maintained. Loads on components (not shown on Figure 2-1) penetrating and supported by the cover are also reported in this section.

A. Reactor Vessel Analysis

The source of potential mechanical damage to the vessel-cover system is the theoretical PV work generated in a hypothetical core disassembly. The calculational models for determining the pressure-expansion characteristics predict a very high acoustic pressure pulse which contains small work energy followed by lower saturated vapor pressures which contain most of the work energy. The lower pressure causes the system to respond rather slowly and the work energy is directed toward the paths of least resistance. The main expansion path for the vapor is upward over the core, pushing coolant ahead of it toward the vessel cover. This mass of sodium which gains kinetic energy is referred to as the "coolant slug".

While the coolant slug is being forced upward, the vapor products in the core region, which constitute a high pressure bubble are also pushing downward on the core support and outward on the vessel wall. Even with saturated vapor pressures, there is a considerable absorption of the gas bubble work energy in the vessel wall.

By the time the coolant slug has moved up into the cover gas region and is about to impact the vessel cover, it has received considerable kinetic energy from the expanding gas bubble. When the slug hits the cover and the gas space is filled, there is no more room for incoming coolant, and water hammer type pressures exist for a very short time (1-2 milliseconds). Energy is transferred from the slug to the vessel cover and upper vessel wall.

With a cover mass of 1-1/2 million lb, most of the slug kinetic energy is dissipated in upper vessel wall strain. A fraction of the initial slug kinetic energy is transferred to the vessel cover. This energy is dissipated by plastic deformation of the holddown bolts.

At this point all vessel motions have stopped. The initial work energy has been expended in plastic deformations of various components or still remains in an unexpanded form as a residual pressure in the gas bubble. The pressure is too low to cause further deformations. The unexpanded energy is primarily in the form of compressed coolant vapor and will condense. Thermal energy remaining in fuel fragments will be dissipated by heat transfer to sodium and structures.

The reactor vessel analysis is based on the foregoing sequence of events.

The reactivity transients associated with a hypothetical core disruptive accident are translated into the effects of heating the fuel-sodium mixture in the core. A high pressure vapor region is assumed to form and the volume and pressure in this region are specified by the computer program SOCOOL II*. This initial condition along with volume-pressure relation for the vaporized region define the input for the hydrodynamics code REXCO⁽⁵⁾. This code uses 2 dimensional (i.e., radial and axial directions in a cylinder) wave analysis techniques. This program includes the mass and equations of state of the reactor internals but conservatively ignores the strength characteristics of the materials except the core barrel, core support structure and reactor vessel where the membrane strength of the material is included. Results include the

(5) Y.W. Chang, J. Gvildys, S.H. Fistedis, "Two Dimensional Hydrodynamics Analysis for Primary Containment", Argonne National Laboratory, ANL 7498, November 1969.

* SOCOOL II data used as input into the REXCO computations discussed in this report are given in Appendix F.

effect of biaxial stress in the vessel wall. The reactor system is divided into a grid which responds to core pressure and produces detailed pressure and velocity histories for selected points of interest. Figure 2-2 is a schematic diagram of the model used to analyze accidents for the FTR. The results from the REXCO calculations provide the basic inputs for establishing the HCDA effects on the mechanical components. The primary outputs from the REXCO analyses that are used to determine the mechanical component structural design criteria are:

- . Integrated pressure (force) versus time history acting on the core support structure,
- . Momentum imparted to the sodium in the upper regions of the vessel,
- . Strain in the vessel.

1. Vessel Wall Strain

Vessel wall strains resulting from HCDA loads are obtained directly from REXCO computations. Strain values are reported for the upper vessel regions near the vessel flange, and in the vicinity of the nozzles. The upper vessel strain results from slug impact with the cover, and much of the slug kinetic energy is dissipated in wall strain. Upper wall strain is 4.3% for a 150 MW-sec theoretical work energy incident. The lower vessel strain occurs opposite the core and is a direct consequence of initial conditions in the expanding gas bubble. Predicted lower wall strain, which occurs in the vicinity of the nozzles, was 0.1%. Thus, vessel strains are less than approximately 5%, and considering that the vessel wall material has the capability to strain more than 25%, the wall should not fail. Neutron irradiation effects have been considered; however, at the vessel wall the calculated fluences of $\sim 4 \times 10^{19}$ nvt cause negligible change in the ductility. For purposes of assessing sensitivity to higher work energies, wall strains have also been computed at the 350 MW-sec level. Results show about 1.5% and 5.8% strain at the lower and upper vessel wall regions respectively.

FIGURE 2-2. *Schematic for REXCO Modeling*

The foregoing strain results were used to assess integrity of the vessel nozzles at the zone of attachment to the wall. Applying the criterion that real vessels with discontinuities such as nozzles can be expected to safely withstand strains of 1/3 ultimate strain⁽⁶⁾, it was concluded that sizable margins to failure are available in the design.

2. Vessel Support Effects

The high pressures generated in the hypothetical accident acts on the core support structure to accelerate the vessel downward. REXCO analysis also provided the forces downward on the core support, as shown in Figure 2-3. These loads cause forces to be transmitted up the vessel wall to the vessel support where some plastic strains occur. The vessel wall and support were modeled using biaxial members with elastic-plastic strain hardening material characteristics. Both the wall strength and vessel inertia resist the pulse type forces. The equations given on pages 2-10 and 2-11 describe the loading of the core support system-

The equations define the force balance on the wall mass (Equation 1), the force balance on the core support element (Equation 2), the axial strain rate (Equation 3), the circumferential strain rate (Equation 4), the radial stress rate (Equation 5) and the axial stress rate (Equation 6).

(6) James F. Proctor, "Containment of Explosions in Water-filled Right-circular Cylinders," Experimental Mechanics, November 1970.

FIGURE 2-3a. REXCO Force History on Core Support (Part A)

FIGURE 2-3b. REXCO Force History on Core Support (Part B)

The force balance on the wall mass is as follows:

$$F_B - F_A + M_w g_c = M_w \ddot{X}_w \quad (1)$$

F_A = Force in support member

$$= (\sigma_z)_A (\text{Area})_A$$

F_B = Force in wall

$$= (\sigma_z)_B (\text{Area})_B$$

σ_z = Axial stress

M_w = Mass of wall

$\ddot{X}_w$ = Acceleration in X direction

g_c = Gravitational constant

The force balance on the core support element

$$F_c(t) - F_B + M_c g_c = M_c \ddot{X}_c \quad (2)$$

$F_c(t)$ = Force history on core support
(Figures A and B)

M_c = Mass of lower vessel

$\ddot{X}_c$ = Acceleration in X direction of
lower vessel

The axial strain rate is determined by

$$\dot{\epsilon}_z = \frac{\dot{X}_c - \dot{X}_w}{X_B} \quad (3)$$

$\dot{\epsilon}_z$ = Axial strain rate

$\dot{X}_c$ = Velocity of mass c

$\dot{X}_w$ = Velocity of mass w

X_B = Length of member B

The circumferential strain rate is determined from strain rate at point of maximum deformation

$$\dot{\epsilon}_{\theta} = 2/3 \dot{R}/R_0 \quad (4)$$

$\dot{\epsilon}_{\theta}$ = Circumferential strain rate (average)

$\dot{R}$ = Vessel wall radial velocity at point of maximum deformation (Table A)

R_0 = Vessel wall initial radius

The stress rates in axial and radial direction are determined by:

$$\dot{\sigma}_{\theta} = (C_{22} \dot{\epsilon}_{\theta} - C_{12} \dot{\epsilon}_Z)/\Delta \quad (5)$$

$$\dot{\sigma}_Z = (C_{11} \dot{\epsilon}_Z - C_{12} \dot{\epsilon}_{\theta})/\Delta \quad (6)$$

$$C_{11} = \frac{1}{E_1} + (1 - g) \frac{S_{\theta}^2}{\frac{2}{3} E_p \bar{\sigma}^2}$$

$$C_{12} = -\frac{\nu}{E_1} + (1 - g) \frac{S_{\theta} S_Z}{\frac{2}{3} E_p \bar{\sigma}^2}$$

$$C_{22} = \frac{1}{E_1} + (1 - g) \frac{S_Z^2}{\frac{2}{3} E_p \bar{\sigma}^2}$$

$$\Delta = C_{11} C_{22} - C_{12}^2$$

E_1 = Elastic modulus

E_p = Plastic tangent modulus

v = Poissons ratio
 S_i = Deviator stress in "i" direction
 $\bar{\sigma}$ = Effective stress
 g = Plasticity condition
 $g = 1$ Elastic deformation
 $g = 0$ Plastic deformation

Equation 6 must be integrated to obtain axial stress which is applied through Equations 1 and 2.

The foregoing equations have been programmed for solution by digital computer. A more complete development of this program is given in Appendix E to this report. Using the force history of Figure 2-3, and strain rate data for the vessel wall, resulting strain on the vessel support is shown in Figure 2-4. The maximum strain was about 2.6%. The foregoing value is based on a vessel support member cross-sectional area of 560 in². Material properties for the vessel support will permit a uniform strain of more than 20% without failure. Thus a substantial margin to failure is available in the design. However, the design of this member is intended to provide a balance between transmission of loads to the reactor cavity structure and vertical displacement of the vessel. With the 2.6% strain value, maximum downward force transmitted by the vessel support to the cavity wall is 17×10^6 lbs. The dead load of the cover and vessel intermediate support ring is added to the foregoing force. The downward force of 17×10^6 lbs includes the mass undergoing motion, i.e., the mass suspended from the vessel support. These loads are within the design specification for the reactor cavity wall. The strain deformation is equivalent to about 1-1/2" of downward displacement. This value is considered well within acceptable limits.

The integrity of the core support plate when subjected to loadings of the magnitude shown on Figure 2-3 was examined in another study⁽⁷⁾.

(7) "Post-accident Heat Removal in LMFBR's - In-Vessel Considerations", ANL/RAS 71-12, March 1971.

FIGURE 2-4. Vessel Support Effects

Results showed that the plate would not fail and that stress at the wall where the plate is joined to the vessel would not cause failure of the vessel wall.

B. REACTOR COVER ANALYSIS

The hypothetical core disruptive accident propels a coolant slug toward the cover and upon impact, energy is transferred from the slug to the cover and upper vessel walls as discussed earlier. This section of the report will present information on slug-cover interactions, including loading on equipment which penetrates the vessel head, cover stress, stretch bolt response, and effects of missiles.

The cover loading history was provided by REXCO calculations for an initial slug energy of 31 MW-sec which occurs in the 150 MW-sec theoretical work energy accident. This is shown in Figure 2-5. Superimposed on the oscillatory force history are straight lines which effectively integrate the impulse. The peak force occurring during the first 1.7 milliseconds is the result of assuming the slug contact surface is flat and impacts a solid, massive cover. Actually, the slug shape will be nonuniform and crushing of the laminated thermal shield allows sufficient time for pressure relaxation by the expanding upper vessel wall. Therefore, these peaks are not considered to occur in the hypothetical accident. In any event, the massive cover has a low natural frequency and would readily absorb this short lived impact pulse. For these reasons, the relatively slower pressure build-up which follows provides controlling loads on the cover. The slower pressure build-up following the initial spike results from increasing wall (and holddown bolt) strengths due to strain hardening of the construction materials. There are two phases to the load history shown on Figure 2-5. The first spans approximately the first 15 msec after impact, and the fluid pressure is sufficiently high that the cover is accelerated. The peak force of the integrating straight line was 63×10^6 lbs which corresponds to an average fluid force per unit area of 1370 psi. At this time, however, the cover is moving away from the

FIGURE 2-5a. *Vessel Cover Force History (Part A)*

FIGURE 2-5b. *Vessel Cover Force History (Part B)*

fluid and does not resist this pressure. Hence, cover stresses are equivalent to pressures far less than fluid pressure. In the second phase of the history, stretch bolt forces overcome the fluid pressure and the cover is rapidly decelerated. This phase occurs late in the load history with maximum forces resulting when the cover is almost stopped. It is this latter phase which presents controlling loads, and these loads are evaluated to assess margins to failure for the cover.

The cover loads are evaluated in terms of maximum stretch bolt force. The cover serves to transmit the head loads to the bolting system, and maximum head stress or effective head pressure is obtained from maximum bolt force⁽⁸⁾. The head loading process is demonstrated by considering the following analysis. By applying the law of conservation of momentum for 1-dimension (inelastic collision between sodium slug and vessel cover), the following relation holds:

$$KE_2 = \left[\frac{M_s}{M_s + M_c} \right] KE_1 \quad (1)$$

KE_1 = Slug kinetic energy before impact

KE_2 = Slug kinetic energy after impact

M_s = Mass of sodium slug

M_c = Mass of vessel cover.

An energy balance is written on the cover as,

$$KE_2 = E_b + [M_c g + M_s g - P_R A_c] x_{\max} \quad (2)$$

E_b = Energy absorbed by head bolts

P_R = Average residual pressure

A_c = Cover area

$x_{\max}$ = Maximum bolt deformation or cover stopping distance.

(8) G.L. Fox, Design Basis Accident Effects on Reactor Vessel Cover Design, ANS Transactions, 13, 1 (June-July, 1970)

The bolts are exerting a downward load on the cover as it is decelerated to a stop, and residual gas bubble pressure provides a force in the opposite direction. The slug and cover masses, of course, act downward. Equation (2) is re-arranged to solve for E_b as,

$$E_b = KE_2 - (M_C g + M_S g - P_R) X_{\max} \quad (3)$$

The following results are applied from the REXCO analysis:

$$KE_1 = 31 \text{ MW-secs}$$

$$M_S = \text{Sodium slug mass} = 8080 \text{ slugs}$$

$$P_R = 190 \text{ psi}$$

The weight of the vessel head including shielding and components is 1.6×10^6 lbs. Head stopping distance, $X_{\max}$, will be fixed at 5". A_C , head area impacted by the sodium slug, is $46,000 \text{ in}^2$.

$$F_R = P_R A_C = 190 \text{ psi} \times 46,000 \text{ in}^2 = 8.7 \times 10^6 \text{ lbs.}$$

KE_2 is obtained from Equation (1) as 3.17×10^6 ft-lbs. By substituting the foregoing values into Equation (3), E_b is found to be 6.0×10^6 ft-lbs.

The average decelerating force is defined by $\bar{F}_E$

$$\bar{F}_E = \frac{E_B}{X_{\max}} = \frac{6.01 \times 10^6}{.416} = 14.45 \times 10^6 \text{ lbs}$$

The maximum decelerating force applied by the retaining mechanism is F_E

$$F_E = \bar{F}_E \frac{\sigma_{\max}}{\sigma_{\text{ave}}} = 14.45 \times 10^6 \frac{39,000}{29,700} = 18.95 \times 10^6 \text{ lbs}$$

$$\sigma_{\max} = \text{maximum stress in retaining mechanism}$$

$$\sigma_{\text{ave}} = \text{average stress in retaining mechanism} \\ \text{(obtained from stress-strain properties of the} \\ \text{stretch bolt material)}$$

The effective head pressure is defined by P_E

$$P_E = \frac{F_E}{A_C} = \frac{18.95 \times 10^6}{46,000} = 410 \text{ psi}$$

Holddown bolt area may be calculated as,

$$\frac{F_E}{\sigma_{\max}} = \frac{18.95 \times 10^6}{39,000} = 486 \text{ in.}^2$$

The foregoing analysis applies to a type 304 stainless steel bolting system with effective cross-sectional area of 485 in.² exposed to a hypothetical accident yielding 150 MW-sec of theoretical work. The maximum effective head pressure was 410 psi. This value compares with a predicted cover collapse pressure of 550 psi. This latter value does not include any strengthening which is imparted to the cover by the neutron-thermal shielding located below the head. Thus a substantial margin is available in the cover design relative to the predicted 150 MW-sec load.

The head bolt elongation was five inches in the above analysis, which is equivalent to less than 6% elongation. Since the bolt material is capable of withstanding strains of 25% uniform elongation, the vessel cover would not become a missile.

