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MH-1A MAXIMUM HYPOTHETICAL ACCIDENT ANALYSIS

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September 1968

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SUMMARY

The Maximum Hypothetical Accident for the MH-1A nuclear reactor has been reanalyzed. The variation with time of the containment vessel temperature and pressure has been obtained.

The model used is intentionally conservative. Following a double-ended shear in one of the primary system pipes, the entire quantity of primary system fluid is instantaneously exposed to the containment vessel free volume and thermal equilibrium is attained among the primary fluid (saturated water-steam) and containment vessel air. The temperature and pressure of the containment vessel atmosphere then change with time according to the contribution of various heat sources and sinks. The effects of the containment vessel liner and internal components, fission product decay heat, metal-water reaction, hydrogen recombination, emergency core cooling systems, and pressure suppression spray systems are taken into account.

Regarding the metal-water reaction, 100% of the cladding is assumed to react with steam. The evolved hydrogen is assumed to burn in place as it is generated.

The peak pressure of 138 psia occurs immediately after the rupture and is within the design pressure of 155 psia. The peak atmosphere temperature of 340°F also occurs immediately after the rupture. It is shown that this temperature does not impair the integrity of the containment vessel or its penetrations nor induce any unsafe malfunctions in the engineered safeguards systems.

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I. INTRODUCTION

1. PURPOSE: The purpose of this report is to describe the methods used to calculate the pressure and temperature history of the MH-1A containment vessel atmosphere following a maximum hypothetical accident (MHA).

2. BACKGROUND: The MH-1A is a barge mounted, nuclear power plant designed to provide up to 10 Mw(e) at any site accessible by water. It is moderated and cooled by light water, utilizes slightly enriched UO_2 fuel, and can operate for one year at its design power of 45 Mw(t). The plant was designed and built for the U. S. Army Corps of Engineers by the Martin Company.

Initial criticality was achieved at Ft. Belvoir, Virginia on 25 January 1967. Since then, the MH-1A has operated for the equivalent of slightly more than one full power month to obtain data on core physics and plant equipment and to provide operator training. The 535th Engineering Detachment was activated on 15 March 1968 to operate the plant and accompany it on its first deployment.

As part of its contract, Martin presented the results of its analysis for the MH-1A maximum hypothetical accident(1). Subsequent to this analysis, however, the containment vessel free volume was reduced by approximately 12%. One per cent (1%) of this reduction came from the installation of additional shielding material whose need was determined during preoperational testing. The remaining 11% came from a recomputation of both the containment vessel total volume and the volume occupied by the plant components inside the containment vessel(2). (NOTE: Containment vessel free volume = containment vessel total volume - internal components volume).

In addition to the reduction in containment vessel free volume, the contribution to the MHA of the secondary system within the containment vessel (primarily the secondary side of the steam generator) was removed.

These two changes in the MHA conditions dictate the need for a reanalysis of the MH-1A maximum hypothetical accident. The analysis in Reference 1 is not presented in sufficient detail to permit straight forward incorporation of these changes in the analysis. Therefore, a model has been developed and the analysis has been performed to predict the pressure-temperature history of the MH-1A containment vessel atmosphere following a MHA.

3. CONTENTS OF THE REPORT: Chapter II contains a qualitative description of the MHA and a discussion of the engineered safeguards incorporated in the design of the MH-1A. Chapter III presents the calculational model employed in the analysis. The results are given in Chapter IV and discussed, along with the appropriate conclusions, in Chapter V. The two appendices contain (A) the solution to the time dependent heat transfer equation for a particular set of boundary conditions, and (B) a sample calculation to illustrate application of the model.

II. DESCRIPTION OF MHA AND ENGINEERED SAFEGUARDS

1. GENERAL DESCRIPTION OF MHA: The customary MHA for a pressurized water reactor is a breach of the primary coolant system such that no primary water is available for removing heat from the reactor core. Simultaneously, all auxiliary means for removing heat from the core are rendered inoperative. This leads to a core meltdown and release of the fission product inventory to the containment vessel atmosphere. The containment vessel is assumed to remain intact throughout the accident. Therefore, the leakage of fission products to the environment outside the vapor container is limited to those which escape through the containment at a leak rate which is a function of the containment vessel internal pressure.

Immediately following the primary system rupture, the hot, pressurized primary coolant expands into the containment vessel free volume and partially flashes to steam. The pressure inside the containment vessel increases rapidly until an equilibrium temperature is attained between the saturated steam-water mixture and the air inside containment. The containment vessel pressure (and temperature) then increase or decrease with time as heat is either added to or removed from the steam-water-air mixture. Examples of heat sources are metal-water reactions between the fuel cladding and steam, fission product decay heat, and hot reactor components inside the containment vessel. Examples of heat sinks are cooling water sprays, both internal and external to containment vessel, cold reactor components inside the containment vessel, and the containment vessel itself.

Determination of the pressure vs time curve enables one to calculate an average pressure over a particular time period of interest. This average pressure is then used to determine a containment leak rate for the time period and, finally, the dissemination rate of fission products to the surrounding environment.

2. <u>ENGINEERED SAFEGUARDS</u>: Before turning to a detailed discussion of the MHA analysis, it is important to review those features of the plant system which are designed to prevent the occurrence of or minimize the consequences from a MHA. The following discussion is particularized, where appropriate, to the MH-1A reactor plant.

a. <u>Primary Coolant System</u>. Although not an engineered safeguard because it performs a normal operating function, the primary coolant system is mentioned because the postulated MHA begins with a breach of this system. The system is designed with a significant safety margin such that the stresses in the various system components at maximum allowable pressure are well below the yield stresses. A break in the system is, therefore, a very unlikely event but is nevertheless assumed to occur. For the MH-1A, the break is taken to be a double-ended shear in the primary system piping.

b. <u>Decay Heat Removal Under Non-rupture Conditions</u>. If the primary system integrity remains intact when a malfunction occurs, decay heat can be removed by any one of three methods:

(1) By-passing of steam directly to the main condenser when electric power is available to drive the primary coolant pumps and one of the boiler feed pumps;

(2) Circulation of primary coolant through the purification cooler when electric power is available (from any of three independent diesel generating units) to drive the decay heat pump;

(3) Natural circulation of primary coolant in the primary loop, forming steam on the secondary side of the steam generator. The steam condenses in a coil in the emergency water storage tank where sufficient water is held to remove decay heat for five days.

c. <u>Decay Heat Removal Under Rupture Conditions</u>. If a rupture of the primary system occurs and all primary coolant is removed from the core, there is still a means to remove fission product decay heat provided the pressure vessel integrity is not violated between the bottom and the level corresponding to the top of the reactor core. This means is operation of the Emergency Injection Cooling Water System and the Primary Coolant Charging System.

(1) Emergency Injection Cooling Water System. This system consists of a tank located outside the containment vessel and containing 2600 gallons of borated water, a piping system to connect the tank to the Reactor Vessel Fill Nozzle Line, and two banks of six each compressed nitrogen bottles either of which can force the borated injection water into the reactor vessel. The system is illustrated in Figure 1.

Upon a loss of coolant accident (LOCA) the system is automatically energized to force the water into the reactor vessel at the rate of 100 gpm. This quantity of water and flow rate are sufficient to submerge completely the reactor core before melting would occur.

(2) Primary Coolant Charging System. Although the Emergency Injection Water completely covers the core, there is sufficient decay heat available to vaporize this water and melt the core. Therefore, the Primary Coolant Charging System is provided to spray 8.5 gpm directly into the reactor vessel. The system is shown in Figure 2. It consists of a 10,000 gallon Emergency Water Storage Tank located external to the containment vessel, piping which connects the tank to the Reactor Vessel Fill Nozzle Line, and two positive displacement charging pumps which are installed in parallel to provide 100% backup capability.

Upon indication of a LOCA, charging pump No. 2 is started automatically. There is sufficient water in the Emergency Water Storage Tank to remove the equivalent of the first five days of decay heat generation. Since the tank water supply can be replenished, the core can be kept submerged indefinitely. Electric power for the charging pumps is backed up by three independent diesel generators and the emergency diesel as well as onshore power when the MH-1A is connected to a dependable power grid.