For the assessment of the system capability at the 350 MW-sec level, the results of the calculations give a maximum effective head pressure of 505 psi, and head bolt elongation of <10%.

The cover load history is also to determine holddown requirements for reactor components which penetrate the vessel head. Some components will experience maximum loadings during the acceleration phase (of the load history, Figure 2-5) while others will have maximum loadings during the deceleration phase. The difference lies with the area/mass ratio. If the component has a larger area/mass ratio than

the cover, it is more easily accelerated during the first phase where the fluid pressures are high and experience maximum loadings at that time. The deceleration phase produces maximum loadings when the area/mass ratio is smaller than the cover.

Equations will now be developed which describe the plug forces required to keep the plugs integral with the cover during the loading history.

A free body diagram of the cover-plug system is presented in Figure 2-6. Newtons law is applied for both cover and plugs.

$$\sum_i F_i = \text{Mass} \frac{dV}{dt}$$

F = Force

V = Velocity

t = Time

For the cover:

$$P_I A_S - F_B - Mcg + F_p = Mc \frac{dV_c}{dt}$$

$$P_I A_S - F_B + F_p = Mc \left[\frac{dV_c}{dt} + g \right] \quad (1)$$

A_S = Cover area minus plug area

F_B = Bolt force

Mc = Cover mass

g = Gravity

F_p = Plug holddown force

V_c = Cover velocity

P_I = FLUID PRESSURE
 F_B = HOLDDOWN BOLT FORCE
 F_P = PLUG HOLDDOWN FORCE

FIGURE 2-6. *Cover Plug System*

For the plug:

$$P_I A_p - F_p - M_p g = M_p \frac{dv_p}{dt}$$

$$P_I A_p - F_p = M_p \left[\frac{dv_p}{dt} + g \right] \quad (2)$$

A_p = Plug Area

M_p = Plug mass

V_p = Plug velocity

In order to hold the system together

$$\frac{dv_c}{dt} = \frac{dv_p}{dt} \quad (3)$$

Combining (1), (2), and (3)

$$\frac{P_I A_s - F_B + F_p}{M_c} = \frac{P_I A_p - F_p}{M_p} \quad (4)$$

$$\text{Using } M_c = M_T - M_p \quad (5)$$

$$\text{and } A_s = A_T - A_p \quad (6)$$

$$\text{and } P_e = F_B / A_T$$

A_T = Total area inside vessel

M_T = Total cover mass (including all plugs)

the following form can be obtained:

$$\frac{F_p}{A_p} = P_I \left[1 - \frac{A_T}{M_T} \frac{M_p}{A_p} \right] + P_e \frac{A_T}{M_T} \frac{M_p}{A_p} \quad (7)$$

Equation (7) represents the required force per unit area to prevent plug and cover from separating.

Values for component masses and areas were determined as shown in the following tabulation:

<u>Component</u>	<u>Area</u> <u>ft²</u>	<u>Weight</u> <u>lb</u>	<u>Area</u> <u>Weight</u> <u>ft²/lb</u>	$\frac{A_T}{M_T} \frac{M_p}{A_p}$
Cover (Total)	322	1,600,000	2.01×10^{-4}	1
Instrument Tree	18	40,000	4.5×10^{-4}	.447
In-vessel Handling Mechanism (IVHM)	36.2	70,000	5.17×10^{-4}	.389
Fuel Transport	0.327	682	4.8×10^{-4}	.42
Closed Loops	0.1577	1,500	1.05×10^{-4}	1.915
Flux Monitor Plug	0.442	2,000	2.21×10^{-4}	0.9095
Control Drive (CRDM)	0.165	2,000	0.825×10^{-4}	2.436
Fuel Transport Assembly	.442	57,000	0.0775×10^{-4}	25.93

Inserting these parameters into Equation (7) produced the following force loadings :

<u>Component</u>	<u>Maximum Upward Force</u> <u>lb</u>
Instrument Tree	2.27×10^6
In-vessel Handling Mechanism (IVHM)	4.91×10^6
Fuel Transport	4.30×10^4
Closed Loops	1.39×10^4 (1)
Flux Monitor Plug	2.5×10^4
Control Drive (CRDM)	1.73×10^4 (2)
Fuel Transport Assembly	3.72×10^5

(1) Maximum downward force = 4.1×10^4 lb

(2) Maximum downward force = 5.6×10^4 lb

Vessel head equipment is being analyzed to determine that the allowable collapse load (or instability load) of the pressure boundary is not exceeded under the foregoing loads. Loads on the plugs which may occur from core pressures prior to slug impact are currently being re-assessed. Design specifications will reflect dominant loads.

The 350 MW-sec work energy level results in plug loads which are higher than the 150 MW-sec level by factors ranging from about 1.3 for the larger plugs to about 1.6 for the smaller plugs. The design will be evaluated for its capability to withstand those loadings.

Missile Evaluation

The reactor vessel head access space is covered with a deck plate as described in the PSAR Section 5, Containment. This head space cover, designed to provide a work area for maintenance and refueling, has an inherent missile barrier capability. Actually, there will be no missiles as the reactor head, plugs and penetration closures are being designed to remain intact, with restricted leakage, even under hypothetical core disruptive accident conditions. However, the capability of the deck plate as a missile barrier has been investigated and results are reported here.

The following potential missiles have been considered:

<u>Missile</u>	<u>Effective Diam, in</u>	<u>Impact Diam, in</u>	<u>Length or Acceleration Distance (in)</u>	<u>Weight, lb</u>
(1) Liquid level monitor port plug	8	11	49	500
(2) Low level neutron flux monitor plug	9	12	49	10000
(3) Open test position plugs	5.5	6	60	400
(4) Fuel Transfer Plug	7.75	10	14	1000
(5) Core Restraint	12	15	49	15000

A preliminary evaluation was made of the missile containment capability of a 2-inch thick steel plate. The calculational method considered the energy transfer from coolant slug impact and also the additional energy that results from the vapor bubble driving pressure.

Calculational Method

The analysis is divided into two phases.

1. Missile energy at impact with deck
2. Missile containment potential of deck.

The missile energy was evaluated for both small and large gas gaps between the sodium and the cover. Large gas gaps produced the highest slug kinetic energy but lowest residual pressure.

The loadings on the cover were obtained for a theoretical work energy release of 150 MW-sec. The starting conditions are listed below:

<u>Gas Gap, ft</u>	<u>Max. Cover Velocity, Ft/sec</u>	<u>Residual Pressure, psia</u>
1.5	11	185
0.5	9	285

It was assumed the impulse pressure to the plugs was the same as the cover.

$$\frac{M_c V_c}{A_c} = \int_t P_I dt = \frac{M_p V_p}{A_p}$$

M_c = Cover mass

M_p = Plug mass

V_c = Maximum cover velocity

V_p = Plug velocity at end of impact

A_c = Cover area

A_p = Plug area

P_I = Impulse pressure

t = Time

Rearranging:

$$V_p = \frac{(A_p/M_p)}{(A_c/M_c)} V_c$$

Additional energy is imparted to the plugs by the residual pressure and was computed by Newtonian mechanics:

$$V_{\max}^2 = V_p^2 + 2 ad$$

$V_{\max}$ = Plug velocity as it clears cover

a = Plug acceleration = $P_r A_p / M_p$

d = Plug length (acceleration distance)

P_r = Residual pressure (assumed constant)

The kinetic energy possessed by the plug is then

$$KE_p = 1/2 M_p V_{\max}^2 = A_p \left[P_r d + \frac{A_p}{M_p} \frac{M_c^2}{A_c^2} \frac{V_c^2}{2} \right]$$

This completes the description of the equations for calculating the missile energy at impact. The calculation of energy absorption by the deck will be discussed next.

Stanford Research Institute performed a series of experiments to determine the characteristics of cylindrical missiles penetrating square steel plates which were fixed on all edges.⁽⁹⁾ These experiments demonstrated that, for any given kinetic energy content per unit impact diameter, the worst missile penetration effect resulted from a cylindrical, tool steel rod with flat ends striking normal to the target surface. Therefore, the equation developed in these experiments gives conservative results for the penetration resistance of a rigidly supported steel plate when struck by a missile of a milder grade (than tool steel) with any general physical configuration.

(9) AEC Research and Development Report SRIS-43, "Containment of Fragments from a Runaway Reactor" by N.B. Botsford, et al.

The equation developed in the Stanford report is as follows:

$$1) \frac{E}{D} = \frac{\sigma_{ult}}{46,500} [16,000 T^2 + 420 (W-d)T]$$

E = Missile kinetic energy (ft-lb)

D = Impacting diameter of missile (inches)

σ_{ult} = Ultimate tensile strength of the barrier plate (lbs/in²)

T = Thickness of barrier plate (inches)

W = Distance between supports for a square plate (inches)

NOTE: This equation is based on using a barrier steel with an ultimate strength of 46,500 lbs/in²

The Stanford equation was applied with the following input conditions in order to determine the energy containment potential of a 2-inch steel deck.

σ_{ult} = 90,000 psi

T = 2 inches

W = 56 inches

The final results of the missile calculation is shown here:

<u>Missile</u>	<u>KE_p ft-lb</u>	<u>E Penetration Resistance Kinetic Energy ft-lb</u>
(1) Liquid Level monitor port plug	6.22 x 10 ⁴	2 .2 x 10 ⁶
(2) Low level neutron flux monitor plug	7.07 x 10 ⁴	2.3 x 10 ⁶
(3) Open test position plugs	3.40 x 10 ⁴	1.2 x 10 ⁶
(4) Control rod drive mechanism	.343 x 10 ⁴	0.8 x 10 ⁶
(5) Fuel Transfer Plug	1.78 x 10 ⁴	2.0 x 10 ⁶
(6) Core Restraint	12.6 x 10 ⁴	2.6 x 10 ⁶

The maximum plug energy occurred with the smallest gas gap and the highest residual pressure so only these values are presented.

All of these missiles lacked by an order of magnitude sufficient energy to penetrate a 2-inch steel plate. The effect of larger energy releases can be assessed by noting that the plug kinetic energy is primarily a function of the residual pressure. Estimations for the residual pressure for theoretical work energy releases of 350 MW-sec are about 365 psi or about 30% higher. The plug kinetic energy therefore would be about 30% higher also. Thus, in the event of design failures, missile protection appears assured.

C. PRIMARY SYSTEM COMPONENTS ANALYSIS

This section describes the analytical approach and results of computations performed to estimate pressure loading on primary loop components under hypothetical accident conditions. Relative location of components is shown schematically in Figure 2-7. The primary loads will be applied to evaluation of the components to determine that the primary system boundary remains intact. Section A of this report describes how the REXCO computer code is used to prescribe conditions in the vessel during a hypothetical accident. The pressure predictions from this code at the inlet and outlet nozzles provide the prime pressure history needed to specify conditions throughout the primary system. The response history predicted by the REXCO code for the 150 MW-sec accident is given in Figure 2-8 for the outlet nozzles and Figure 2-9 for the inlet nozzles. It should be noted that the core support plate and vessel bottom are modeled as flat surfaces which do not provide membrane strength until substantial deformation has occurred. It is currently believed that inlet region pressure pulses in a real system would be attenuated by the core support plate to lower values than shown in Figure 2-9.

The primary system can be thought of as a hydraulic system subjected to the pressure histories from the HCDA at the inlet and outlet nozzles. These pressure histories at the nozzles are composed of a series of short pulses that result from the acoustic reflections of the pressure waves traveling in the reactor vessel. Normally, pressure waves in hydraulic systems are analyzed with a specialized theory found in texts on Water Hammer Analysis. A computer program⁽¹⁾ for performing such calculations (WHAM) is being used to study the overall behavior

(1) S. Fabric, Pressure, Velocity, and Force Transients in Liquid Filled Piping Networks, Kaiser Engineers No. 67-49-R, Argonne Code Center Reference Material Abstract 278-WHAM, November 1967.

FIGURE 2-7. *Relative Location of Primary System Components*

2-32

FIGURE 2-8a. Outlet Nozzle Pressure History (Part A)

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FIGURE 2-8b. Outlet Nozzle Pressure History (Part B)

FIGURE 2-9a. Inlet Nozzle Pressure History (Part A)

FIGURE 2-9b. Inlet Nozzle Pressure History (Part B)

of pressure waves in the FTR primary system. The nature of the pressure pulses predicted by the REXCO analyses precludes a direct interpretation of REXCO's output with WHAM. Some of the very high peaks predicted by REXCO would be damped or smoothed by plastic deformations in a localized region of the piping. The calculational procedure described in Appendix D and implemented with the computer code BHAM⁽²⁾ is used to define the effects of short, high pressure peaks. The rapid pressure variations are smoothed during transmission. The effects of these details of the combined hydraulic wave transmission in a flexible walled container is studied with the general hydraulics code PISCES⁽³⁾. Thus, the analyses from the BHAM and PISCES code calculations provide a bases for treating the input pressure histories from REXCO before doing the complete system analyses with WHAM. Maximum pressure pulse magnitudes are then used to specify the primary pressure loads for the various system components.

The evaluation of the effects of these pressure histories on the primary piping requires that the true stress-strain properties of the piping be described at representative strain rates and temperature conditions. True stress-strain values were obtained from tensile tests* run at 1000 °F and at strain rates of 0.1^{-1} and 1.0 sec^{-1} . The results from these tests indicated that no appreciable difference in strain-rate hardening resulted in this strain-rate range. Predictions of the strain rates in the piping material near the isolation valve were in the strain-rate range of the tensile tests. The test values and derived values used for the analyses are given in Table C-1.

(2) G.L. Fox, K.R. Merckx, Pressure Wave Transmission in a Fluid Contained in a Plastically Deforming Pipe, WADCO, Richland, Washington, 1971, To be published.

(3) A General Description of PISCESTM/DYNAMIC 1-DL, Physics International Company, San Leandro, California, March 1970.

*Test data were run and calculations were performed for both 316 SS and 304 SS. The results for the 316 SS are presented in this section, but similar conclusions were obtained from the studies on 304 SS.

Piping System

Since the pressure pulses generated during the HCDA encounter the inlet and outlet piping before any other primary system components, the piping near the reactor vessel attenuates the high pressure peaks. This pulse attenuation is studied with the computer program BHAM described in Appendix D. The program is based on a one-dimensional analysis for the longitudinal displacements of the fluid column where the effects of changes of pipe area due to plastic yielding are taken into account in determining the relationship between the pressure and the longitudinal strain in the fluid column. This solution does not take into account inertial effects for the radial movement of the pipe wall. If the inertia of the pipe had been taken into account the piping would show an increased ability to withstand the high pressure pulses which peak in less than 1 msec in duration. The results of the analyses apply for either constant pressure pulses or constant fluid velocity boundary conditions. Table C-1 gives the experimental material data (true stress-true strain values) and the derived values of the tangent moduli (see Appendix D) used in the analyses for the FTR primary piping under hypothetical core disruptive accident conditions. Tables C-2 and C-3 give the results for the analyses for the attenuation of a pressure pulse in the outlet and inlet legs of the primary piping. These tabular values (Tables C-2 and C-3) give the strain in the pipe (column EPSB), the wave velocity in the fluid (column CP), the strain in the fluid column (column EPS), and the velocity of the fluid (column VT) for any magnitude of pressure (column P).