There is an additional capability of the Coolant Charging System. A boron solution can be mixed manually in the Chemical Addition Manifold (see Figure 2) and injected into the reactor vessel with the charging pumps. This feature is intended for use in situations where negative reactivity cannot be inserted by control rods.

The valves and piping associated with each of these emergency injection systems are arranged such that a single pipe failure in either or both of them will not violate containment integrity. Failure of the piping external to the containment vessel in either of these systems will prevent water from that system from entering the containment vessel. Failure inside the containment vessel does not prevent water from the system entering the containment vessel. Finally, it is noted that both systems

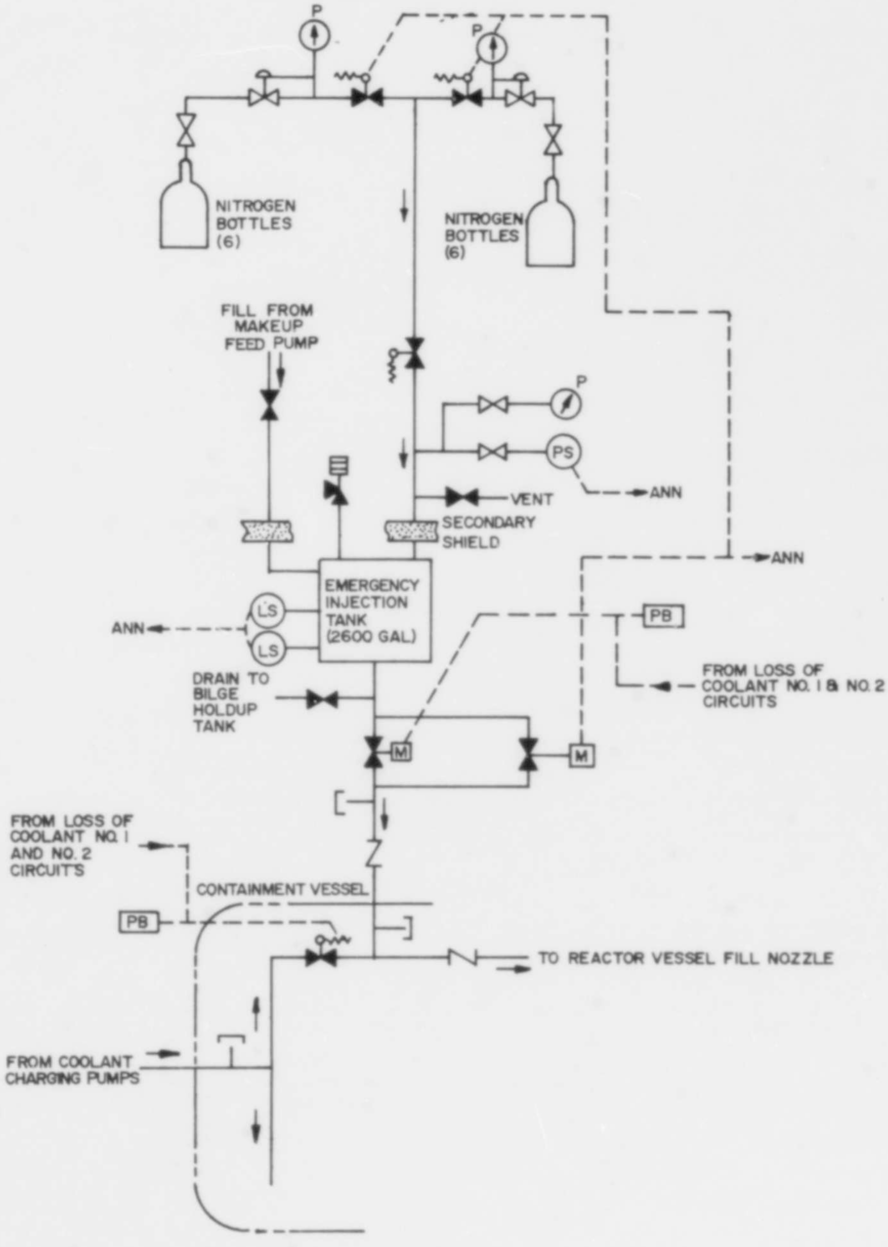

FIGURE 1
EMERGENCY INJECTION COOLING WATER SYSTEM

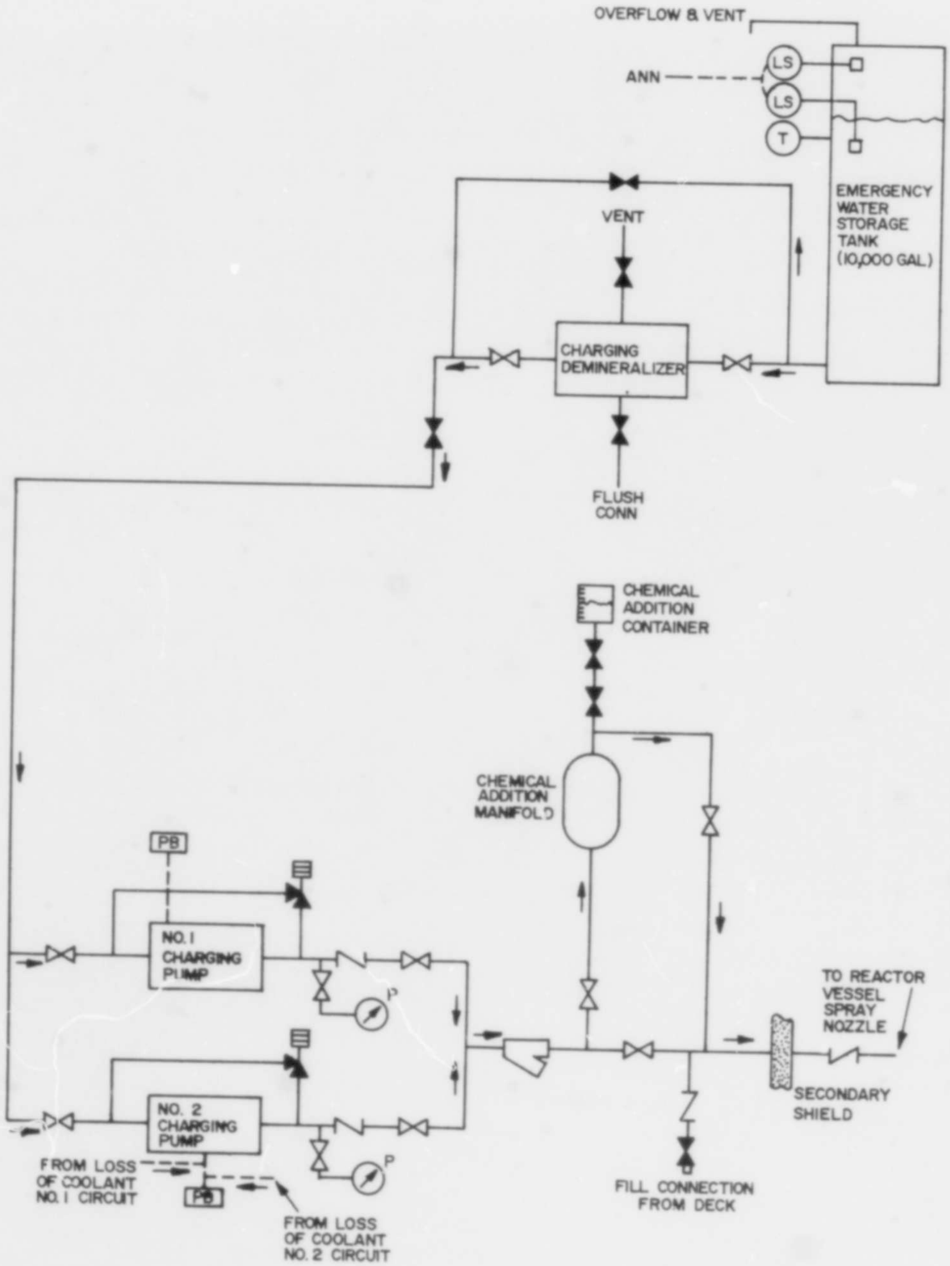

FIGURE 2
PRIMARY COOLANT CHARGING SYSTEM

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must operate as intended if a core meltdown is to be prevented. Failure of water from the Emergency Injection Cooling Water System to reach the core leads to a meltdown shortly after the LOCA because the 8.5 gpm flow rate from the Coolant Charging System is insufficient to cover the core before melting conditions are reached. Failure of the Primary Coolant Charging System Water to reach the core leads to melting as soon as sufficient decay heat has been generated to vaporize the Emergency Injection Water covering the core.

d. Pressure Suppression Spray Systems. These systems are designed specifically to help reduce the containment vessel internal pressure after a LOCA. Cooling water is sprayed either directly into the containment vessel or over the external surface of the containment vessel. Heat is transferred to the spray water which drives the pressure down.