The analysis for the outlet leg of the piping was interpreted to show that all pressure pulses greater than 383 psi would be reduced to this value due to the plastic deformation of the piping. This analysis shows that the fluid wave travel is dispersive or dependent upon pressure magnitude. The higher the pressure, the slower the wave travels; thus, elastic unloading waves overtake the plastic waves. Once the pipe is strained to a certain level and the material is strain hardened, new pressure pulses of equal or lesser magnitude travel at elastic wave

velocities. Thus, the length of pipe plastically deforming can be determined by summing up the time of pulse amplitude at a given pressure and multiplying it by the wave velocity. Figure 2-10 is a plot of accumulative time at a given pressure obtained from the REXCO analyses* of the 150 MW-sec HCDA. Since a ~6 msec of pulse reaches the pressure corresponding to yielding and the most rapid plastic wave velocity (row two of Table C-2) is 1,510 ft/sec, only 7.5 ft of pipe yields. At 1.0 msec of pulse length, the pressure is 800 psi and the corresponding wave velocity is ~580 ft/sec; thus, less than 8" of pipe would strain to the 3.4% level. At pulse of shorter length, inertial effects are controlling and resist these rapid deformations within the pipe. Thus, only a short length of pipe yields and even in the highly strained (~3.4%) region, the strains are far from those that would cause a plastic instability. Plastic deformation in the outlet leg of pipe does smooth out the pulses so that only a pulse of ~400 psi travels through the rest of the system.

The inlet leg of the piping is stiffer than the outlet leg (Table C-3) and the peak pressures at the inlet nozzle are less than those at the outlet nozzle. The few peaks of over 700 psi (Figure 2-9) would be reduced by plastic deformation. The actual plastic deformation in the inlet piping system is less than 0.2%; hence, at the level associated with the engineering yield stress.

Valves

The isolation and check valves in the primary system have similar wall thickness to radius ratios as the piping system; hence, can resist pressure pulses of the same magnitude as the piping. Also a pressure wave from the piping is transmitted into a larger area in the valve which causes a reduction in pressure for a short pulse. In the outlet leg of the primary system the peaks have been smoothed by pipe deforma-

*Locations 4, 5, and 6 are nodal areas approximately 12 inches apart.

FIGURE 2-10. Acting Time of Pressure Pulse - REXCO Results

tion; hence, the pressures remain at ~400 psi. In the inlet leg of the primary system all high pulses are short. The REXCO pulse energy should be averaged over the volume driving the fluid wave through the pipe. This averaging decreases the pulse heights from the individual elements and no plastic deformation should occur in the valves in the inlet leg.

Pumps

The primary sodium pump barrel contains a free sodium-argon interface. The WHAM analyses for the system behavior have shown that the argon-sodium boundary effectively dissipates pressure pulses by gas compression in the pump shell. Thus, pressures at the pump inlet can peak at 380 psi, but are reduced to 200 psi or less in the pump shell.

Intermediate Heat Exchanger (IHX)

The long distance of wave travel through the primary piping has reduced the pulse heights from the inlet nozzle and the pump gas-sodium boundary has decreased the pulse from the outlet nozzle to a low pressure. The IHX also has a large area compared to the inlet pipe and a large volume. These factors are judged to result in the reduction of pulse heights to 450 psi or less at the IHX.

TABLE C-1 TRUE STRESS-STRAIN RELATIONS

136 SS ANNEALED TESTED AT.1 (SEC*-1) AND 1000F

STRAIN EPSB	ADJUSTED STRESS (PSI) SIG	EXPERIMENTAL DATA (PSI) SIGD	TANGENT MODULUS ET
.1000-02	.1394+05	.1394+05	.1394+08
.1000-02	.1394+05	.1394+05	.1430+07
.1300-02	.1449+05	.1470+05	.1759+07
.1800-02	.1530+05	.1530+05	.1476+07
.2600-02	.1635+05	.1618+05	.1138+07
.3200-02	.1700+05	.1687+05	.1026+07
.5100-02	.1877+05	.1790+05	.8418+06
.8800-02	.2142+05	.1991+05	.5918+06
.1440-01	.2419+05	.2277+05	.3968+06
.1820-01	.2565+05	.2456+05	.3725+06
.2410-01	.2780+05	.2692+05	.3560+06
.3360-01	.3118+05	.3088+05	.3554+06
.4350-01	.3460+05	.3430+05	.3363+06
.5080-01	.3701+05	.3753+05	.3214+06
.6280-01	.4075+05	.4059+05	.3033+06
.7230-01	.4363+05	.4347+05	.3019+06
.8190-01	.4651+05	.4634+05	.2978+06
.9120-01	.4924+05	.4941+05	.2894+06
.1007+00	.5194+05	.5198+05	.2801+06
.1101+00	.5451+05	.5451+05	.2660+06
.1193+00	.5695+05	.5699+05	.2643+06
.1286+00	.5927+05	.5913+05	.2342+06
.1376+00	.6127+05	.6187+05	.2103+06
.1486+00	.6349+05	.6307+05	.1938+06
.1562+00	.6494+05	.6484+05	.1885+06
.1655+00	.6664+05	.6665+05	.1761+06
.1745+00	.6825+05	.6829+05	.1814+06
.1836+00	.6984+05	.6955+05	.1689+06
.1863+00	.7029+05	.7070+05	.1645+06
.2015+00	.7263+05	.7260+05	.1436+06
.2106+00	.7393+05	.7380+05	.1418+06
.2192+00	.7509+05	.7501+05	.1286+06
.2228+00	.7554+05	.7576+05	.1223+06
.2367+00	.7708+05	.7707+05	.9891+05
.2457+00	.7796+05	.7789+05	.9549+05
.2474+00	.7812+05	.7806+05	.9485+05
.2508+00	.7844+05	.7850+05	.9356+05

TABLE C-2

OUTLET PIPE CONDITIONS FOR 316 SS AT 1000 °F

PRESSURE (PSI) P	STRAIN IN PIPE EPSB	WAVE VELOCITY (FT/SEC) CP	STRAIN IN FLUID LPS	VELOCITY OF FLUID (FT/SEC) VT	
.3829+03	.1000-02	.3645+04	-.2622-02	.9558+01	.0729-12
.3829+03	.1000-02	.1511+04	-.2622-02	.9558+01	.1485-01
.3979+03	.1300-02	.1444+04	-.3246-02	.1048+02	.2078-01
.4197+03	.1800-02	.1323+04	-.4282-02	.1191+02	.2208-01
.4476+03	.2600-02	.1160+04	-.5927-02	.1396+02	.2485-01
.4648+03	.3200-02	.1100+04	-.7155-02	.1534+02	.2728-01
.5114+03	.5100-02	.9904+03	-.1103-01	.1939+02	.3129-01
.5793+03	.8800-02	.8176+03	-.1854-01	.2618+02	.3769-01
.6469+03	.1440-01	.6491+03	-.2985-01	.3448+02	.4421-01
.6808+03	.1820-01	.6211+03	-.3751-01	.3934+02	.4730-01
.7292+03	.2410-01	.5973+03	-.4938-01	.4658+02	.5022-01
.8024+03	.3360-01	.5844+03	-.6850-01	.5787+02	.5134-01
.8761+03	.4350-01	.5526+03	-.8842-01	.6919+02	.5629-01
.9201+03	.5080-01	.5282+03	-.1031+00	.7712+02	.5680-01
.9793+03	.6280-01	.4942+03	-.1272+00	.8945+02	.6070-01
.1039+04	.7230-01	.4817+03	-.1463+00	.9876+02	.6428-01
.1087+04	.8190-01	.4660+03	-.1656+00	.1079+03	.6638-01
.1129+04	.9120-01	.4458+03	-.1842+00	.1164+03	.6729-01
.1169+04	.1007+00	.4244+03	-.2033+00	.1247+03	.7008-01
.1204+04	.1101+00	.3969+03	-.2222+00	.1324+03	.7458-01
.1235+04	.1193+00	.3850+03	-.2406+00	.1397+03	.7793-01
.1261+04	.1286+00	.3545+03	-.2592+00	.1464+03	.8468-01
.1281+04	.1376+00	.2896+03	-.2773+00	.1520+03	.1038+00
.1296+04	.1486+00	.2494+03	-.2993+00	.1579+03	.1203+00
.1308+04	.1562+00	.2318+03	-.3145+00	.1616+03	.1294+00
.1317+04	.1655+00	.1963+03	-.3331+00	.1656+03	.1428+00
.1325+04	.1745+00	.1992+03	-.3512+00	.1691+03	.1506+00
.1331+04	.1836+00	.1594+03	-.3694+00	.1724+03	.1572+00
.1333+04	.1862+00	.1436+03	-.3748+00	.1752+03	.2166+00

FLO BECAME UNSTABLE AT P= .1336+04

TABLE C-3

----- INLET PIPE CONDITIONS FOR 316 SS AT 1000 °F

PRESSURE (PSI) P	STRAIN IN PIPE EPSB	WAVE VELOCITY (FT/SEC) CP	STRAIN IN FLUID LPS	VELOCITY OF FLUID (FT/SEC) VT	
.6842+03	.1000-02	.4474+04	-.3111-02	.1392+02	.8441-02
.6842+03	.1000-02	.1989+04	-.3111-02	.1392+02	.2011-01
.7109+03	.1300-02	.1902+04	-.3755-02	.1517+02	.2103-01
.7499+03	.1800-02	.1747+04	-.4818-02	.1711+02	.2290-01
.7998+03	.2600-02	.1537+04	-.6499-02	.1987+02	.2403-01
.8306+03	.3200-02	.1458+04	-.7749-02	.2174+02	.2744-01
.9138+03	.5100-02	.1315+04	-.1168-01	.2720+02	.3042-01
.1035+04	.8800-02	.1088+04	-.1928-01	.3633+02	.3277-01
.1156+04	.1440-01	.8652+03	-.3068-01	.4745+02	.4223-01
.1217+04	.1820-01	.8280+03	-.3838-01	.5347+02	.4831-01
.1303+04	.2410-01	.7965+03	-.5032-01	.6367+02	.5022-01
.1434+04	.3360-01	.7793+03	-.6953-01	.7881+02	.5133-01
.1560+04	.4350-01	.7371+03	-.8953-01	.9398+02	.5426-01
.1644+04	.5080-01	.7046+03	-.1043+00	.1046+03	.5577-01
.1768+04	.6280-01	.6595+03	-.1285-00	.1211+03	.6065-01
.1857+04	.7230-01	.6429+03	-.1476-00	.1336+03	.6222-01
.1942+04	.8190-01	.6220+03	-.1670-00	.1458+03	.6431-01
.2018+04	.9120-01	.5952+03	-.1857-00	.1572+03	.6721-01
.2089+04	.1007+00	.5666+03	-.2048-00	.1683+03	.7059-01
.2151+04	.1101+00	.5300+03	-.2237-00	.1787+03	.7547-01
.2206+04	.1193+00	.5140+03	-.2422-00	.1883+03	.7781-01
.2254+04	.1286-00	.4468+03	-.2609-00	.1973+03	.8052-01
.2286+04	.1376-00	.3860+03	-.2789-00	.2048+03	.1036+00
.2320+04	.1486-00	.3532+03	-.3010-00	.2127+03	.1200+00
.2337+04	.1562-00	.3098+03	-.3162-00	.2176+03	.1291-00
.2354+04	.1655-00	.2624+03	-.3348-00	.2230+03	.1425-00
.2368+04	.1745-00	.2663+03	-.3528-00	.2277+03	.1502-00
.2379+04	.1836-00	.2131+03	-.3711-00	.2321+03	.1577-00
.2382+04	.1863-00	.1922+03	-.3765-00	.2332+03	.2081-00

PIPE BECAME UNSTABLE AT P= .2387+04

III. CONTAINMENT ANALYSIS

The previous section described response of the reactor vessel to a series of hypothetical events and ended approximately 100 msec after the start of the incident. At that time, under the limiting conditions hypothesized, sodium in the outlet plenum of the reactor vessel was assumed to be pressurized by a sodium vapor bubble. This section will describe analysis of the transient sodium release resulting from the foregoing events, and its effect on the reactor cavity structure and containment vessel.

A. SODIUM RELEASE

The vapor bubble is assumed to provide a driving pressure for release of sodium if a flow path is postulated. The quantity of the release depends on rate of pressure decay. The lifetime of the sodium vapor bubble, in the midst of a sub-cooled sodium pool, is expected to be short. Chances for rapid condensation are excellent owing to the large heat sinks represented by core internals and the reactor sodium. The sodium pool is approximately 23 ft deep from the core center line to the normal sodium level. There are 500,000 lbs of sodium in the reactor vessel when the level is just above the outlet nozzles. This heat sink with its high thermal conductivity can absorb 1000 MW-sec of energy with a temperature rise of only about 6.3°F. In the absence of directly applicable data, the results from the Naval Ordnance Laboratory Fermi Reactor tests have been used to provide an estimate of bubble cool-down time. Those tests⁽¹⁾ provided a simple expression for scaling heat absorption from internal blast pressure gases in a reactor vessel with internal structures and filled with coolant:

$$\Omega = \frac{100}{3} R, \text{ where}$$

$$\Omega = \text{time, msec},$$

$$R = \text{inside radius of vessel, ft.}$$

(1) W. R. Wise, et al, "Response of Enrico Fermi Reactor to TNT Simulated Nuclear Accidents," U.S. Naval Ordnance Laboratory, NOLTR-62-207, November 1964.

For a 20 ft diameter vessel, cool-down time is 330 msec, according to the foregoing expression. However, for conservatism, rather than assuming total heat absorption in the time predicted above, the cool-down time of 0.33 secs was used as a time constant in the following equation:

$$P_r = P_o e^{-t/\tau} \frac{u_i}{u_i + u} \quad \text{Eqn. (1)}$$

P_r = Pressure

P_o = Initial vapor bubble pressure

t = Time

τ = Time constant = 0.33

u_i = Initial bubble volume

u = Volume of sodium expelled

In the calculations discussed here, P_o was taken as 200 psig. The initial bubble volume is a function of the cover gas gap in the vessel and vessel deformation caused by the initial pressure pulses occurring in the accident. These calculations were based on an initial cover gas gap of six inches and initial bubble volume of 740 ft³.

The path for sodium expulsion postulated for this analysis is shown schematically in Figure 3-1. In order to open this path, vessel motion must be sufficient to rupture an omega seal between the reactor vessel and support structure. A second release path into the reactor cavity would be provided if the reactor cover lifts; however, it is more constricted than the path analyzed, and was not included. A seal is provided between the vessel cover and vessel intermediate support ring. This seal remains intact under cover motion and prevents sodium release above the vessel cover.

For the flow calculation, the assumed leakage path was modeled as a simple annular slot, 1/2" wide and 2' long around the 20' diameter vessel. Even though the flange and slotted vessel support skirt provides several corners for turning the flow and causing energy losses, these were ignored. Friction and entrance losses were considered in addition to the pressure change of converting the fluid from a static to a dynamic state. Initially, wave analysis methods were used for the fluid velocity transient; however, preliminary work showed that Newton's formulations were adequate. The following equations were used to describe the flow.

$$V - V_0 = \int_0^t \frac{\text{Force}}{\text{mass}} dt = \int_t \frac{PA}{\rho AL} dt = \int_t \frac{P}{\rho L} dt \quad \text{Eqn. (2)}$$

V	=	Velocity	ρ	=	Fluid density
P	=	Pressure	L	=	Path length
V ₀	=	Initial velocity	A	=	Flow area

The pressure P was represented by:

$$P = P_r - \frac{\rho V^2}{2g} \left(1 + \frac{fL}{d_e} + K_e \right) \quad \text{Eqn. (3)}$$

P _r	=	Residual pressure
f	=	Friction factor
d _e	=	Hydraulic diameter
K _e	=	Entrance loss (0.25)

The friction factor f was represented by:

$$f = 0.0032 + \frac{0.221}{(\text{Rey})} \quad 0.237$$

Rey = Reynolds No.

The total mass flow, Q, was obtained by integrating the velocity equation:

$$Q = \int_t \rho VA dt \quad \text{Eqn. (4)}$$

Equations 1, 2, 3 and 4 were solved simultaneously using the analog simulation program MIMIC. An entrance loss of 0.25 was used; changing this value to 0 and 0.5 was found to vary the sodium release by about $\pm 10\%$. The friction loss term in the flow equation was similarly found to be unimportant.

Results of the flow and pressure decay calculations are shown on Figure 3-2. Driving pressure is down to about 15 psig in about one second, and the total sodium release is about 10,000 lb. This sodium release curve formed the basis of the cavity test pressurization calculation. Sensitivity of the cavity pressure to the quantity of sodium expelled was assessed and found to cause little effect. For example, the peak pressure increased by about 0.9 psig when the sodium release is increased from 10,000 lb to 100,000 lb, and this effect is mainly the result of gas compression by the volume of the sodium released. Those particular calculations are given in an appendix to this report.

B. CONTAINMENT TRANSIENTS

1. Reactor Cavity Analysis

The containment spaces which would be affected by a sodium expulsion are shown in Figure 3-3. This envelope, which consists of the HTS pipeways and cavity proper, is designed for 35 psig. The piping penetrations through the pipeway wall to the heat transport cells are equipped with 35 psig seals. The pressure analysis was made assuming zero leakage from this envelope in order to predict maximum hypothetical pressure rise.

Pressurization of the reactor cavity, by an assumed sodium release, depends upon the character of the release. That is, spillage of sodium is usually assumed to form a pool on the floor with atmosphere heating limited by the surface area of the floor which confines the spill. If the driving pressure is small or if the slots in reactor vessel support structure suppress random spray, the effects may approximate a pool type release. In that case, reactor cavity peak pressure would be in the 5 to 10 psig range. For example, we have calculated a peak pressure of 6.7 psig in

FIGURE 3-2. Sodium Ejection from Reactor Vessel

FIGURE 3-3. *Section View of Reactor Cavity and HTS Pipeways*

the 1% O₂ nitrogen atmosphere of the reactor cavity, for an 80,000 lb spill of 1050°F sodium. Design provisions can be made to limit spillage of reactor outlet sodium to masses less than that amount. With pool releases, the pressure rises slowly, with the peak being reached 2.4 hrs after the spill occurs, in this particular case.

To demonstrate containment capability, we have investigated the effects of a spray release into the cavity. Those cavity transient analyses were made with the CACECO digital computer code, an FFTF project code developed for sodium reactor containment analysis. This code is described in an FFTF analyses code summary.⁽²⁾

The CACECO code provides for conduction, convection, and radiation heat transfer between the heat absorbing structural masses and the atmosphere and sodium pool.

The view factors for radiation heat transfer are evaluated by separate methods for input to CACECO. Also, fission product decay heat is obtained from the RIBD code for input to CACECO. The decay heat is released in the atmosphere, sodium pool, or within the heat absorbing structures according to the input data provided to the CACECO code.

For this analysis, the reactor cavity and HTS pipeways were modeled with nine heat structures because of expected differing heat transfer effects. Figure 3-4 shows the model arrangement used. The reactor vessel and guard tank has sodium, stainless steel, and insulation that absorbs heat or loses it according to temperature differences at the surfaces. The upper reactor vessel structure represents the vessel and tank down to the minimum safe operating level for sodium. Both the upper and lower reactor vessel structures (which include the guard tank) contain sodium taken at a temperature of 900°F to represent an approximate bulk average of inlet and outlet sodium for normal operation. This sodium is cooled by the heat transport system to the shutdown temperature of 500°F following

(2) Summary of Computer Codes used for FFTF Containment and Siting Studies, HEDL-TME 71-30, February 1971.

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FIGURE 3-4. Heat Structures for CACECO Code Analysis

the sodium release. The cavity wall opposite the reactor core was initially considered as a separate structure owing to an expected concentration of gamma heating in the concrete. However, it was found later that this heating is not significant. The lower wall receives more radiant heat from the sodium pool, because of its proximity, than wall areas further up towards the roof. Concrete surfaces were covered with a steel liner 1/4 inch thick and a gas gap of 1/4 inch was assumed between the liner and concrete. These parameters are based on preliminary design; however, design changes are not expected to alter basic conclusions. The floor was insulated with 13.5 inches of firebrick. Piping and valves within the cavity and HTS pipeways absorb or lose heat in a manner similar to the reactor vessel.

A detailed description of the containment model is given in Appendix B to this report.

The temperature and pressure transients were based on the following input data:

Sodium Release

The 10,000 lb sodium release derived in Section III-A was injected into the cavity atmosphere. The release rate at 0.00 sec was calculated as 18,500 lb/sec and at 1.08 sec as 0.0 lb/sec. The release rate was linearly interpolated between these two rate-time points. The internal energy of this sodium was that of liquid sodium at 1050°F. In the CACECO code analysis, material injected into the atmosphere is assumed to mix with the atmosphere. The code calculates the material and energy effects of this mixing and the further effects of the sodium-oxygen reaction in the atmosphere. Sodium not consumed by the chemical reaction, or evaporated, is rained out and forms the sodium pool on the floor of the cavity.

Fission Product Release

The disruption of the core was assumed to release all of the noble gas fission products into the reactor cavity. The thermal energy effects

of these particular fission products were taken into account in the analysis. The time versus decay power of these noble gas fission products is listed below.

<u>Time, hours</u>	<u>Noble Gas Fission Products Decay Power, Btu/hr</u>
0.0	3,560,000
1.0	719,000
7.4	350,000
24.0	165,000
72.0	61,600
120.0	45,600

The fission product decay power includes both beta and gamma decay energy. The decay power for beta is about 50% of the total listed. The beta and gamma energies were estimated to be distributed in the containment model as follows:

50% Beta Energy

- 20% directly into the cavity atmosphere.
- 30% onto the surfaces of the heat structures.
 - 14.2% onto the surfaces of the roof and walls.
 - 14.0% onto the surfaces of the reactor vessel and piping.
 - 1.8% onto the surface of the sodium pool.

50% Gamma Energy

- 44.5% into the steel and concrete of the roof and walls.
- 0.8% into the sodium pool.
- 4.7% into the floor under the sodium pool.

The fission product decay heat generated at the surfaces and in the interiors of the heat structures were input as volumetric heat sources for the heat structures. In the CACECO code, these volumetric heat sources are specified for the spaces between mesh points rather than at the mesh points themselves. Typical values of the heat sources at 1.0 hour are listed below for various structures.

Upper and Lower Reactor Vessel and Primary Piping

<u>Linear Distance into Material</u>	<u>Material</u>	<u>Volumetric Heat Source at 1 hr</u>	<u>Comment</u>
0.4 inches	Kaylo	34.8 Btu/hr-ft ³	Beta heat
3.0	Kaylo	0.0	Gamma neglected

Cavity Roof and Upper Walls, Lower Walls, and HTS Pipeways' Roofs, Walls, and Floors

<u>Linear Distance into Material</u>	<u>Material</u>	<u>Volumetric Heat Source at 1 hr</u>	<u>Comment</u>
0.042 inches	carbon steel	418. Btu/hr-ft ³	Beta heat
0.125	carbon steel	63.4	Gamma heat
0.5	concrete	56.5	" "
6.5	concrete	26.5	" "
33.0	concrete	0.9	" "

Cavity Floor under Sodium Pool

<u>Linear Distance into Material</u>	<u>Material</u>	<u>Volumetric Heat Source at 1 hr</u>	<u>Comment</u>
0.48 inches	firebrick	50.5 Btu/hr-ft ³	Gamma heat
6.27	firebrick	45.0	" "
0.125	steel	33.9	" "
0.5	concrete	26.7	" "
6.5	concrete	15.3	" "
31.5	concrete	0.9	" "

CACECO Code Results

The 10,000 lb sodium injected into the cavity atmosphere during the initial 1.08 seconds is considered to have been sprayed into the atmosphere. It heats the atmosphere by chemical reaction with the 1% oxygen (consuming about 72 lbs of sodium) and by effective heat transfer mechanisms (assumed in the mixing process) while falling to the floor of the cavity. The

heating of the atmosphere to 1037°F (maximum at 0.90 seconds) pressurizes the cavity to 24.7 psig and this is the maximum pressure experienced in the cavity following the accident. The atmosphere cools quickly by heat transfer to the roof, wall, and equipment structures and by 60 sec following the sodium release, the atmosphere temperature is down to 466°F and the pressure down to 9.5 psig. The atmosphere temperature and pressure continue to decrease to minimum values of 278°F and 1.6 psig, respectively, at about 9 minutes. The transient temperature and pressure out to 360 sec (6 min) are shown in Figure 3-5.

The heat losses from the hot sodium pool on the floor and from the reactor vessel and primary piping and the decay heat of the noble gases cause the atmosphere temperature and pressure to increase, slowly, to second maximum values of 306°F and 5.3 psig, respectively, at 1.46 hours. Thereafter, the atmosphere temperature and pressure decrease as shown in Figure 3-6. Figure 3-6 also shows the sodium pool temperature out to 96 hours.

The CACECO code analysis provides detailed data on structure temperatures, heat transfer rates, and integrated heat transfer. Selected temperature data are listed in Table 3-1 for each heat structure. In this table, the sodium temperatures inside the reactor vessel and primary piping and valves show the assumed cooloff due to shutdown: 900°F during normal operation at the outset of the accident, then cooling to the normal shutdown temperature of 500°F. The temperatures of the lower wall, which was subjected to the most thermal radiation from the sodium pool, are shown in Figure 3-7. The temperatures recorded there and in Table 3-1 are judged to fall within structural design limitations.

Selected heat transfer data are listed in Table 3-2 at the same times as temperatures are listed in Table 3-1. The heat transfer data for each heat structure are the heat transfer rates at each surface, integrated over the whole surface, and integrated to the time listed. Both convection and radiation heat transfer are shown to provide an immediate comparison about the relative importance of these two modes of energy exchange in the

FIGURE 3-5. Reactor Cavity Pressure and Temperature Response

FIGURE 3-6. Long Term Reactor Cavity Response

FIGURE 3-7. Cavity Lower Wall Temperatures Due to 10,000 lb Spill of 1050°F Sodium

TABLE 3-1

SELECTED TEMPERATURE DATA

<u>Location</u>	<u>Initial Conditions</u>	<u>3.96 sec, 0.001 hr</u>	<u>6 min, 0.10 hr</u>	<u>1.0 hr</u>	<u>10 hr</u>	<u>96 hr</u>
Cavity atmosphere	100°F	971°F	281°F	304°F	272°F	240°F
Cavity sodium pool	-	960	820	500	280	227
Upper R Vessel						
sodium inside	900	900	728	507	500	500
insul. surface	136	174	282	313	285	250
Lower R Vessel						
sodium inside	900	900	712	503	500	500
insul. surface	136	176	322	331	289	250
Guard T Supports						
metal inside	364	364	364	362	366	332
insul. surface	128	162	333	330	284	248
Cavity Roof and Upper Walls						
liner surface	107	111	158	243	244	229
concrete @ 35"	107	107	107	107	107	127
Cavity Lower Wall						
liner surface	108	112	181	281	252	231
concrete @ 63"	101	101	101	101	101	101
Cavity Floor under Sodium Pool						
insul. surface	98	534	816	500	280	227
steel liner	81	81	81	83	104	183
concrete @ 42"	62	62	62	62	62	64
Primary Piping & Valves						
sodium inside	900	899	701	500	500	500
insul. surface	134	177	270	301	278	248
HTS Pipeways' Roofs, Walls, and Floors						
liner surface	103	106	142	209	230	223
concrete @ 35"	103	103	103	103	103	121

TABLE 3-2
SELECTED HEAT TRANSFER DATA

Total Heat Transfer Rates Integrated to Times Indicated, Btu x 10⁻³.
Notes: (+) indicates heat loss by the structure and (-) indicates heat gained by the structure.
Conv. = Convection heat transfer and Rad. = Radiation heat transfer.

Surface	Mode	3.96 sec, 0.0011 hr	6 min, 0.10 hr	1.0 hr	10. hr	96. hr
Upper R. Vessel						
insul. surface	Conv.	-5	-25	-21	+198	+1,540
	Rad.	-.02	+4	+28	+240	+1,172
Lower R. Vessel						
Insul. surface	Conv.	-9	-41	+48	+665	+3,841
	Rad.	-.5	-15	-62	+189	+1,562
Guard T. Supports						
insul. surface	Conv.	-4	-20	+23	+211	+1,291
	Rad.	-.3	-11	-40	+74	+917
Cavity Roof and Upper Walls						
liner surface	Conv.	-9	-114	-322	-1,220	-5,096
	Rad.	-.5	-49	-286	-1,188	-4,584
Cavity Heated Wall at core midplane						
liner surface	Conv.	-2	-13	+71	+713	+4,503
	Rad.	-.2	-8	+42	+636	+3,790
concrete @ 63"	Conv.	+0.02	+1	+14	+142	+1,467
Cavity Lower Wall						
liner surface	Conv.	-5	-56	-106	-405	-1,954
	Rad.	-.8	-66	-309	-969	-3,300
concrete @ 63"	Conv.	+0.001	.1	.1	+9	+100
Sodium Pool on floor						
top surface	Conv.	-.012	+46	+290	+498	+283
	Rad.	+2	+146	+627	+1,004	+433
bottom surface	Conv.	+144	+378	+646	+858	+2,049
Cavity Floor under sodium pool						
insul. surface	Conv.	-144	-378	-646	-858	-2,049
concrete @ 42"	Conv.	+0.003	.3	+2	+24	+284
Primary Piping and Valves						
insul. surface	Conv.	-5	-26	-40	+46	+966
	Rad.	+0.020	+7	+48	+334	+1,617
HTS Pipeways" Roofs, Walls, and Floors						
liner surface	Conv.	-10	-129	-455	-1,984	-8,451
	Rad.	-.016	-7	-48	-334	-1,616

reactor cavity following the 10,000 lb release of 1050°F sodium which, itself, was initiated at 0.0 hours. All of the heat structures except the sodium pool (itself a heat structure in the CACECO code analysis) act as heat sinks during the early phase of the accident because of the very high atmosphere temperature. Then as the atmosphere cools, the reactor vessel, guard tank supports, and primary piping and valves resume their usual roles of being heat sources due to heat losses through their insulation. The roofs, walls, and floors of the cavity and pipeways act as heat sinks throughout the accident history. The sodium pool loses heat by convection and radiation from its surface to about 10 hours. Thereafter, the pool gains heat by convection from the atmosphere and by radiation from the cavity roof, walls, and equipment. Throughout the whole history, the sodium pool loses heat into the insulated floor.

This case was run with the assumption that there was no leakage from the reactor cavity-HTS pipeways containment space. As a result, the atmosphere pressure 96 hours after the sodium release was 3.6 psig. Even a little leakage would have reduced this pressure to zero. Therefore, when structural temperatures start to rise from thermal energy absorption, the structures should be subjected to little or no pressure.

2. Containment Vessel Pressure Transient Analysis

The cavity pressure analysis was made using the assumption of zero leakage in order to obtain maximum pressure. Therefore, there is no pressurization of containment vessel corresponding to that model. In order to compute a containment pressure transient for radiological evaluations, the cavity was assumed to leak at a rate of 1000%/24 hrs, a factor of 10 greater than design goal leakage, and carry with it fission gas which heats the atmosphere with its decay energy. The fission decay energy was approximated by an energy input term equivalent to 10% of the total core decay energy. The heated atmosphere pressurizes the containment vessel and the pressure decays by heating the steel vessel and transferring heat to the outside atmosphere. Within the reactor cavity, pressure and temperatures are decreased with time by heat absorption in walls and

equipment masses.

This calculation of the containment vessel transient was made with the SOHOT code which solves the differential mass and heat balance equations using the analog simulation program, MIMIC.⁽³⁾ The mathematical formulation of the code described here is based on the model shown in Figure 3-8. For this calculation, as in the CACECO model, the openings between the cavity and pipeways are considered large enough to offer negligible flow resistance.