(1) Fresh Water Spray System. This system is actually two systems--one external to containment and the other internal--and is illustrated schematically in Figure 3. Both systems use the same supply tanks, pump, and header but are otherwise separate from each other. The total capacity of the two fresh water supply tanks is 30,800 gallons. These tanks can be refilled during operation to provide an additional supply if required.

The internal spray system is designed to discharge 20 gpm of fresh water into the containment at an initial pressure of 150 psia and a minimum of 200 gpm when containment pressure is at or below 50 psia. This system is lined up during normal plant operation and is automatically initiated upon indication of high containment pressure and low primary pressure. Initiation occurs three minutes after receipt of these signals.

The external spray system is independent of the containment vessel pressure, resulting in constant flow from the fresh water pump of at least 200 gpm. The system is backed up through a hose connection by any one of the three 300 gpm salt water fire pumps. Because this system also serves as a fire system for the reactor access compartment, not all of the 200 gpm spray impinges on the exterior of the containment vessel. Approximately one third (1/3) of the surface area is covered by a conservatively estimated 50 gpm of spray.

(2) Salt Water Spray System. Figure 4 shows the salt water spray system which is external to the containment vessel. It can be seen that the reactor access compartment spray nozzles are

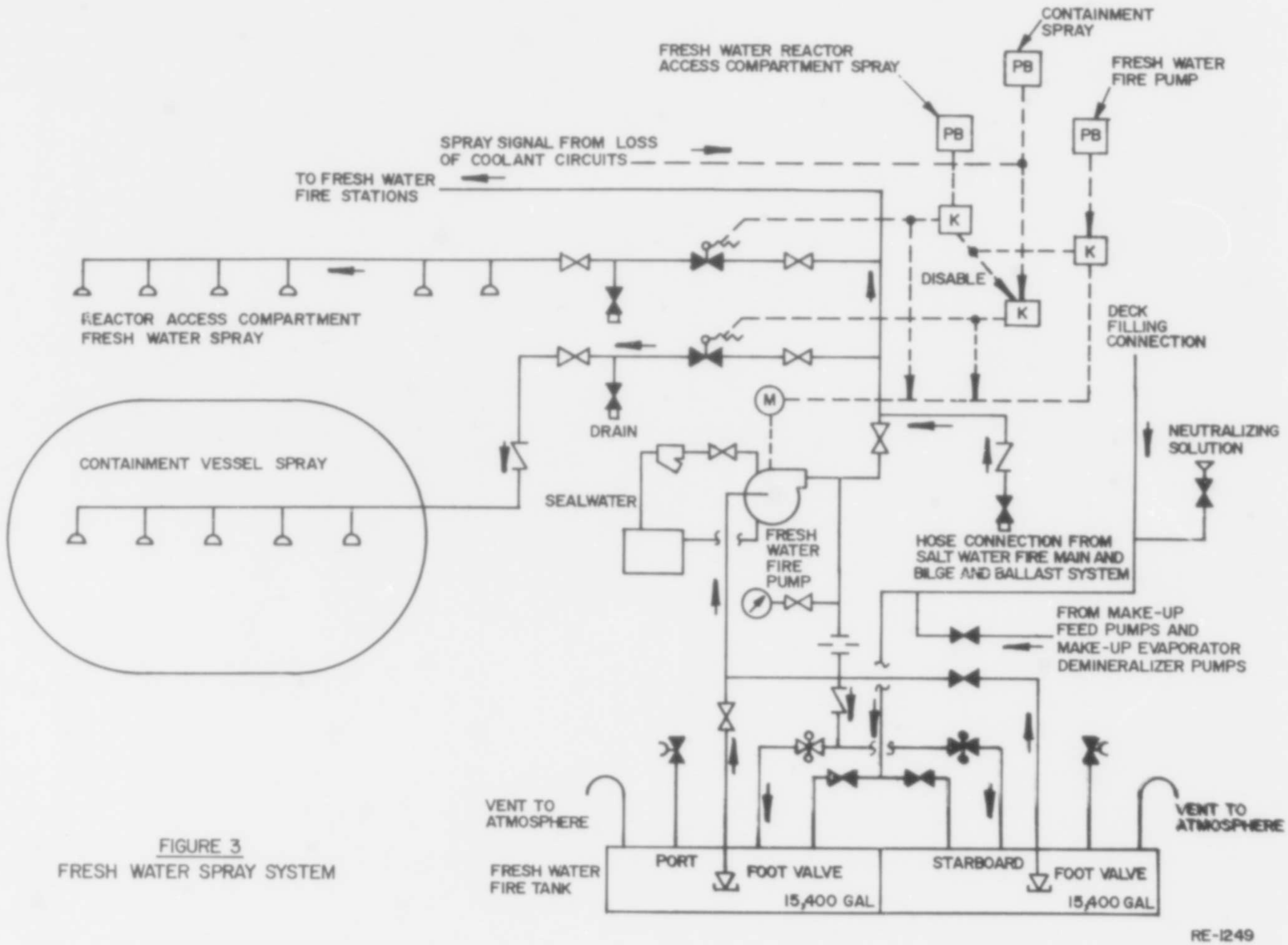

FIGURE 3
FRESH WATER SPRAY SYSTEM

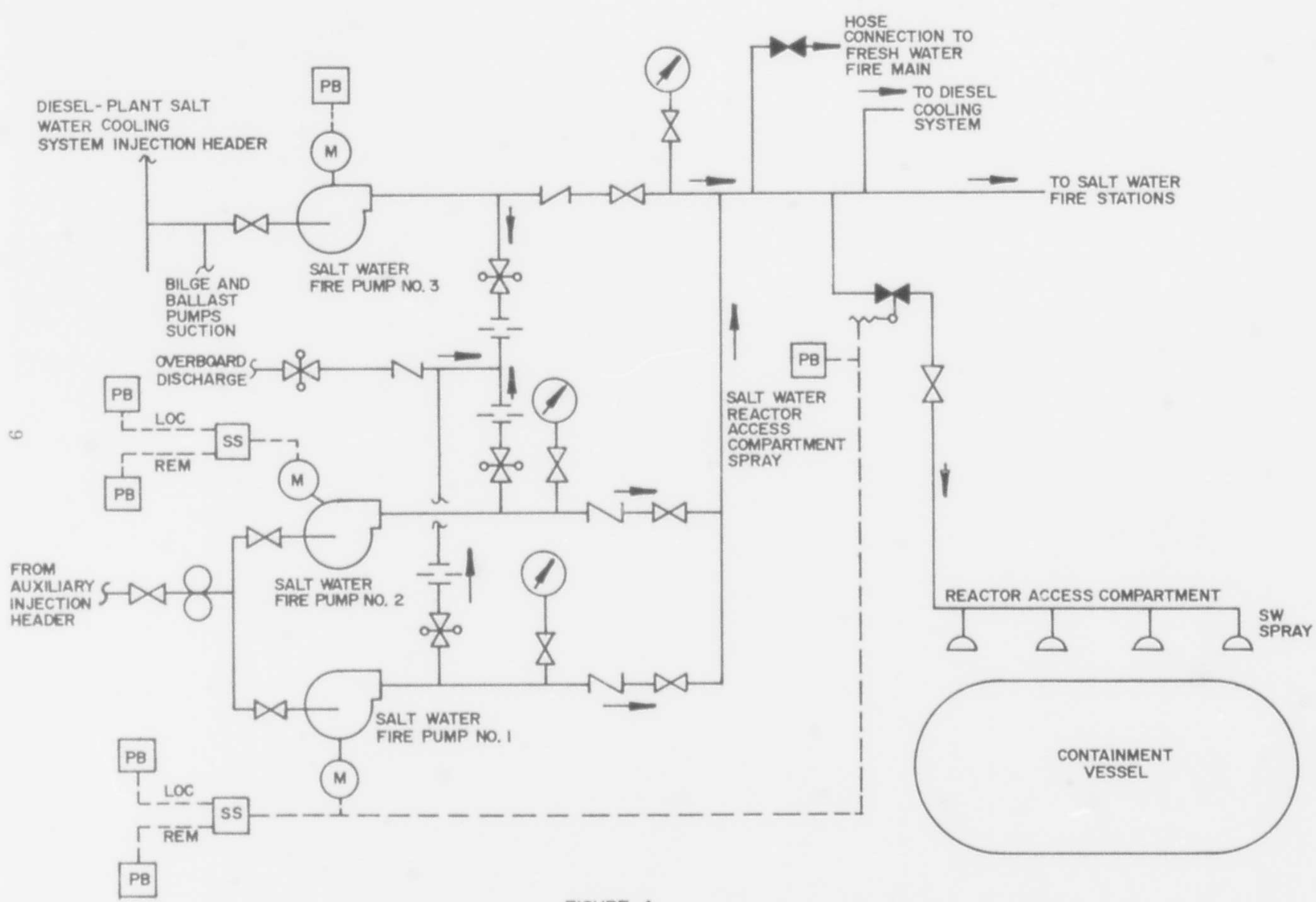

FIGURE 4
SALT WATER SPRAY SYSTEM

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supplied from a main salt water header. Salt water to this header is supplied by three salt water fire pumps. Pump #3 is energized by the emergency bus so that backup power is available to operate at least one pump. The system is designed such that with any one of the three 300 gpm salt water fire pumps operating, the spray is directed evenly on approximately 80% of the exterior of the containment vessel. Comparison of the effectiveness of the external salt water spray system with the external fresh water spray system indicates that the salt water system is more effective in that six times as much water is sprayed over more than twice the area.

Neither of these pressure suppression systems is completely duplicate. Thus, failure (piping, valves, or pumps) of either the fresh water or the salt water system can result in that system being rendered inoperative. However, taken as a whole these two systems are independent and constitute an analogus one out of two coincident systems. It is thus considered valid to assume one system will be operative in the event of a MHA. The least effective, the external fresh water spray system, is selected for the MHA analysis.

e. <u>Containment Vessel.</u> The final barrier to direct release of radioactive fission products to the surrounding environment is the containment vessel (or vapor container). Both the pressure of the containment atmosphere and the fission product leak rate derived therefrom depend on the unimpaired integrity of the containment vessel. Therefore, it is important that the containment vessel be able to withstand any pressure occurring throughout the course of the accident.

Nominal design pressure for the containment vessel is 155 psia which results in a hoop stress equal to 46% of the yield stress (minimum yield stress is 38,000 psi). A containment pressure of 315 psia results in a hoop stress equal to the minimum yield stress. It will be shown in succeeding sections of this report that a conservative analysis of the MH-1A MHA results in a maximum pressure well below that required to reach the minimum yield stress.

3. <u>DESIGN SEQUENCE OF EVENTS AFTER LOCA AND POSTULATED SEQUENCE OF EVENTS FOR MHA:</u> This chapter closes with a summary of the normal functioning of the engineered safeguards after a LOCA and the postulated functioning of the engineered safeguards after a LOCA leading to the MHA.

The following sequence of events is designed to be automatically initiated after a LOCA:

a. The pressure-actuated (set point 10 psig) containment isolation valves close. Since these valves are designed to fail in a closed position, the integrity of the containment vessel as a barrier to the release of radioactivity is assured by the isolation of all fluid lines which penetrate the containment vessel. Thus, any possible escape of fission products from the containment vessel is limited to those which escape by leakage from the containment vessel.

b. Both halogen filters initiate automatically.

c. The emergency injection tank is pressurized, thereby forcing its contents into the reactor vessel at a rate of 100 gpm. The reactor core is completely submerged. Cooling is accomplished by natural convection and pool boiling with makeup provided by the charging pump.

d. Charging Pump #2 is started. This pump injects water at a rate of 8.5 gpm directly into the active core region to remove decay heat. Charging Pump #2 is connected to the emergency bus and draws water from the emergency water storage tank (10,000 gallons). If necessary, additional water can be piped to the emergency water storage tank or added directly through the deck connection. Injection into the reactor vessel can thereby be continued as required.

e. The fresh water internal spray system automatically starts after three minutes delay.

The postulated MHA requires that the above described sequence of events <u>not</u> occur. Instead, the MHA is characterized by the following sequence of events:

a. The pressure-actuated containment isolation valves close.

b. Both halogen filters initiate automatically.

c. The emergency injection tank is pressurized five minutes after the LOCA. The contents of the tank are forced into the <u>containment</u> vessel at a rate of 100 gpm but do not reach the <u>pressure</u> vessel.

d. There is a failure in the primary coolant charging system external to the containment vessel. This system is, therefore, inoperative throughout the accident.

e. The fresh water external spray system initiates after a 15 minute delay.

This sequence of events, which is the same as that used by the designer of the MH-1A,[1] is adopted as a basis for the analysis and the results which are described in the remainder of this report.

III. THEORETICAL METHODS

This chapter is divided into two sections. The first section presents the method used to calculate the containment temperature and pressure immediately following the primary system rupture. The second section describes the variation with time of the temperature and pressure following attainment of the initial conditions.

1. <u>INITIAL TEMPERATURE AND PRESSURE:</u> The initial containment temperature and pressure are calculated on the assumption of a constant internal energy process. The entire mass of water is the primary system is exposed to the containment vessel free volume and partially flashes to steam. No heat is transferred to any components inside the containment vessel, to the walls of the containment vessel, or to the environment external to the containment vessel. The process takes place instantaneously and equilibrium is reached according to the following equation:

$$\begin{pmatrix}\text{internal energy of}\\ \text{primary system before}\\ \text{rupture}\end{pmatrix} = \begin{pmatrix}\text{difference in internal}\\ \text{energy of air before}\\ \text{and after rupture}\end{pmatrix} + \begin{pmatrix}\text{internal energy of}\\ \text{water-steam mixture}\\ \text{after rupture}\end{pmatrix} \quad (1)$$

Equation (1) is written quantitatively as:

$$M\bar{u}_o = M_a C_{va} (T_{a1} - T_{ao}) + m_{w1} u_{w1} + m_{s1} u_{s1} , \quad (2)$$

where,

M = total mass of primary system fluid (loop water, pressurizer water, and pressurizer steam), lbs.,

$\bar{u}_o$ = average specific internal energy of primary system fluid before rupture, BTU/lb.,

M_a = mass of air in containment vessel, lbs.,

C_{va} = specific heat of air at constant volume, BTU/lb. °F,

T_{ao} = temperature of containment atmosphere (air) before rupture, °F,

m_{w1}, m_{s1} = masses of water and steam, respectively, after rupture, lbs.,

u_{w1}, u_{s1} = specific internal energies of saturated water and steam, respectively, after rupture, BTU/lb.

The last two terms can be put in a form more tractable for numerical calculations (the subscript$_1$ is dropped for convenience):

$$m_w u_w + m_s u_s = m_w u_w + m_s (u_w + u_{ws}) \tag{3}$$

$$= M \left(u_w + \frac{m_s}{M} u_{ws}\right),$$

where,

u_{ws} = change in specific internal energy going from saturated water to saturated steam, BTU/lb.,

$M = m_w + m_s$.