The development of the equations solved by the code is as follows:

$$P_{IA} = \frac{(M_I)R(T_I)}{V_I} \quad (1)$$

where P_A = partial pressure of nitrogen, lb/ft²

M = mass of nitrogen, lb

R = gas constant (ft-lb/°F)/W = 1545/28

W = molecular weight of gas, lb/mol

T = absolute temperature, °R

V = volume, ft³

I indicates the i th space

$I = 1$, reactor cavity

$I = 2$, pipeways

$I = 3$, outer containment

Equating the pressure in cells 1 and 2,

$$P_{IA} + P_{SI} = P_{2A} + P_{S2} \quad (2)$$

P_{SI} = partial pressure of sodium in cell I , lb/ft²

Substituting (1) into (2) and differentiating with respect to time,

$$\frac{P_{1A}}{M_1} \frac{dM_1}{dt} + \left[\frac{P_{1A}}{T_1} + \frac{dP_{S1}}{dT_1} \right] \frac{dT_1}{dt} = \frac{P_{2A}}{M_2} \frac{dM_2}{dt} = \left[\frac{P_{2A}}{T_2} + \frac{dP_{S2}}{dT_2} \right] \frac{dT_2}{dt} \quad (3)$$

The nitrogen balance in cell 2 is

$$\frac{dM_2}{dt} = - \frac{dM_1}{dt} - L_2 \quad (4)$$

L_2 = leakage from cell 2 to cell 3, lb/hr

⁽³⁾ F. J. Sansom and H. E. Peterson, "MIMIC Programming Manual Air Force Systems Command Document, SEG-TR-67-31, July 1967.

FIGURE 3-8 *Containment Model For Pressure Transient*

Substituting (4) into (3) and redefining terms:

$$\frac{dM_I}{dt} = S_A \frac{dT_2}{dt} - S_B \frac{dT_1}{dt} - S_C \quad (5)$$

$$S_A = \frac{\frac{P_{2A}}{T_2} + \frac{dP_{S2}}{dT_2}}{\frac{P_{1A}}{M_1} + \frac{P_{2A}}{M_2}} \quad S_B = \frac{\frac{P_{1A}}{T_1} + \frac{dP_{S1}}{dT_1}}{\frac{P_{1A}}{M_1} + \frac{P_{2A}}{M_2}} \quad S_C = \frac{\frac{P_{2A}}{M_2}}{\frac{P_{1A}}{M_1} + \frac{P_{2A}}{M_2}} + \frac{L_2}{M_2}$$

The mass balance for cell I is:

$$\frac{dM_I}{dt} = W_{aI} - W_{bI} \quad (6)$$

W_a = gas entering cell, lb/hr

W_b = gas leaving cell, lb/hr

The energy balance for the gas in cell I is:

$$\frac{dU_I}{dt} = (H_{aI})(W_{aI}) - (H_{bI})(W_{bI}) + Q_I \quad (7)$$

U = total internal energy, Btu

H = specific enthalpy, Btu/lb

Q = summation of all heat sources and sinks in the cell emitting or absorbing energy to or from the gas

The rate of change of energy contained in the cell is:

$$\frac{dU_I}{dt} = \frac{d(M_I \cdot u_I)}{dt} = u_I \frac{dM_I}{dt} + M_I \frac{du_I}{dt} \quad (8)$$

u = specific internal energy, Btu/lb

$$\text{But } \frac{du_I}{dt} = \frac{du_I}{dT_I} \cdot \frac{dT_I}{dt} = C_{vI} \frac{dT_I}{dt}$$

C_v = heat capacity of the gas at constant volume, Btu/lb - °F

Substituting this result and combining (6), (7) and (8):

$$\frac{dT_I}{dt} = \frac{(H_{aI})(W_{aI}) - (H_{aI})(W_{bI}) + Q_I - u_I (W_{aI} - W_{bI})}{(M_I)(C_{vI})} \quad (9)$$

Equations (5), (7) and (9) are solved simultaneously to give the temperature and pressure history in cells (1) and (2). Equations (6), (7), (8) and (9) are combined with the differential form of equation (1) to get the mass and energy interchange between the reactor cavity and cell (3), the containment vessel. The pressure-time history in the latter space is thus obtained. Equations were developed by standard methods for unsteady state heat transfer from the cell gas to the cell walls and for leakage between cells.

The leakage from the cavity to containment, and from containment to the outside atmosphere was related to the pressure differential by the orifice equation:

$$\Delta P = K_p L^2$$

$$\Delta P = \text{pressure differential between spaces, lb/ft}^2$$

$$K = \text{combined orifice constant}$$

$$p = \text{gas density, lb/ft}^3$$

$$L = \text{mass leaking, lb/hr}$$

Initial conditions for this calculation were as follows:

	<u>Temp., °F</u>	<u>Pressure, psia</u>
Reactor cavity	1037	40
Pipeways	1037	40
Containment vessel	100	14.7

The foregoing conditions correspond essentially to the peak atmosphere temperature and pressure in the reactor cavity calculated in the preceding sodium spray case. The calculation was started at .01 hours after reactor shutdown. Other typical parameters used are shown in Table 3-3.

TABLE 3-3. PARAMETERS FOR CONTAINMENT TRANSIENTS

Volume of cavity and pipeways	=	32,000 ft ³
Volume of containment	=	1.5x10 ⁶ ft ³
Surface area of containment	=	50,000 ft ²
Mass of steel in shell	=	1.9x10 ⁶ lbs
Natural convection heat transfer coefficient	=	1.0 B/hr-ft ² -°F
Overall U for shell and insulation	=	.15 B/hr-ft ² -°F
Outside ambient temperature	=	100°F

The resulting temperature and pressure transients in the containment vessel are shown in Figure 3-9. The pressure peaks at under 0.6 psig. Maximum atmospheric temperature is 120°F. These results are believed to be conservative since additional heat absorption capability is available in the mass of equipment in the work spaces in the containment vessel. These masses which total about one million lb with a surface area of 27,000 ft² have been neglected in this calculation. Atmosphere coolers available in the containment work spaces have a cooling capacity of 1.8 million Btu/hr. This cooling capability has also been neglected in these calculations.

Effects of a Hypothetical Work Energy Release of 350 MW-sec on Reactor Cavity Pressure Transients

The preceding section reported in detail analyses based on a 150 MW-sec theoretical work release.

The response of the reactor cavity to a 350 MW-sec work energy accident has also been analyzed, and this section will provide a summary of results. The main difference between the two work energy levels is a higher residual vapor pressure for the 350 MW-sec case. The initial driving pressure is approximately 280 psig* instead of 200 psig. Both values are based on a six inch gas gap over the sodium in the

* Pressure after maximum cover travel.

FIGURE 3-9 Containment Building Transients

reactor vessel.

It should be noted that the slug impact analysis described in Section III of this report is based on a gas gap of 18" which results in a residual pressure of approximately 220 psig for a 350 MW-sec theoretical work accident. These two different bases are used since the larger gas gap is the more conservative case for slug impact evaluation, and the smaller gap is more conservative for sodium release rate predictions.

The analysis was performed in a manner identical with the 150 MW-sec case. Sodium release and pressure decay within the vessel follows a pattern similar to that shown on Figure 3-2. However, sodium release totals approximately 12,000 lbs. The cavity pressure transient would be similar to that shown on Figure 3-5. Peak pressure is approximately 25 psig, essentially the same as the 150 MW-sec case owing to the achievement of near thermal equilibrium between the cavity atmosphere and outlet plenum sodium. Additional data on the effects of sodium release quantities on cavity pressure are given in Table III, Appendix C.

Containment Pressure Accompanying Missile Release

Closure plugs for the ports in the reactor cover are designed to remain unseparated from the cover in a manner which limits cover leakage to less than 1000%/24 hrs. The plugs will be locked in place prior to reactor start-up and specific design and administrative procedures will be used to verify that correct locking procedures have been completed. However, consistent with the missile analysis given in an earlier section of this report, analyses have been made of the consequences of loss of integrity of hold-down for the cover plugs. The potential openings analyzed were approximately 5-1/2", 8", 9" and 12" in diameter. The results of the analysis for the largest port will be reported here.

It was assumed that the port opened up at the end of the cover travel induced by sodium slug impact and liquid sodium was assumed to be pressed against the cover by the residual pressure from the hypothetical disassembly accident core vapor bubble.

The driving pressure for sodium release was taken as 200 psi, as compared with a residual pressure of 165 psi and 220 psi associated with a 150 and 350 MW-sec theoretical work energy respectively (1-1/2 ft gas gap). In these calculations, the driving pressure persisted for two seconds and was diminished only by increase in volume resulting from sodium expulsion from the vessel. This basis is considered conservative since the vapor bubble is expected to collapse by condensation in less than one second.

For these conditions, calculations showed an initial sodium release rate of 6,100 lbs/sec with a total of 11,700 lbs of Na expelled over the two second period.

The head cavity is provided with a maintenance access deck which covers the entire vessel head area. The deck also functions as a missile barrier and sodium spray deflector. This deck, in effect, provides a vented cover for the head cavity region. The pressure analysis performed assumes that sodium released through the vessel head strikes the deck and rebounds as droplets through the air atmosphere in the head cavity. The droplets were assumed to have a diameter of 0.05 cm and burn at a rate of 50 lbs/hr-ft² of surface area. More specifically, in the calculational model, sodium was assumed to be uniformly distributed over the area of the deck and rain down through the air atmosphere at rates equal to release from the plug. The choice of particle size and the wide distribution of the release is believed to be conservative. An assessment of conservatism in the burning rate has not been made, but the particle size and distribution assumptions will compensate for substantially higher burning rates.

Results of the calculations indicate a pressure buildup under the deck of less than 6.5 and 4 psig for vent areas of 200 and 300 ft², respectively. The sodium which is un-burned during the flight time between deck and floor, is assumed to accumulate on the floor of the cavity and burn as a pool fire. Calculations of containment pressure as a result of the pool fire were made with the SOFIRE-II code. Details of this type of calculation and the SOFIRE-II code are described in the WADCO report, "Sodium Fire Analysis for the Fast Flux Test Facility," HEDL-TME-71-31. Results for

200 and 300 ft² vent openings are shown in the table below.

ACKNOWLEDGMENT

REXCO code analyses were performed by the staff of the Reactor Analysis and Safety Division of Argonne National Laboratory, and their contribution is gratefully acknowledged.

TABLE 3-4

Containment Pressure
Vented Head Cavity

11,700 lb Spill of 1050°F Sodium

200 ft ² Vent		300 ft ² Vent	
<u>Time</u> <u>hrs</u>	<u>Pressure</u> <u>psig</u>	<u>Time</u> <u>hrs</u>	<u>Pressure</u> <u>psig</u>
.0001	.3077	.0006	.31
.50	1.3934	.05	.54
.75	1.5966	.50	1.84
1.00	1.7441	.75	2.10
2.01	2.1716	1.00	2.29
3.41	2.6320	2.01	2.83
3.94	2.1375	2.69	3.12
5.77	1.8281	3.94	2.10
8.27	1.7163	5.51	1.93
11.37	1.6018	7.07	1.84
14.46	1.4961	9.26	1.75

APPENDIX A

EXPERIMENTAL PROGRAM ON RESPONSE OF PRIMARY
BOUNDARY TO HYPOTHETICAL ACCIDENTS

To determine the response of the reactor vessel-cover structure to an excursion, it is necessary to understand the hydrodynamic and structural interaction between the liquid sodium slug that is thrown upwards and the vessel wall and cover plate, including the role played by the cover hold-down bolts. To determine the response of the primary cooling system, it is necessary to understand the interaction between the coolant and piping as the pressure pulse caused by the excursion propagates through the fluid in the piping from the reactor vessel to the pump and heat exchanger.

The information required to evaluate margins to failure afforded by the primary coolant boundary will be acquired by a series of experiments on simple and complex scale models of the reactor vessel-cover system and the primary cooling system. The simple models will be composed of the major components (vessel, cover, core barrel, core support, and piping), whereas the complex models will represent the FFTF in much more detail. The use of simple and complex models is being proposed instead of using complex models throughout, because the major effects of parameter variations in the experiments are more rapidly established with simple models, and the number of parameters required for later tests on the complex models is reduced. Water will be used as a simulant for the coolant, and a chemical energy source will be used to simulate the excursion.

Approach

The investigation will be divided into four tasks:

- Task 1: Interaction of coolant slug and vessel-cover structure.
- Task 2: Interaction of coolant and primary piping
- Task 3: Planning of the Task 4 test program.
 Design and construction of the complex models.
- Task 4: Excursion tests on complex models of the FFTF.

In Tasks 1 and 2, experiments will be carried out with simple 1/20 and 1/30 scale models to characterize the main features of the response. The results obtained from these experiments will be employed in Task 3 to optimize the Task 4 program in which the complex models will be tested. Optimization means selecting the most meaningful parameters such as coolant level, structural components wherever feasible (e.g., core barrel mass), and excursion energy release, while minimizing the number of tests; it also means the most efficient use of instrumentation channels. The purpose of the Task 4 tests is to provide evidence of the adequacy of the basic FFTF design for the containment of radioactive material and for the functioning of the primary cooling system following a hypothetical core disruptive accident. In all experiments, the liquid sodium coolant and the excursions will be simulated with water and a chemical energy source.

An outline of the contents of each task follows.

Task 1

The main effects to be studied are:

- Initial coolant slug velocity and mass
- Kinetic energy imparted to a free cover
- Strains and permanent deformations sustained by a cover and bolting system
- Deformations of a reactor vessel
- Model scale
- Energy release

Task 2

The main effects to be studied are:

- Pulse shapes entering the primary pipe from the vessel
- Pulse attenuation and pipe strain and deformation as the pulse travels along a straight pipe and a pipe with a 90-degree bend
- Model scale

Task 3

Based upon results of Tasks 1 and 2, parameters will be selected for the complex model tests.

Task 4

Tests on the complex models will be carried out to examine:

- Coolant leakage
- Damage as it affects the functioning of the primary cooling system
- Extension of the cover holddown bolts
- Strains and deformations in the cover and vessel
- Pressures in the primary piping
- Scale effects

Program

The program that will be carried out with scale models and chemical energy sources to examine the effects discussed in the previous section is described below by task. In Tasks 1 and 2, explosive charges with attenuators will be used at the outset for excursion simulation. The adequacy for excursion simulation will be determined for use in further tests.

Task 1: Interaction of coolant slug and vessel-cover structure

This task will be subdivided into three subtasks.

Task 1a: Slug velocity and mass

In the tests to determine the velocity and mass of the coolant slug that is initially thrown against the cover by an excursion within the core barrel, the model will consist of a simple flat-bottomed cylindrical vessel containing a thick steel cylinder supported by a perforated steel plate to represent the core barrel and core support structure. No cover plate will be used. The explosive charge, enclosed in an attenuator, will be located in the center of the core barrel. The vessel will contain water representing the coolant. The velocity and mass of the water slug

will be estimated by observing the motion of water (loaded with X-ray absorbing material) by means of a double-flash X-ray unit. The observations will include the shape of the free surface of the water to show how a cover would be impacted. For this technique to be effective, the cylindrical tank must be made of material that is highly transparent to X-rays. The experimental parameters will include fluid level, charge energy, and model scale; most experiments will be 1/30 scale with check experiments of 1/20 scale for scale effect studies. Damage to components will be documented, and the components replaced for subsequent testing.

Task 1b: Cover velocity

In this task a steel cover plate will be placed on the cylindrical vessel, and the velocity imparted by impact of the water slug will be observed by the double-flash X-ray technique. In this way, the momentum and kinetic energy of the cover will be determined. In some of these experiments, X-ray absorbing material will again be used in the upper part of the vessel to provide information about the hydrodynamic portion of the slug-cover interaction. The experimental parameters will include fluid level, charge energy, and model scale (as in Task 1a).

Task 1c: Strains and deformations in cover

This task will use cover plates bolted down around the periphery to a rigid annular support, structurally independent of the vessel. Scaled stretch bolts will be used. Strains and deformations of the cover and deformations of the upper vessel will be recorded. The experimental parameters will include fluid level, charge energy, and scale, as well as hold-down bolt area, cover mass, and upper vessel stiffness. A matrix of test parameters for initial scoping is given in Table A-1A and is subject to change as the program proceeds.