The mass of steam is given by:

$$m_s = \frac{\text{steam volume}}{v_s} = \frac{V - m_w v_w}{v_s} \tag{4}$$

where,

V = containment vessel free volume, ft^3,

v_w, v_s = specific volumes of saturated water and steam, respectively, ft^3/lb.

Multiplying numerator and denominator of Eq. (4) by the quantity,

$$1 - \frac{v_w}{v_s} = 1 - \frac{m_s v_w}{V - m_w v_w},$$

the result is:

$$m_s = \frac{V - m_w v_w}{v_s} \times \left\{ \frac{1 - \dfrac{m_s v_w}{V - m_w v_w}}{1 - \dfrac{v_w}{v_s}} \right\}. \tag{5}$$

Algebraic manipulation of Eq. (5) and insertion of the result in Eq. (3) yields the desired expression:

$$m_w u_w + m_s u_s = M(u_w + \frac{V/M - u_w}{v_{ws}} u_{ws}). \tag{6}$$

Finally, Eq. (6) is introduced into Eq. (2)

$$M\bar{u}_o = M_a C_{va} (T_{a1} - T_{ao}) + M(u_{w1} + \frac{V/M - v_{w1}}{v_{ws1}} u_{ws1})$$

$$= \Delta Q_{air} (T_{a1}) + Q_{ws} (T_{a1}, M). \tag{7}$$

The term $\Delta Q_{air}(T_{a1})$ represents the change in internal energy of the air after the rupture and the term $Q_{ws}(T_{a1}, M)$ represents the internal energy of the saturated water-steam mixture after the rupture. These two terms could also be called heat contents, instead of internal energies, because no work is performed. Note that the heat content of the water-steam mixture is a function of the total mass of water and steam and of the mixture temperature.

The left side of Eq. (7) is the known internal energy of the primary system fluid before the rupture. For a particular value of the final temperature, T_{a1}, the right side of Eq. (7) can be evaluated because all quantities are either known (M_a, C_{va}, T_o, M, V) or obtainable from the steam tables (u_w, u_{ws}, v_w, v_{ws}). If Eq. (7) is not satisfied by the guessed value of T_{a1}, another is selected and the process repeated until a balance is obtained.

The temperature of the steam-water-air mixture immediately after rupture is that obtained by the above procedure. The pressure of the mixture is the sum of the partial pressures of steam at that temperature and air at that temperature. The air partial pressure is calculated according to the perfect gas law:

$$P_{air_1} = P_{air_o}\left[\frac{T_{a1} + 460}{T_{ao} + 460}\right]. \qquad (8)$$

2. <u>TEMPERATURE - PRESSURE HISTORY</u>: After the rupture and establishment of initial conditions, the containment temperature and pressure vary with time as various heat sources and sinks come into play. Removal of heat from the containment vessel atmosphere by a heat sink reduces the temperature and pressure; the opposite is true for a heat source.

The calculation is divided into a series of discrete time steps. Contributions from all applicable heat sources and sinks are evaluated for each time step. The containment temperature and pressure at the end of the j^{th} time step are then obtained from a modified form of Eq. (7):

$$M\bar{u}_o + \sum_{i,j} \Delta Q_i (\tau_j) = \Delta Q_{air} (T_a) + Q_{ws} (T_a, M). \qquad (9)$$

The summation index "i" represents all heat sources and sinks. The summation index "j" indicates that contributions from all previous time steps are included in the summation. The sign of $\Delta Q_i (\tau_j)$ is positive for a heat source and negative for a sink. Implicit in the use of Eq. (9) is the assumption that heat is added to or removed from the steam-water-air mixture homogeneously.

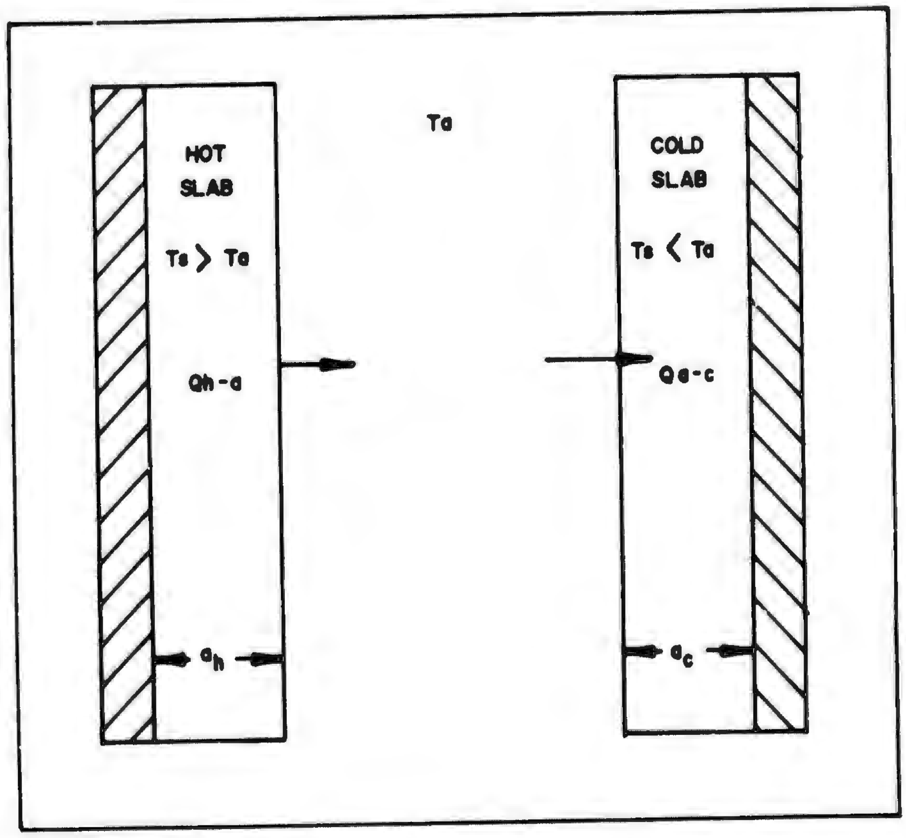
The determination of containment temperature and pressure is similar to that described in Section (III. 1) with regard to Eq. (7). The left side of Eq. (9) is the known internal energy of the primary system fluid before rupture plus the calculated <u>net</u> addition or removal of heat at time τ_j. The right side of Eq. (9) is iterated on T_a until agreement with the left side is obtained. The pressure, as before, is the sum of the partial pressures of air and steam.

The remainder of this section provides a more detailed discussion of the various heat sources and sinks. Section (2. a) describes the effect of the vapor container liner and internal components. Fission product decay heat and metal-water reactions are discussed in sections (2. b) and (2. c), respectively. The effects of emergency injection water and cooling water sprays are taken up in section (2. d). Finally, the sequence of steps to determine the temperature and pressure at the end of a time step is presented in section (2. e).

a. <u>Heat Transfer Between Slab and Atmosphere</u>. As long as there is a temperature difference between the containment atmosphere and the containment vessel liner or internal components, heat will be transferred from the higher temperature medium to the lower temperature medium. A model which describes this heat transfer is presented in this section. The containment vessel liner and internal components are divided into two slabs. One slab represents the liner and those components which are at ambient temperature prior to the rupture (T_{ao}). This slab is called the cold slab. The surface area and thickness of the slab are chosen to approximate as nearly as possible the actual heat transfer area and thickness. One side of the slab is perfectly insulated to coincide with (1) the assumption that the outside of the containment vessel is insulated and (2) the fact that most of the reactor components inside containment are insulated on one side. The second slab, the hot slab, represents those internal components in contact with the hot, primary coolant prior to rupture. Its temperature prior to the rupture is close to that of the primary coolant. This slab is also insulated on one side and has a thickness and heat transfer area chosen in an identical manner to those of the cold slab.

The preceding description is illustrated schematically in Figure 5 which shows a hot and a cold slab immersed in an atmosphere at temperature T_a. The terms "hot" and "cold" are relative to T_a.