Task 2: Interaction of coolant and primary piping

This task will be subdivided into 4 tasks.

Primary Boundary Experimental Program Test Matrices

TABLE A-1A

		SCALE							
		1/30				1/20			
		Level				Level			
		6"		18"		6"		18"	
		<u>Bolt Area</u>		<u>Bolt Area</u>		<u>Bolt Area</u>		<u>Bolt Area</u>	
Charge	Energy	<u>A</u>	<u>B</u>	<u>A</u>	<u>B</u>	<u>A</u>	<u>B</u>	<u>A</u>	<u>B</u>
50		X		X					
150				XX	X			X	
350		X	X	XX	X	X	X	X	X
500				X	X				

TABLE A-1B

Complex Models

<u>Scale</u>	<u>Scaled Energy</u> <u>MW-sec</u>
1/30	150
1/30	350
1/30	500
1/10	350
1/10	Open

Task 2a: Pulse shape in piping

Models of Task 1c will be modified by adding scaled straight outlet pipe to the side of the vessel. The pressure pulse in this pipe outlet caused by the explosion will be monitored by a pressure transducer. This pulse will be used to choose a well-defined pulse for the experiments in Tasks 2b and 2c. The experimental parameters will include fluid level, charge size, and scale (as in Task 1).

Task 2b: Fluid-straight pipe interaction

A well-defined pressure pulse similar in character (peak pressure, duration, etc.) to the pulse obtained in Task 2a will be passed along a scaled straight pipe. The pulse shape change and pipe strain will be recorded by pressure transducers and strain gages.

Task 2c: Effect of pipe bends

This task is similar to Task 2b, but pipe bends equivalent to the reactor vessel outlet elbows will be used to ascertain the effect of the bend on the pulse.

Task 3: Planning of the Test 4 Program

Design and Construction of the Complex Models

The planning will involve the final design of five models of 1/30 and 1/10 scales, charge sizes, fluid levels, locations of strain gages on the cover, vessel, and primary piping, and locations of pressure transducers on the primary piping.

Task 4: Excursion tests on complex models of the FFTF

As currently planned, the test matrix for the complex models is shown in Table A-1B, in which the two test parameters are scale and energy. The coolant level (low or high) associated with the most damage will have been determined in Tasks 1 and 2. The high-energy excursion of 500 MW-sec is included to accentuate vulnerable features and to give an indication of the margin of safety. The models will be supported by a stiff deck, as in the FFTF, with the cover fastened to the deck by scaled hold-down bolts

and the vessel suspended from the deck. Strains, deformations, and pressures will be recorded and analyzed.

Analyses of experiments simulating reactor vessel and cover response will be performed using the ASPRIN and REXCO codes. Data from the primary loop integrity experiments will be applied to the FFTF full scale plant using the WHAM code⁽¹⁾ and the MARC II code.⁽²⁾

(1) S. Fabric, "Pressure, Velocity and Force Transients in Liquid Filled Piping Network," Kaiser Engineers No. 67-49-R, Argonne Code Center Reference Material, Abstract 278-WHAM, November 1967.

(2) P. V. Marcal, "On General Purpose Programs for Finite Element Analysis with Special Reference to Geometric and Material Non-linearities," Division of Engineering, Brown University, Providence, R.I., ARPA-E-73, May 1970.

APPENDIX B
CONTAINMENT MODEL FOR CACECO CODE

The containment model used for the CACECO code analysis was based on Bechtel drawings of May 1970. These drawings showed that the reactor cavity was sealed from the access cell, directly above it; the cavity was connected to the three HTS pipeways, adjacent to it, by pipe penetrations which appeared as large vents; but the HTS pipeways were sealed from the HTS equipment cells, adjacent to each. The volumes of the cavity and pipeways were as follows:

<u>Space</u>	<u>Gross Volume</u>	<u>Equipment Volume</u>	<u>Atmosphere Volume</u>
Reactor cavity	44,100 ft ³	22,000 ft ³	22,100 ft ³
Each HTS pipeway	3,500	200	3,300

The assumption was made that these four spaces could be represented by a single containment space in the CACECO code analysis. This assumption implies sufficient convection mixing between the cavity and pipeways that the atmospheres could be represented by the same value for temperature, pressure, or composition. The initial conditions for the atmosphere of this 32,000 ft³ containment space were taken as 100°F, 0.0 psig, 1% oxygen, and 99% nitrogen.

The structures of the reactor cavity and HTS pipeways were the roofs, walls, floors, and enclosed equipment as shown in the section view of Figure B-1. These various structures were modeled as nine heat structures for the CACECO code analysis. The nine structures and the sodium pool are shown in Figure B-2 and described below.

The insulated structures were the reactor vessel, guard tank, and tank supports in the reactor cavity, the primary piping in the cavity and pipeways, the primary valves in the pipeways, and the floor of the cavity.

The reactor vessel and guard tank were combined on the basis that the insulation on the outside of the vessel and tank limited heat transfer more than did the convection and radiation in the annulus between the

FIGURE B-1. *Section View of Reactor Cavity and HTS Pipeways*

FIGURE B-2. Heat Structures for CACECO Code Analysis

vessel and tank. The combination, called the reactor vessel, was modeled as two cylindrical heat structures with dimensions, in common, as follows.

<u>Inside Radius</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Outside Radius</u>
10.0000 ft	stagnant sodium	11.5 inches	7	10.9583 ft
10.9583	stainless steel	2.0	3	11.1250
11.1250	Kaylo* insulation	8.0	10	11.7917

*Kaylo is the trade name for rigid hydrous calcium silicate pipe insulations manufactured by the Owens-Corning Fiberglas Corporation.

The inside surface of the sodium was assumed to be heated by circulating sodium. The outside surface of the insulation was cooled (or heated) by the cavity atmosphere.

Upper Reactor Vessel 1,586 ft² outside surface area

This structure includes the reactor vessel and guard tank above the "Minimum Safe Operating Level," about 1 ft above the reactor outlet nozzles. The actual structures are about 15 ft high. The CACECO heat structure was taken as 21.4 ft high to account for the greater surface area of the actual structures.

The outside surface of the upper reactor vessel was cooled (or heated) by radiation heat transfer to the cavity roof and walls and to the sodium pool on the cavity floor. The geometrical view factors used in the radiation heat transfer analysis are listed below.

<u>View to</u>	<u>View Factor</u>
cavity roof and upper walls	0.963
heated wall at core midplane	0.024
lower wall	0.008
sodium pool on floor	0.005

Lower Reactor Vessel 3,186 ft² outside surface area

This structure includes the reactor vessel and guard tank below the "Minimum Safe Operating Level." The actual structures are about 29.8 ft.

high. The CACECO heat structure was taken as 43.0 ft high to account for the greater surface area of the actual structures.

The outside surface of the lower reactor vessel was cooled (or heated) by radiation heat transfer to the cavity roof and walls and to the sodium pool on the cavity floor. The geometrical view factors used in the radiation heat transfer analysis are listed below.

<u>View to</u>	<u>View Factor</u>
cavity roof and upper walls	0.271
heated wall at core midplane	0.258
lower wall	0.422
sodium pool on floor	0.050

Guard Tank Supports 1,420 ft² outside surface area

The several leg supports of the reactor guard tank were combined into one cylindrical heat structure. The dimensions are as follows.

<u>Inside Radius</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Outside Radius</u>
1.4300 ft	stainless steel	1.0 inches	3	1.5134 ft
1.5134	Kaylo insulation	3.0	3	1.7634

This single CACECO heat structure was taken as 128.2 ft long to account for the surface area of the several supports. The inside surface of this structure was assumed to be heated by circulating sodium so as to simulate the heat conduction down the tank supports. The outside surface was cooled by the cavity atmosphere.

<u>View to</u>	<u>View Factor</u>
cavity lower wall	0.964
sodium pool on floor	0.036

Primary Piping 1,648 ft² outside surface area

The 16-in. diameter piping and valves and the 28-in. diameter piping and valves were combined into one cylindrical heat structure. The dimensions of this structure are as follows:

<u>Inside Radius</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Outside Radius</u>
0.9119 ft	stainless steel	0.5 inches	3	0.9536 ft
0.9536	Kaylo insulation	7.0	10	1.5345

This single CACECO heat structure was taken as 170.9 ft long to account for the surface area of the actual piping and valves. The inside surface of this structure was assumed to be heated by circulating sodium. The outside surface was cooled by the cavity atmosphere.

The containment model was simplified by assuming that all of the primary piping was inside the HTS pipeways instead of being distributed between the cavity and pipeways. This assumption permitted the piping and valves to be cooled by radiation heat transfer to the HTS pipeways' roofs, walls, and floors. The geometrical view factor was as follows.

<u>View to</u>	<u>View Factor</u>
HTS pipeways' roofs, walls, and floors	1.000

The circulating sodium mentioned as heating the upper and lower reactor vessel, the tank supports, and the primary piping is the sodium that would continue to circulate through the primary sodium system by forced or natural convection following the sodium release accident and reactor shutdown. The initial, average temperature of this sodium was taken to be 900°F. Following the sodium release, this circulating sodium was assumed to cool at about 33°F/min to 500°F at 12 minutes and, thereafter, stay at the shutdown cooling temperature of 500°F. The circulating sodium removed the sensible heat of the sodium and stainless steel of the reactor vessel, supports, piping, and valves and the 500°F shutdown temperature reduced the heat losses through the insulation into the cavity atmosphere.

Cavity Floor 1,000 ft² surface area

This structure, called the cavity floor, includes both the floor and the adjacent wall, about 2.4-in. height, which was submerged by the sodium pool. Previous studies had shown that the hot sodium pool would

heat the steel liner on the cavity floor above its design temperature unless the liner was protected by insulation. Preliminary analysis suggested that this insulation should be an insulating firebrick composed of 57.7% SiO_2 , 36.8% Al_2O_3 , and small or fractional percentages of Fe_2O_3 , TiO_2 , CaO , and MgO so that the fusion point was about 2980-3000°F. An ordinary 9-in. brick of this firebrick weighs only about 2.25 lbs. The thermal conductivity of this light weight firebrick was about 0.167 Btu/hr-ft-°F and the heat capacity about 0.22 Btu/lb. The preliminary analysis also suggested that this insulation should be three bricks thick or about 13.5-in. thick.

The floor liner was assumed not to touch the concrete. That is, the actual installation described by Carroll* wherein the liner was attached to the concrete at various locations and separated from the concrete in-between these locations was represented by the analysis model having the liner separated from the concrete by a uniform gap filled with nitrogen. This gap was taken to be 1/16-in. wide. Such a gap is too thin to permit convection heat transfer between the liner and concrete. The heat transfer across the gap would be by conduction and radiation and the radiation heat transfer was assumed to be negligible.

The dimensions of the slab heat structure representing the cavity floor and submerged walls are as follows:

<u>Linear Distance</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Linear Distance</u>
0.0000 ft	insulating firebrick	13.5 inches	14	1.1250 ft
1.1250	carbon steel	0.25	3	1.14583
1.14583	nitrogen	0.0625	3	1.15104
1.15104	concrete	42.0	14	4.65104

Initially, the top surface of the firebrick was heated by the cavity atmosphere by a natural convection heat transfer coefficient of 1.0 Btu/hr-ft²-°F. The sodium spray of the accident, that was not consumed by the oxygen

* P. E. Carroll, letter, "FFTF Job 5853, Plant Design Analysis for a Core Disruptive Accident, BCL-992," BC-70580, May 4, 1970, Appendix D by H. S. Tsang, Sheet 13, Bechtel Corporation.

in the atmosphere or evaporated into the atmosphere, fell to the floor to form a pool and heat the floor. The pool heated the top surface of the firebrick with a natural convection heat transfer coefficient of 240 Btu/hr-ft²-°F. The bottom surface of the concrete was assumed to be in contact with the ground as represented by a heat sink of 60 °F.

Initially, the top surface of the insulation on the floor was heated and, later when the sodium pool was formed, the top surface of the pool was cooled by radiation heat transfer to the cavity roof and walls and to the reactor vessel, guard tank, and tank supports. The geometrical view factors used in the radiation heat transfer analysis are listed below.

<u>View to</u>	<u>View Factor</u>
cavity roof and upper walls	0.109
heated wall at core midplane	0.053
lower wall	0.532
upper reactor vessel	0.008
lower reactor vessel	0.206
guard tank supports	0.091

In the heat structures representing the cavity roof and walls and the HTS pipeways' roofs, walls, and floors, the steel liners were assumed not to touch the concrete. That is, the actual installation described by Carroll wherein the liner was attached to the concrete at various locations and separated in between these locations was represented for analysis by a model having the liner separated from the concrete by a uniform 0.25-in. gap of nitrogen. This gap is too thin to permit convection heat transfer between the liner and concrete. The heat transfer across the gap would be by conduction and radiation and the radiation heat transfer was assumed to be negligible.

Cavity Roof and Upper Walls 2,770 ft² surface area.

The dimensions of the slab heat structure representing the cavity roof and upper walls are as follows.

<u>Linear Distance</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Linear Distance</u>
0.0000 ft	carbon steel	0.25 inches	3	0.02083 ft
0.02083	nitrogen	0.25	3	0.04167
0.04167	concrete	35.	14	2.95833

The outside surface of the steel liner was cooled (or heated) by the cavity atmosphere. The outside surface of the concrete was assumed to be insulated as if the 35-in. depth of concrete was in the center of the actual roof and wall.

The surface of the steel liner was heated by radiation heat transfer from the surfaces of the reactor vessel, guard tank, and sodium pool. The geometrical view factors used in the radiation heat transfer analysis are listed below.

<u>View to</u>	<u>View Factor</u>
cavity roof and upper wall	0.258
heated wall at core midplane	0.029
lower wall	0.029
upper reactor vessel	0.402
lower reactor vessel	0.255
sodium pool on floor	0.025

Heated Wall 820 ft² surface area.

The nuclear radiation from the reactor vessel was distributed and absorbed in the roof, walls, and floor of the reactor cavity. Preliminary estimates of this nuclear radiation indicated that at the core midplane during normal operation, the energy flux into the wall due to Na²⁴ decay gammas was about 0.001 w/cm² and the flux due to neutrons and to fission product and capture gammas was about 0.024 w/cm². The total energy flux into the wall at the core midplane was 0.025 w/cm² = 79 But/hr-ft². The energy flux integrated over the roof, walls and floor was about 19 kW = 64,900 Btu/hr. The heated wall was an analytical device to combine the

radiation energy flux at the core midplane and the total radiation energy into the roof, walls, and floor of the cavity. That is, the heated wall had volumetric heat sources within it equivalent to the distribution of nuclear heating due to the energy flux at the core midplane and the integration of these volumetric heat sources was about 64,900 Btu/hr at the initial, steady state conditions representing normal operation at 400 MW.

The dimensions of the slab heat structure representing the heated wall are as follows:

<u>Linear Distance</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Linear Distance</u>
0.0000 ft	carbon steel	0.25 inches	3	0.02083 ft
0.02083	nitrogen	0.025	3	0.04167
0.04167	concrete	63.	21	5.2500

In the CACECO code, the volumetric heat sources are specified for the spaces between mesh points rather than at the mesh points themselves. These heat sources were calculated using the equations, $H = H_0 \text{Exp}(-0.163 x)$ for steel and $H = H_0 \text{Exp}(-0.050 x)$ for concrete where H_0 is the volumetric heat source at the surface of the material and x is the distance into the material. The results of these calculations are illustrated below by 4 of the 27 heat source values used by the code for the heated wall.

<u>Linear Distance into Material</u>	<u>Material</u>	<u>Volumetric Heat Source Value (Btu/hr-ft³)</u>
0.125 inches	carbon steel	379
0.5	concrete	324
6.5	concrete	129
33.0	concrete	2.3

The outside surface of the steel liner was cooled (or heated) by the cavity atmosphere. The outside surface of the concrete was assumed to be cooled by the atmosphere in an adjacent pipeway or equipment cell which remained steady at 100 °F.

The surface of the steel liner was also cooled (or heated) by radiation heat transfer to the surfaces of the reactor vessel, cavity roof, walls and sodium pool. The geometrical view factors used in the radiation heat transfer analysis are listed below.

<u>View to</u>	<u>View Factor</u>
cavity roof and upper wall	0.087
heated wall at core midplane	0.069
lower wall	0.055
upper reactor vessel	0.030
lower reactor vessel	0.722
sodium pool on floor	0.036

Cavity Lower Wall 1,440 ft² surface area.

The dimensions of the slab heat structure representing the cavity lower wall are as follows:

<u>Linear Distance</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Linear Distance</u>
0.0000 ft	carbon steel	0.25 inches	3	0.02083 ft
0.02083	nitrogen	0.25	3	0.04167
0.04167	concrete	63.	21	5.2500

The outside surface of the steel liner was cooled (or heated) by the cavity atmosphere. The outside surface of the concrete was assumed to be cooled by the 100 °F atmosphere in an adjacent pipeway or equipment cell.

The surface of the steel liner was also cooled (or heated) by radiation heat transfer to the surfaces of the reactor vessel, guard tank supports, cavity roof, walls, and sodium pool. The geometrical view factors used in the radiation heat transfer analysis are listed below.

<u>View to</u>	<u>View Factor</u>
cavity roof and upper walls	0.025
heated wall at core midplane	0.016
lower wall, itself	0.049
upper reactor vessel	0.003
lower reactor vessel	0.335
guard tank supports	0.469
sodium pool on floor	0.103

HTS Pipeways' Roofs, Walls, and Floors 3,000 ft² total surface area.

The three HTS pipeways have roofs, walls, and floors in contact with the cavity-pipeway atmosphere. The dimensions of the slab heat structure representing the HTS pipeways' roofs, walls, and floors are as follows.

<u>Linear Distance</u>	<u>Material</u>	<u>Thickness</u>	<u>Mesh Point Spaces</u>	<u>Linear Distance</u>
0.0000 ft	carbon steel	0.25 inches	3	0.02083 ft
0.02083	nitrogen	0.25	3	0.04167
0.04167	concrete	35.	14	2.95833

The outside surface of the steel liner was cooled (or heated) by the cavity-pipeway atmosphere. The outside surface of the concrete was assumed to be insulated as if the 35-in. depth of concrete were in the center of an actual roof, wall, or floor.

The surface of the steel liner was also heated by radiation heat transfer from the primary piping and valves. As noted earlier, the analysis model was simplified by assuming that all of the piping was in the pipeways. The geometrical view factors used in the radiation heat transfer analysis are listed below.

<u>View to</u>	<u>View Factor</u>
primary piping and valves	0.549
HTS pipeways' roofs, walls and floors, themselves	0.451

Radiation heat transfer depends upon the surface areas, geometrical view factors, emissivities, and temperatures of the surfaces involved. In the CACECO code, radiation heat transfer calculations are based upon the assumption of grey surfaces which have constant values of emissivities. The input data required by the code are area-view-emissivity factors which, in this analysis, were calculated by using an equation from McAdams*. The equation is based on a two-zone assumption for source-sink surfaces. An authoritative source of emissivity values for various material surfaces is the review by Gubareff, et al **. The emissivity values selected were based on the assumption that the sodium pool surface and the surfaces of the steel liners and Kaylo insulation covers were all oxidized. The emissivity values are listed below.

<u>Surface</u>	<u>Emissivity</u>
sodium pool	0.85
carbon steel liners	0.80
aluminum insulation covering	0.80

* W.H. McAdams, Heat Transmission, Third Edition, McGraw-Hill Book Company, Inc., New York 1954, p 76, Equation 4-33.

**G.G. Gubareff, Shao-Yen Ko, and P.E. McNall, Jr., Review of the Thermal Radiation Properties Values for Metals and Other Materials, Minneapolis-Honeywell Regulator Co., Hopkins, Minnesota, Third Printing dated September 1958.

The area-view-emissivity factors used in the CACECO code caluations are listed below.

AREA-View-Emissivity Factors

<u>Surfaces</u>	<u>Cavity Roof & U.Wall</u>	<u>Heated Wall</u>	<u>Cavity Lower Wall</u>	<u>Upper Reactor Vessel</u>	<u>Lower Reactor Vessel</u>	<u>Guard Tank Supports</u>	<u>Sodium Pool</u>
Cavity roof and upper walls	88.2	49.6	58.1	201.0	233.0	0.0	61.0
Heated wall	49.6	44.8	34.0	20.8	128.0	0.0	23.8
Lower wall	58.1	34.0	103.0	8.26	198.0	158.0	100.0
Upper R. vessel	201.0	20.8	8.26	0.0	0.0	0.0	4.66
Lower R. vessel	233.0	128.0	198.0	0.0	0.0	0.0	72.9
Guard T. supports	0.0	0.0	158.0	0.0	0.0	0.0	38.4
Sodium pool	61.0	23.8	100.0	4.66	72.9	38.4	0.0

APPENDIX C

Effects of 10,000 to 100,000 lb Sodium Releases on Reactor Cavity Pressure

The maximum temperature and pressure conditions in the reactor cavity and HTS pipeways were examined in Cases 260, 261, and 262 for sodium releases of 10,000 lbs, 31,623 lbs, and 100,000 lbs respectively. The containment model for these three cases differed from that already described in section III of this report because of the emphasis on the early portion of the containment transient. In particular, the reactor cavity and the three HTS pipeways were modeled as two containment spaces for the CACECO code analysis. The atmosphere volumes were as follows.

<u>Cases 260-262</u> <u>Containment Space</u>	<u>Atmosphere</u> <u>Volume</u>
Reactor cavity	22,100 ft ³
Three HTS pipeways	10,500

The cavity and pipeways were assumed to be connected by vent passages at the pipe penetrations through the cavity-pipeway walls. The analysis assumed that there was a nominal 1 inch gap between the insulation of the primary piping and the liner of the pipe penetration so that there was about 0.49 ft² vent area into each pipeway or a total of 1.4 ft² vent area out of the cavity. The initial conditions for the atmospheres of the cavity and pipeways were taken as 100°F, 0.0 psig, 1% oxygen, and 99% nitrogen.

The roofs, walls, floors, and enclosed equipment of the reactor cavity and HTS pipeways were modeled in five heat structures described, briefly, below.

Cavity Roof and Walls

The emphasis on the early portion of the containment transient meant that the conduction and radiation heat transfer from the steel liner into the concrete could be neglected. Thus, one heat structure of 0.25-in. thick carbon steel was used to represent the 5,360 ft² of steel liner.

Reactor Vessel, Guard Tank, and Cavity Piping

One heat structure of 8.0-in. thick Kaylo insulation was used to represent the reactor vessel, guard tank, tank supports, and cavity piping. The outside surface area of these components was taken as 5,150 ft².

Cavity Floor

One heat structure of 0.50-in. thick carbon steel was used to represent the 1,040 ft² to 1,234 ft² of cavity floor and submerged walls. The 10,000 lb sodium pool would cover about 22 ft² of wall and the 100,000 lb pool about 216 ft².

HTS Pipeways' Roofs, Walls, and Floors

One heat structure of 0.25-in. thick carbon steel was used to represent the 12,570 ft² of steel liner in the three pipeways.

HTS Pipeways' Piping and Valves

One heat structure of 7.66-in. thick Kaylo insulation was used to represent the primary piping and isolation valves in the three pipeways. The outside surface area of these components was taken as 1,370 ft².

Radiation heat transfer was included between the cavity roof and walls, the reactor vessel, and the sodium pool on the floor.

The sodium releases were injected into the cavity atmosphere. The initial release rates and the duration of the releases are listed below.

<u>Case</u>	<u>Total Sodium Release</u>	<u>Initial Sodium Release Rate</u>	<u>Duration of Sodium Release</u>
260	10,000 lbs	18,518 lb/sec	1.08 sec
261	31,623	32,931	1.92
262	100,000	58,561	3.42

The internal energy of the sodium was that of liquid sodium at 1050°F.

The atmosphere temperatures and pressures following the 10,000 lb release into the cavity, Case 260, are shown in Figures C-1 and C-2, respectively, and following the 100,000 lb release, Case 262, in Figures

C-3

FIGURE C-1. 10,000 lb Spray of Sodium at 1050°F into Reactor Cavity - Case 260

FIGURE C-2. 10,000 lb Spray of Sodium at 1050°F into Reactor Cavity - Case 260

FIGURE C-3. 100,000 lb Spray of Sodium at 1050°F into Reactor Cavity - Case 262

FIGURE C-4. 100,000 lb Spray of Sodium at 1050°F into Reactor Cavity - Case 262

C-3 and C-4, respectively. Figures C-1 and C-3 are similar as are Figures C-2 and C-4. The releases heat the cavity atmosphere to about the same maximum, 1039°F to 1049°F and the attendant pressure causes the cavity atmosphere to vent into the three pipeways. In the CACECO analysis, the atmosphere vent flow carries hot nitrogen and sodium vapor but no entrained sodium spray. None the less, the vent flow heats the pipeways' atmospheres by enthalpy and chemical reaction effects. The cavity and pipeways attain pressure equalization in about 5 seconds and, thereafter, because the cavity cools relatively more quickly than the pipeways, the pipeways' atmospheres vent back into the cavity.

The results for Cases 260 to 262 are compared to those of Case 46 in Table C-1. The maximum cavity temperature and pressure conditions are about the same for these four cases. However, the conditions at 10 seconds are different, the conditions for Case 46 being higher than for Cases 260 to 262. In Case 46, the atmospheres of the cavity and pipeways were assumed to be well mixed by vent and convection flows so that these atmospheres could be modeled as a single containment space. In Cases 260 to 262, the atmospheres of the cavity and pipeways were assumed to be separate although vent flow between the two containment spaces did provide a degree of mixing. The cavity atmosphere cooled more quickly in Cases 260 to 262 than in Case 46 because: the vent flow into the pipeways reduced the weight of atmosphere in contact with the cavity surfaces, the vent flow carried enthalpy energy (internal energy plus the energy of pressure times vent flow) out of the cavity, and the subsequent vent flow into the cavity brought cooler gases back from the pipeways.

TABLE C-1
COMPARISON OF CONTAINMENT TRANSIENTS
FOLLOWING 10,000 to 100,000 POUND SODIUM RELEASE

Topic	Combined R. Cavity and HTS Pipeways, Case 46	Reactor Cavity Case 260	Separate Cavity and HTS Case 261	Pipeways Case 263
Sodium release, lbs	10,000	10,000	31,623	100,000
Conditions at Maximum Pressure in Cavity				
Time, sec	0.90	0.35	0.30	0.45
Atm. temperature, °F	1,037	1,039	1,046	1,049
Pressure, psig	24.7	24.1	24.5	24.9
Oxygen content, %	0.000	0.000	0.000	0.000
Conditions at Maximum Pressure in Pipeways				
Time, sec	same	4.60	4.65	5.00
Atm. temperature, °F	as above	396	423	469
Pressure, psig		14.6	15.8	18.5
Oxygen content, %		0.746	0.729	0.694
Conditions at 10 Seconds After Outset of Release				
Cavity atmosphere Temperature, °F	854	676	707	757
Pressure, psig	19.70	11.46	12.48	14.80
Oxygen content, %	0.000	0.746	0.729	0.694
Conditions at 10 Seconds After Outset of Release				
Cavity atmosphere Temperature, °F	854	676	707	757
Pressure, psig	19.70	11.46	12.48	14.80
Oxygen content, %	0.000	0.004	0.000	0.000
Pipeways' atmosphere				
Temperature, °F	854	338	358	398
Pressure, psig	19.70	11.53	12.53	14.86
Oxygen content, %	0.000	0.745	0.729	0.693

APPENDIX D

BASIC EQUATIONS OF WAVE MOTION

The equations describing a one dimensional solution for the dynamic behavior in a fluid contained in a pipe that undergoes plastic deformation are presented in this appendix. Some of the pertinent assumptions used in this derivation are that the radial inertia of the pipe is neglected, that the radial variations in fluid motions are neglected, that the gravitational forces are neglected, that the original dimension of the pipe are uniform, and that the fluid column remains continuous.

The position of a fluid particle is described by Lagrangian coordinates $x(t)$, and the displacement function $u(t) = x(t) - x(0)$. The axial equilibrium of the fluid column including inertial effects is described by the expression:

$$\rho \ddot{x} + \frac{\partial P}{\partial x} = 0 \quad (A-1)$$

where P = fluid pressure
 ρ = fluid density

A unique relation between the pressure and the fluid column strain is assumed to exist; thus,

$$\frac{\partial P}{\partial x} = \frac{dP}{d\epsilon} = \frac{\partial \epsilon}{\partial x} = \frac{dP}{d\epsilon} \frac{\partial^2 u}{\partial x^2} \quad (A-2)$$

where $\epsilon = \frac{\partial u}{\partial x}$ is the true strain of the fluid column.

An increment of fluid volume Δv is decreased dv by an increment in pressure

$$dv = \left(\frac{-dP}{K} \right) \Delta v = \left(\frac{-dP}{K} \right) \pi R^2 \Delta x, \quad (A-3)$$

where R = inner radius of pipe
 K = compressibility of fluid

The increment in pressure also causes an incremental change in the pipe radius dR . For a material obeying a Tresca yield criteria (or a

free ended pipe obeying a von Mises yield criteria), the radial increment is given by

$$dR/R = d \left(\frac{R^P}{H} \right) / E_p \quad (A-4)$$

where H = tube thickness

E_p = tangent modulus to tensile true stress-strain curve

$\frac{R^P}{H} = R^P/H$ = instantaneous circumferential membrane stress in pipe

Material volume remains constant during plastic deformation and for a Tresca yield condition the circumferential strain is the negative of the radial strain ($dR/R = -dH/H$). Equation (A-4) is solved for the relation between the increment in circumferential strain and pressure increment.

$$\frac{dR}{R} = \frac{R}{H} \left[\frac{dP}{(E_p - 2\sigma)} \right] \quad (A-5)$$

Instantaneous values of the pipe radius and pipe thickness are related to the circumferential strain in the pipe, $\bar{\epsilon}$, and the initial dimensions R_0 and H_0 by the relations

$$R = R_0 \exp(\bar{\epsilon}),$$

$$H = H_0 \exp(-\bar{\epsilon}) \quad (A-6)$$

The fluid is assumed to remain in contact with the pipe, thus both the change of the fluid column length, dx , and the increase of the pipe dimension, dR , can be related to the change of fluid volume to yield the mass conservation relation

$$dv = \pi \Delta X \quad 2RdR + \pi R^2 dx = \left(\frac{-dP}{K} \right) \pi R^2 x \quad (A-7)$$

Since $(dx/\Delta x) = d\epsilon$, eqs. (A-3), (A-5), and (A-7), can be solved to yield

$$\frac{dP}{d\epsilon} = - \left(\frac{1}{K} + \frac{2R}{H(E_p - 2\sigma)} \right)^{-1} \quad (A-8)$$

Substitution of eqs. (A-8) and (A-2) into (A-1) yields the resultant wave equation

$$\frac{\partial^2 u}{\partial t^2} = c_p^2 \frac{\partial^2 u}{\partial x^2}, \quad (\text{A-9})$$

where the wave velocity c_p is

$$c_p^2 = (1/\rho) \left(\frac{1}{K} + \left(\frac{2R}{H(E_p - 2\sigma)} \right)^{-1} \right) \quad (\text{A-10})$$

A particular solution is obtained for eq. (A-9) if $x(t) = tc_p$. The c_p is assumed to be a unique function of ϵ in the fluid which can be inverted to yield

$$\epsilon = f(c_p) = f(x/t) \quad (\text{A-11})$$

This relation can be used to solve for the fluid displacements u

$$u(x) = \int_{-\infty}^x \frac{\partial u}{\partial x} dx = t \int_{-\infty}^{(x/t)} f(c) dc \quad (\text{A-12})$$

The $u(x)$ from eq. (A-12) identically satisfies eq. (A-9). The fluid velocity is given by

$$\frac{\partial u}{\partial t} = \int_{-\infty}^{(x/t)} f(c) dc - \left(\frac{x}{t} \right) f(x/t)$$

which for $x = 0$

$$v = \frac{\partial u}{\partial t} \Big|_{x=0} = \int_{-\infty}^0 \epsilon(c) dc = \int_0^{(-\epsilon)_{\max}} c_p d(-\epsilon) \quad (\text{A-13})$$

The prescribed solution applies for either a constant pressure or constant velocity imposed at $x(t) = 0$. A constant maximum strain or constant maximum pressure would correspond to a $(c_p)_{\max}$ and the resultant

conditions in the fluid column are

$$\begin{aligned}
 (-\varepsilon)_{\max} & \quad 0 \leq x \leq C_{p\max} t \\
 \varepsilon = f(C_p) & \quad C_{p\max} t \leq x \leq C_\varepsilon t \\
 \varepsilon = 0 & \quad x \leq C_\varepsilon t
 \end{aligned} \quad (A-14)$$

where C_ε corresponds to elastic pipe and fluid behavior. The mass conservation relationship (A-7), expressed in terms of total strains is

$$\begin{aligned}
 \log (v/v_0) &= - \int_0^P \frac{dP}{K} = \frac{-P}{K} = \ln \frac{x}{x_0} + 2 \ln \frac{R}{R_0} \quad (A-15) \\
 &= \varepsilon + 2 \bar{\varepsilon},
 \end{aligned}$$

where $\bar{\varepsilon}$ = circumferential strain in the pipe. Equation (A-15) defines the relation between the pressure in the fluid, strain of the fluid column and strain in the pipe. For a given strain on the pipe, the instantaneous pipe thickness and radius can be determined with equation (A-6), fluid pressure with (A-4), and fluid strain with equation (A-15).