The model is illustrated by considering the cold slab. Treatment of the hot slab is identical. The slab is very large in the y and z directions compared to the x direction so that heat transfer and conduction are one dimensional. One side of the slab is perfectly insulated. The heat



CONTAINMENT VESSEL

Ts = SLAB TEMPERATURE

Ta = MIXTURE TEMPERATURE

Qh-a = HEAT TRANSFERRED FROM HOT SLAB TO ATMOSPHERE

Qa-c = HEAT TRANSFERRED FROM ATMOSPHERE TO COLD SLAB

FIGURE 5

DIAGRAM OF HEAT TRANSFER MODEL

absorbed by the slab is determined from the slab temperature distribution in space and time. This is given by the solution to the one-dimensional heat conduction equation:

$$\frac{\partial T_s(x,t)}{\partial t} = \alpha^2 \frac{\partial^2 T_s(x,t)}{\partial x^2}, \qquad (10)$$

where $\alpha^2 = k/\rho C_p$. The slab is initially at uniform temperature T_{so}; one side is insulated (the side at x = a); and the temperature of the other side is assumed to increase exponentially to the temperature that would be attained at final equilibrium of the slab-mixture medium. Mathematically, these conditions are expressed as:

$$T_s(x,o) = T_{so}, \qquad (11a)$$

$$\frac{\partial T_s(a,t)}{\partial x} = 0, \qquad (11b)$$

$$\begin{aligned} T_s(o,t) &= T_{so} + (T_{sf} - T_{so})\left[1 - \exp(-t/\beta)\right] \\ &= T_{so} + \Delta T_s \left[1 - \exp(-t/\beta)\right], \end{aligned} \qquad (11c)$$

where,

T_{sf} = final equilibrium temperature, ^oF,

β = slab surface decay constant which is unknown, hr.

The temperature variation embodied in Eq. (11c) is selected because it (1) is physically reasonable and (2) can be treated analytically. It is more realistic than the usual assumption of a constant slab surface temperature over a particular time step.

The solution to Eq. (10) with the initial and boundary conditions of Eqs. (11) is treated in detail in Appendix A. The result is repeated here:

$$T_s(x,t) = T_{so} + \Delta T_s \left[1 - \exp(-t/\beta) - \frac{4}{\pi}\sum_{n\ odd}^{\infty} \sin\left(\frac{n\pi x}{2a}\right)\left(\frac{\exp(-t/\beta) - \exp(-n^2 t/\lambda)}{n\left[n^2(\beta/\lambda) - 1\right]}\right)\right] \qquad (12)$$

where $\lambda = (2a/\pi\alpha)^2$.

The heat absorbed by the slab at time $t = \tau$ is given by:

$$\Delta Q_s(\tau) = (C_p \rho A)_s \int_0^a \left[T_s(x, \tau) - T_{so} \right] dx , \qquad (13)$$

where,

C_p = slab heat capacity, BTU/lb. ^oF,

ρ = slab density, lb/ft^3,

A = slab surface area, ft^2.

Inserting Eq. (12) in Eq. (13) and performing the integration, there results:

$$\Delta Q_s(\tau) = (mC_p \Delta T)_s F_s(\tau, \beta, \lambda), \qquad (14)$$

where,

$$F_s(\tau, \beta, \lambda) = 1 - \exp(-\tau/\beta) - \frac{8}{\pi^2} \sum_{n \text{ odd}}^{\infty} \frac{\exp(-\tau/\beta) - \exp(-n^2\tau/\lambda)}{n^2\left[n^2(\beta/\lambda) - 1\right]} . \qquad (15)$$

The heat absorbed by the slab, given by Eqs. (14) and (15), must be equal to the heat transferred from the atmosphere to the slab across the slab-atmosphere boundary. This is expressed as

$$\Delta Q_s(\tau) = (h A)_s \int_0^\tau \left[T_a(t) - T_s(o, t) \right] dt, \qquad (16)$$

where,

h = heat transfer coefficient, BTU/hr ft^2 ^oF.

The integral in Eq. (16) can be evaluated by (1) assuming that temperature of the steam-water-air mixture decreases linearly from T_{a1} to T_{a2} over the time step τ and (2) inserting the expression for $T_s(o, t)$ given in Eq. (11c). The result is:

$$\Delta Q_s(\tau) = (h\,A)_s \left[\left(\frac{T_{a1} + T_{a2}}{2} - T_{so} - \Delta T_s \right) \tau + \Delta T_s\, \beta \left(1 - \exp\left[-\tau/\beta \right] \right) \right]. \quad (17)$$

Equations (14) and (17) both describe the amount of heat transferred during the time step and a consistent set of parameters must be determined such that both equations give the same value of $\Delta Q_s(\tau)$. A resume of the procedure is given in section (2. e); Appendix B provides a numerical example.

b. Fission Product Decay Heat. One of the sources of heat comes from the decay of radioactive fission products. These build up during normal power operation and continue to produce heat long after the reactor is shut down. After the MHA, the fission product heat initially goes into raising the temperature of the stainless-steel fuel cladding until reaction between the cladding and steam is possible. (Metal-water reactions are discussed in the next section.) Heat generated subsequent to this is then available to increase the temperature and pressure of the containment vessel atmosphere.

The rate of decay heat generation after shutdown is a function of the reactor power level and operating time as well as the time after shutdown. One representation for the heat generation rate is given by the empirical expression (3):

$$\frac{P(t)}{P_o} = 0.1\,(t + 10)^{-0.2} - 0.087\,(t + 2 \times 10^7)^{-0.2} - 0.1\,(t + T_o + 10)^{-0.2} + 0.087\,(t + T_o + 2 \times 10^7)^{-0.2}, \quad (18)$$

where,

t = time after shutdown, secs.,

T_o = reactor operating time, secs.,

$P(t)$ = decay heat generation rate at time t, BTU/hr.,

P_o = average power level during time T_o, BTU/hr.

The heat generated during a particular time period is determined by numerical integration of Eq. (18).

c. Metal-Water Reactions. Another source of heat comes from the stainless steel - water reaction. If the temperature of the stainless steel cladding is high enough (threshold value of approximately 1100°C), exothermic reactions between the stainless steel and containment vessel steam can occur. The two reactions of importance are:

$$F_e + H_2O \longrightarrow F_eO + H_2 + \text{Energy},$$

$$2C_r + 3H_2O \longrightarrow Cr_2O_3 + 3H_2 + \text{Energy}.$$

The energy released when the reaction product oxides are in solid form is 253 cal/gram of stainless steel consumed, and the calculated quantity of hydrogen evolved is 0.51 liters (STP)/gram of stainless steel consumed (5).

The reaction product hydrogen gas is evolved at the reaction temperature (greater than 1100°C). It mixes with that portion of the containment vessel atmosphere circulating through the pressure vessel (and the hot reactor core) and is exhausted to the containment vessel through the outlet rupture. Since the ignition temperature for the hydrogen-oxygen recombination is 585°C, the hydrogen is assumed to recombine with containment vessel oxygen at the outlet rupture. The recombination energy is 51,593 BTU/lb of hydrogen reacted[6].

For inclusion in the MHA analysis, 100% reaction of the stainless steel clad is assumed. The evolved hydrogen combusts as it is generated until the entire amount of containment oxygen is exhausted. Subsequent hydrogen generated collects in the containment vessel and contributes its partial pressure to the total pressure. The two energies (reaction + recombination) are added together and released homogeneously to the containment atmosphere at a rate greater than 0.05% of the clad reacted per second.

Additional investigations related to the metal-water and recombination reactions were also undertaken for informational purposes. Part of this effort was geared toward the determination of a termination mechanism for the metal-water reaction. The remainder considered the question of hydrogen accumulation in the containment vessel and possible combustion at appropriate concentrations of hydrogen, air, and steam.

(1) Termination Mechanism. The 100% stainless steel-steam reaction assumed for the MHA analysis represents a conservative upper bound. It is reasonable to expect it to be less. There are two ways in which the reaction might be terminated prior to 100% reaction: (a) exhaustion of the steam supply or (b) core slumping and subsequent quench in residual water in the bottom of the pressure vessel.