The preceding relations are sufficient to evaluate the conditions in a pipe filled with fluid subjected to a constant pressure or a constant fluid velocity at $x = 0$. The numerical evaluations based on the preceding relations and the material property data are performed by a computer program described in a WADCO Corporation report.* The data reported in Section C of the primary boundary analysis was obtained with the foregoing code.

* GL Fox, KR Merckx, Plastic Wave Transmission in a Fluid Contained in a Plastically Determining Pipe.

NOMENCLATURE

x	=	fluid column length in Lagrangian coordinates
u	=	displacement of fluid particle ($X(t) - X(0)$)
R	=	instantaneous inner pipe radius
H	=	instantaneous inner pipe thickness
ϵ	=	true strain in fluid column
$(\cdot)$	=	material derivate with respect to time
$\underline{P}$	=	pressure in fluid
C_p	=	wave velocity in fluid
K	=	fluid compressibility
E_p	=	Tangent modulus of true stress strain curve
V	=	fluid particle velocity
ρ	=	fluid density
Δv	=	incremental volume in the fluid column
$\bar{\epsilon}$	=	true circumferential strain in the pipe
$\bar{\sigma}$	=	true circumferential stress in the pipe

APPENDIX E

COMPUTATION OF LOADS ON VESSEL SUPPORT STRUCTURE

The analysis of the forces and strains in the vessel wall and supports is based on an oscillating spring-mass system with two degrees of freedom. The springs, however, are not simple elastic members but are modeled by elastic-plastic structures with strain hardening characteristics. The physical system is shown in Figure 1, part "a" and the analytical representation is shown in part "b". Since the vessel support is slotted, member A is modeled as a uniaxial structure whereas the vessel wall is continuous and undergoing circumferential strains and therefore must be modeled as a biaxial stress-strain system. The force balance on the wall mass is as follows:

Applying Newtons Law

$$\sum_i F_i = M_w \ddot{X}_w$$

$$F_B - F_A + M_w g_c = M_w \ddot{X}_w \quad (1)$$

F_A = Force in A = support member

$$= (\sigma_z)_A (\text{Area})_A$$

F_B = Force in B = force in wall

$$= (\sigma_z)_B (\text{Area})_B$$

FIGURE E-1. *Vessel Support Structure*

σ_z = Axial stress

M_w = Mass of wall

$\ddot{X}_w$ = Acceleration in X direction

g_c = Gravitational constant

The force balance on the element which represents the core support and other lower vessel masses is:

Applying Newtons Law

$$\sum_i F_i = M_c \ddot{X}_c$$

$$F_c(t) - F_B + M_c g_c = M_c \ddot{X}_c \quad (2)$$

$F_c(t)$ = Force history on core support

M_c = Mass of lower vessel

$\ddot{X}_c$ = Acceleration in X direction of
lower vessel

Once the axial stresses are determined, it becomes a simple matter to determine forces F_A and F_B , integrate Equations (1) and (2) twice to obtain velocities, displacements and strains. The remainder of this

treatment will be concerned with developing the plasticity equations for a work hardening material⁽¹⁾. The equations for a biaxial stress system will be developed for member B which may be simplified for application to uniaxial member A.

The analysis begins by specifying the input to the plasticity equations. First, the axial strain rate is known from the integration of Equations (1) and (2).

$$\dot{\epsilon}_z = \frac{\dot{\chi}_c - \dot{\chi}_w}{\chi_B} \quad (3)$$

$\dot{\epsilon}_z$ = Axial strain rate

$\dot{\chi}_c$ = Velocity of mass c

$\dot{\chi}_w$ = Velocity of mass w

χ_B = Length of member B

(1) B. A. Boley and J. G. Weiner "Theory of Thermal Stress",
Section 14.6 Third Printing 1966, John Wiley and Sons, Inc.

Next, the maximum circumferential strain rate is input from the REXCO calculations. The average strain rate is obtained by integrating over a parabolic shaped deformation pattern and found to be 2/3 the strain rate at the point of maximum deformation.

$$\dot{\epsilon}_{\theta} = \frac{2}{3} \frac{\dot{R}}{R_0} \quad (4)$$

$\dot{\epsilon}_{\theta}$ = Circumferential strain rate (Average)

$\dot{R}$ = Vessel wall radial velocity

R_0 = Vessel wall initial radius

The VonMises yield criteria is applied to a biaxial condition, i.e., stress in radial direction is negligible.

$$\bar{\sigma} = \left[\sigma_{\theta}^2 + \sigma_z^2 - \sigma_{\theta} \sigma_z \right]^{1/2} = \left[\frac{3}{2} \left(s_{\theta}^2 + s_z^2 + s_r^2 \right) \right]^{1/2} \quad (5)$$

$$\bar{\epsilon} = \left[\frac{2}{3} \left(\epsilon_{\theta}^2 + \epsilon_z^2 + \epsilon_r^2 \right) \right]^{1/2}$$

$\bar{\sigma}$ = Effective stress

σ_{θ} = Circumferential stress

σ_z = Longitudinal stress

Deviator stresses

$$S_{\theta} = \frac{2 \sigma_{\theta} - \sigma_z}{3}$$

$$S_z = \frac{2 \sigma_z - \sigma_{\theta}}{3}$$

$$S_r = - \left(\frac{\sigma_{\theta} + \sigma_z}{3} \right)$$

$\bar{\epsilon}$ = Effective strain

ϵ_B = Circumferential strain

ϵ_z = Longitudinal strain

ϵ_r = Radial strain

The stress-strain relation is of the form:

$$\dot{\epsilon}_i^P = (1 - g) \gamma S_i \quad (6)$$

where i represents
coordinates γ, θ, z

$\dot{\epsilon}_i^P$ = Plastic strain rate in "i" direction

γ = Parameter to be evaluated

S_i = Deviator stress in "i" direction

g = 0 for plastic behavior,

if $\bar{\sigma} \frac{\dot{\sigma}}{\sigma} \geq 0$ and $\bar{\sigma} = Y_F$

g = 1 for elastic behavior

if $\bar{\sigma} \frac{\dot{\sigma}}{\sigma} < 0$ and $\bar{\sigma} = Y_F$, or $\bar{\sigma} < Y_F$

Y_F = Instantaneous yield stress

The bilinear form of stress-strain curve is used.

The plastic modulus E_p is defined as follows:

$$E_p = \frac{d\bar{\sigma}}{d\bar{\epsilon}_p} = \frac{E_1 E_2}{E_1 - E_2} \quad (7)$$

Where E_1 and E_2 is slope of elastic and plastic segments of the stress-strain curve.

Squaring and summing Equation (6) produces:

$$\frac{3}{2} \sum \dot{\epsilon}_i^2 = \gamma^2 \dot{\bar{\sigma}}^2 = \dot{\bar{\epsilon}}_p^2 \quad (8)$$

Then from Equations (7) and (8)

$$\gamma = \frac{\dot{\bar{\epsilon}}_p}{\dot{\bar{\sigma}}} = \frac{\dot{\bar{\sigma}}}{E_p \dot{\bar{\sigma}}} = \frac{\left(\sigma_\theta - \frac{\sigma_z}{2}\right) \dot{\sigma}_\theta + \left(\sigma_z - \frac{\sigma_\theta}{2}\right) \dot{\sigma}_z}{E_p \dot{\bar{\sigma}}^2} \quad (9)$$

or in terms of deviator stresses:

$$\gamma = \frac{S_\theta \dot{\sigma}_\theta + S_z \dot{\sigma}_z}{\frac{2}{3} E_p \dot{\bar{\sigma}}^2} \quad (10)$$

The elastic portion of the strains

$$\dot{\epsilon}_{\theta}^E = \frac{1}{E_1} (\dot{\sigma}_{\theta} - \nu \dot{\sigma}_z) \quad (11)$$

$$\dot{\epsilon}_z^E = \frac{1}{E_1} (\dot{\sigma}_z - \nu \dot{\sigma}_{\theta})$$

ν = Poissons ratio

The plastic portion of the strains

$$\dot{\epsilon}_{\theta}^P = (1 - g) \gamma S_{\theta} \quad (12)$$

$$\dot{\epsilon}_z^P = (1 - g) \gamma S_z$$

Since the total strain is the sum of elastic and plastic strains

$$\dot{\epsilon}_{\theta} = \left\{ \frac{1}{E_1} + (1 - g) \frac{S_{\theta}^2}{\frac{2}{3} E_p \bar{\sigma}} \right\} \dot{\sigma}_{\theta} + \left\{ \frac{-\nu}{E_1} + (1 - g) \frac{S_{\theta} S_z}{\frac{2}{3} E_p \bar{\sigma}} \right\} \dot{\sigma}_z \quad (13)$$

$$\dot{\epsilon}_z = \left\{ \frac{-\nu}{E_1} + (1 - g) \frac{S_{\theta} S_z}{\frac{2}{3} E_p \bar{\sigma}} \right\} \dot{\sigma}_{\theta} + \left\{ \frac{1}{E_1} + (1 - g) \frac{S_z^2}{\frac{2}{3} E_p \bar{\sigma}} \right\} \dot{\sigma}_z \quad (14)$$

Define

$$C_{11} = \frac{1}{E_1} + (1 - g) \frac{S_{\theta}^2}{\frac{2}{3} E_p \bar{\sigma}}$$

$$C_{12} = C_{21} = \frac{-\nu}{E_1} + (1 - g) \frac{S_\theta S_z}{\frac{2}{3} E_p \bar{\sigma}^2}$$

$$C_{22} = \frac{1}{E_1} + (1 - g) \frac{S_z^2}{\frac{2}{3} E_p \bar{\sigma}^2}$$

Δ = Determinant of coefficients

$$= C_{11} C_{22} - C_{12}^2$$

$$= \frac{1 - \nu^2}{E_1^2} + \frac{1-g}{\frac{2}{3} E_1 E_p} \frac{S_\theta^2 + S_z^2 + 2\nu S_\theta S_z}{\bar{\sigma}^2}$$

Solving Equations (13) and (14) in terms of $\dot{\epsilon}_z$ and $\dot{\epsilon}_\theta$ produces

$$\dot{\sigma}_\theta = (C_{22} \dot{\epsilon}_\theta - C_{12} \dot{\epsilon}) / \Delta \quad (15)$$

$$\dot{\sigma}_z = (C_{11} \dot{\epsilon}_z - C_{12} \dot{\epsilon}_\theta) / \Delta \quad (16)$$

Equations (15) and (16) are integrated to obtain both axial and circumferential stress to be used in determining the forces in the vessel model. They are also used to evaluate the equivalent stress in Equation (5) which is compared with the yield function, $\gamma_F(\bar{\epsilon})$. This completes the derivation for the response of the vessel support system. These equations are programmed and available to potential users.

APPENDIX F

PRESSURE - VOLUME SODIUM EXPANSION INPUT TO REXCO

The REXCO-H code, which was used to determine the dynamic structural response of the primary containment boundary, requires characterization of the heating of the sodium in the core by the molten fuel and the subsequent expansion. Although a model for the thermal interaction of the sodium and molten fuel will eventually be incorporated into REXCO-H, the present study utilized sodium expansion data calculated by the SOCCOL-II code⁽¹⁾.

The pressure-volume sodium expansion data used for input to REXCO-H is shown in Table A-1. The 150 MW-sec accident results from a reactivity power excursion without reactor scram in which about 40% of the core fuel was molten at the end of the disassembly phase of the accident. It was assumed that this molten fuel interacted thermally with 40% of the core sodium which was heated up to a saturation pressure of 200 atm. Vaporization of the sodium was assumed to cut off further heat transfer from the fuel and the data in Table A-1 correspond to isentropic expansion of the sodium.

The SOCCOL-II model calculates acoustic pressures due to the compression and expansion of the sodium liquid caused by rapid heating, but these high pressures have been neglected because the presence of voids, which would increase the compressibility of the system. More recent analysis of the same power excursion has led to a higher fraction (about 60%) of molten fuel but this has been compensated for by neglecting the sodium-fuel thermal interaction in those regions of the core which have been voided and by assuming that the instantaneous mixing process includes the stainless steel cladding as well as the sodium and fuel. These latter calculations resulted in slightly higher initial vaporization pressures of 240 atm.

(1) D. E. Simpson, A. E. Waltar and A. Padilla, Jr., "Assessment of Magnitude and Uncertainties of Hypothetical Accidents for the FFTF", HEDL-TME 71-34, March 1971.

The 350 MW-sec accident is assumed to result from a higher reactivity ramp without reactor scram. The effect of the cladding capacitance was taken into account but the effect of voided regions of the core was not considered. The work energies obtained from SOC00L-II were slightly lower than the values listed on Table A-1. To obtain expansions characteristic of 150 and 350 MW-sec, the amount of sodium in the expansion process was increased.

Table A-1 Summary of Sodium Expansion Data

<u>Pressure (atm)</u>	<u>150 MW-sec Accident</u>		<u>350 MW-sec Accident</u>	
	<u>Volume (liters)</u>	<u>Work Energy (MW-sec)</u>	<u>Volume (liters)</u>	<u>Work Energy (MW-sec)</u>
408.2			1,295	0
350			2,813	64.6
300			3,417	86.1
200	286	0	4,960	123.3
100	2,422	37.7	8,226	162.4
90	2,777	41.4	8,843	167.1
80	3,200	45.3	9,586	172.2
70	3,724	49.2	10,505	177.6
60	4,395	53.4	11,685	183.7
50	5,299	57.8	13,280	190.7
40	6,616	62.8	15,610	199.0
30	8,796	68.6	19,471	209.7
20	13,340	76.9	27,572	226.0
10	26,570	94.4	50,821	256.7
9	29,460	97.3	55,850	261.7
8	35,080	100.4	62,076	267.2
7	37,670	104.0	69,990	273.3
6	43,760	108.1	80,389	280.3
5	52,140	112.8	94,680	288.4
4	64,520	118.4	115,590	297.9
3	84,620	125.4	149,210	309.7
2	123,100	134.9	212,840	325.3
1	228,000	150.0	383,850	350.0