The former conclusion was reached in an analysis of the loss of coolant accident for the N. S. Savannah[10] Nuclear Reactor. This reactor is fueled with slightly enriched UO_2 and clad with stainless steel. The metal-water reaction ends with 9.36% of the clad reacted due to evaporation of all residual water in the pressure vessel. The analysis was aided by use of the FLASH[11] and NURLOC[12] digital computer programs. This type of analysis, if applied to the MH-1A, would probably yield a much higher per cent reaction because the N. S. Savannah primary coolant inlet and outlet pipes are located much closer to the bottom of the pressure vessel than are those of the MH-1A (i. e., much less water left in N. S. Savannah pressure vessel after blowdown).

The latter approach, core slumping, has been used in a number of metal-water reaction calculations. This is particularly true for reactors clad with zirconium. In this model, the reaction is assumed to terminate when the clad melting temperature is reached. An additional quantity of metal (usually 5-10% of the remaining, unreacted clad) is often assumed to react during the quenching process. The analysis for the Tennessee Valley Authority's Browns Ferry Plant[13] resulted in a 30-35% metal-water reaction before the reaction terminated. For the Pacific Gas and Electric Company's Diablo Canyon Plant[14] the calculated quantity is 25-30% of the zirconium clad. This analysis assumed that the reaction terminated at the melting point of ZrO_2 (4700-4800^oF) rather than that of Zr (3200-3300^oF). This slumping model (using the melting point of Zr as the termination temperature) has also been applied to the Millstone Point, Dresden Unit 2, and Humboldt Bay Reactor Plants[15].

It is not clear that this model is valid for stainless steel clad, UO_2 fueled reactors. Some experimental evidence[5] indicates that stainless steel tends to foam in place as it approaches its melting temperature rather than fall away from the fuel element. If this is true, then slumping might not occur until the much higher UO_2 melting temperature is approached. Nevertheless, a calculation was performed to determine the expected amount of reaction according to the clad melting model.* The calculational model is described as follows. After the primary coolant blowdown, the cladding is heated up adiabatically by decaying fission products. The cladding begins reacting with steam as soon as its temperature enters the reaction region (approximately 2000°F). The reaction continues with an unlimited supply of steam until the melting point of stainless steel (approximately 2550°F) is reached at which time the reaction terminates. The termination mechanism is quenching of the stainless steel in residual water in the bottom of the pressure vessel. An additional 5% of the unreacted metal prior to quench is assumed to react during the quenching process.

(2) Hydrogen Accumulation. The effect of hydrogen collecting in the containment vessel, rather than burning at the outlet rupture, was also investigated. Combustion of hydrogen in a steam-air-hydrogen can occur when the proper concentrations of these constituents are attained. Flammability limits for a mixture of this type are shown in Figure 6. Combustion of the hydrogen is assumed to occur when the 300°F - 100 psig limit is reached.

The hydrogen burns very rapidly (instantaneously for calculational purposes) according to the following formula:

$$2H_2 + O_2 \rightarrow 2H_2O + \text{Energy},$$

and super heats the steam-air mixture. The reaction terminates when the supply of either reactant is exhausted. Containment temperature and pressure are determined from the following equation:

$$Q_H = m_s (u_{s2} - u_{s1}) + \int_{T_{a1}}^{T_{a2}} (m_N C_{vN} + m_H C_{vH})\, dT, \qquad (13)$$

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* The actual calculations were carried out by W. J. Gallagher and G. Lurf of NUS Corporation, Washington, D. C.[16]

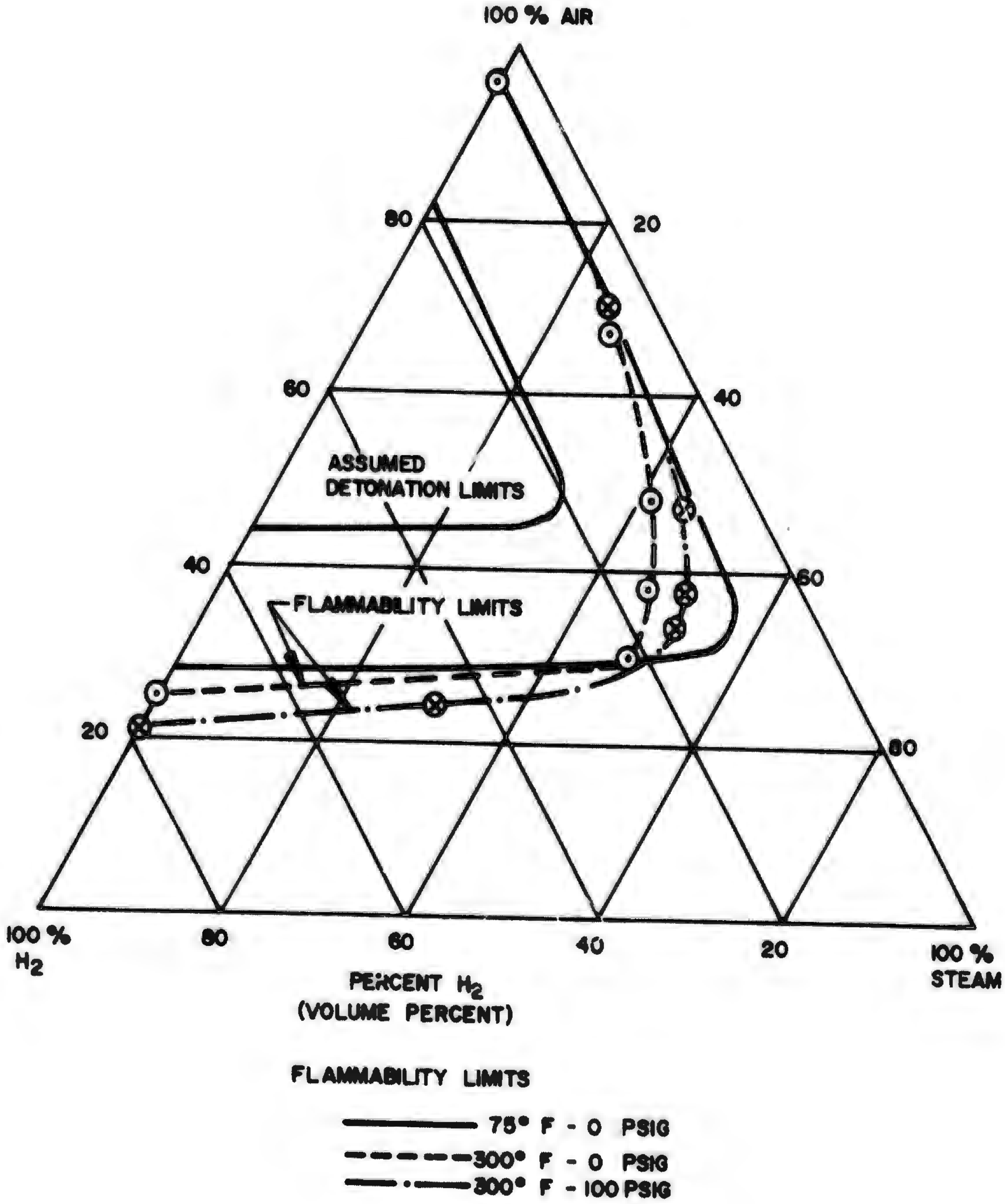

FIGURE 6

FLAMMABILITY LIMITS OF HYDROGEN AIR STEAM MIXTURES FROM WAPD-SC-545 DATED SEPTEMBER 1957 BY DR. Z. M. SHAPIRO AND T. R. MOFFETTE

Q_H = total heat of combustion (which is a function of the amount of clad reacted), BTU,

m_s = mass of steam in atmosphere before combustion plus mass of steam formed as combustion product, lbs.,

u_{s1}, u_{s2} = specific internal energies of steam before and after combustion, respectively, BTU/lb.,

T_{a1}, T_{a2} = containment temperatures before and after combustion, respectively, ^oF,

m_N = mass of nitrogen in containment atmosphere, lbs.,

C_{vN} = specific heat of nitrogen, BTU/lb ^oF,

m_H = mass of hydrogen in containment atmosphere which does not react, lbs.,

C_{vH} = specific heat of hydrogen, BTU/lb. ^oF.

Note that the terms in Eq. (19) involving hydrogen would be replaced by similar terms involving oxygen if the hydrogen were exhausted first. The temperature after combustion is determined by trial and error solution of Eq. (19) with data on steam taken from the superheat section of the steam tables and data on specific heat taken from Ref. 7. The pressure after combustion is the sum of the partial pressures of the constituents left in containment.

The superheated atmosphere loses temperature and pressure rapidly due to heat transfer to the containment vessel liner and components therein. An additional heat sink is the liquid water inside containment which is still at its precombustion temperature. The calculation of the heat removal is similar to that described in section (2. a) of this chapter.

d. Injection Water and Cooling Water Sprays.

(1) Emergency Injection Water. The emergency injection water is assumed to enter the containment vessel but not the pressure vessel. It mixes uniformly with the containment vessel mixture. At the end of a time step, its contribution appears twice in Eq. (9):

(a) as a heat source whose magnitude is the product of the mass of water injected with the specific internal energy of the water;

(b) as a heat sink in the term representing the heat content of the water-steam mixture ($Q_{ws}(T, M)$).

The net effect of the injection water is to lower containment temperature and pressure because it must absorb heat to reach equilibrium with the containment atmosphere.

(2) Cooling Water Sprays. The cooling water sprays are also heat sinks. The internal spray is treated exactly as the emergency injection water. External sprays are handled according to

$$\Delta Q_{cw} = (m\ C_p)_{cw}\ (T_{cw2} - T_{cw1}), \tag{20}$$

where T_{cw1} and T_{cw2} are the initial and final temperatures of the spray water. The spray water is conservatively not allowed to vaporize so that T_{cw2} is 212°F or less depending on the temperature of the containment vessel liner. Any portion of the containment vessel not covered by spray water is assumed to remain perfectly insulated.

e. Illustration for a Particular Time Step. The effect of the several sources and sinks is summed up in this section by carrying out, qualitatively, the calculation for a particular time step. A quantitative example is provided in Appendix B.

The j^{th} time step, $t_j \leq t \leq t_j + 1$, is considered ($\tau_j = t_{j+1} - t_j$). The procedure is as follows:

(1) At time t_j the equilibrium temperature, T_{sf}, is determined. This is the temperature which would be attained by both slabs and the atmosphere after a sufficiently long time with no further heat addition. The heat balance includes the effect of all heat sinks up to t_j and the effect of all heat sources up to t_j plus the contribution of all heat sources (except the hot slab) over the period τ_j. This temperature is required in the solution of the slab heat transfer equations (see section 2. a).

(2) The heat lost by the hot slab (heat source) and heat gained by the cold slab (heat sink) are calculated via Eqs. (14) and (17). The quantities m, C_p, ΔT_s, λ, h, and A, and T_{so} are assumed known in these equations. A guess is made for $T_{a,j+1}$, the temperature of the containment atmosphere at the end of the time step, and Eqs. (14) and (17) are iterated on β until they both yield the same value of $\Delta Q_s(\tau_j)$. (Note: A value of $\Delta Q_s(\tau_j)$ is obtained for each slab.)

(3) The assumed value of $T_{a, j+1}$ also determines the values of ΔQ_{air} and Q_{ws} at t_{j+1}. Thus, Eq. (9) must also be satisfied:

$$M\overline{u}_o + \sum_{i,j} \Delta Q_i(\tau_j) = \Delta Q_{air}(T_{a,j+1}) + Q_{ws}(T_{a,j+1}, M), \quad (9)$$

where the summation term now includes the values of $\Delta Q_s(\tau_j)$ obtained in step (2) as well as the sources and sinks described in step (3). It is unlikely, however, that the assumed value of $T_{a,j+1}$ will lead to satisfaction of Eq. (9). Therefore, steps (2) and (3) are repeated with successive values of $T_{a,j+1}$ until Eq. (9) is satisfied.

(4) When the correct value of $T_{a,j+1}$ has been obtained via steps (2) and (3), the effect of the other sources and sinks is considered. The contribution of heat sinks over the time step τ_j, namely the emergency injection water and cooling water sprays, is calculated. The contribution of heat sources over the <u>next</u> time step (τ_{j+1}), namely the metal-water reaction and fission product heat, is also calculated. Both contributions are included in the summation term of Eq. (9) at the time t_{j+1}, along with all previous source and sink contributions (including the values of $\Delta Q_s(\tau_j)$ obtained in steps (2) and (3), and a new value of $T_{a,j+1}$ is obtained from Eq. (9). This procedure leads to a step change in temperature at each time step (and the "before" and "after" notation of Chapter IV). It reflects the fact that the slabs are treated as functions of time whereas the other sources and sinks are treated as step inputs in time.

This completes the description of one time step. Following step (4), a new value of T_{sf}, step 1, is determined and so on. The procedure is repeated for successive time steps to obtain the containment pressure and temperature as functions of time.

IV. RESULTS

The results obtained with this model are presented in this chapter. The first section provides a resume of the pertinent data and numerical assumptions employed in the analysis. Section 2 gives the results in tabular and graphical form. A sample calculation is undertaken in Appendix B.

1. <u>NUMERICAL DATA:</u>

a. Heat Transfer Slabs. The vapor container liner and internal components are represented as two slabs; one hot and one cold. Pertinent data for the two slabs are given in Table 1. These parameters, with the exception of the pre-rupture temperature, are held constant throughout the analysis.

TABLE I

Slab Data

Quantity	Units	Hot Slab	Cold Slab
Mass	lbs.	248,000	605,400
Surface Area	ft^2	1,991	8,049
Thickness	ft	0.2552	0.1544
Thermal Diffusivity	ft^2/hr	0.1863	0.4543
Heat Capacity	BTU/lb ^oF	0.11	0.11
Pre-rupture Temperature	^oF	500	120
Thermal Conductivity	BTU/hr ft ^oF	10	25
Heat Transfer Coefficient	BTU/hr ft^2 ^oF	10	30

b. Initial Conditions. Table II lists the pre-rupture inventory of primary fluid. The mass is 21,038.8 lbs and the total internal energy is 11.395×10^6 BTU. The containment vessel free volume is 23,016 ft^3 (see Table III) and contains 1575.2 lbs of air at 120^{o}F and 14.7 psia. The value of C_v for air is 0.1716 BTU/lb^{o}F.

c. Fission Product Decay Heat. The contribution to containment atmosphere from decaying fission products was evaluated by numerical integration of Eq. (18). Values for various time intervals after the rupture are given in Table IV. Conservative analysis (1) indicates that no metal-water reaction will occur before 2.5 minutes. Thus, the decay heat generated during the first 2.5 minutes is not added to the containment atmosphere but, instead is assumed to raise the stainless steel cladding to the reaction temperature.

TABLE II

Loop Volume and Energy Data

	Volume ft^3	Temperature ^oF	Mass, lb	Total Energy BTU
Reactor Loop	355	532	16,720.5	8,746,500
Pressurizer				
Water	95.96	587	4,141.2	2,454,500
Steam	53.33	587	177.1	194,000
			21,038.8	11,395,000

TABLE III

Containment Vessel Volume

Gross Volume

Hemispherical Ends	15,600 ft^3
Cylindrical Center Section	10,243
Main Access Nozzle and Cover	453
External Purge System	125
Grout Lines	32
	26,453

Displaced Volume (metal, cold water, and secondary side of steam generator including feed and steam lines)

Reactor Vessel, Internals and Rod Drives	229
Steam Generator (including secondary side and piping	359
Primary shield tank and surge tank	1500
Equipment Support Structure	190
Quench Tank	51
Pressurizer	35
Purification Demineralizer and Cooler	78
Reactor Coolant Pumps	11
Decay Heat Pumps, Sump Pumps and Buffer Seal Tank	5
Thermal Insulation	320
Containment Vessel Shield Plug	107
Containment Vessel Ring Frames	332
Main Loop Piping	18
Containment Cooler	12
Ring Frame Shielding	100
Upper Primary Shield Addition	49
Deck Grating and Support	37
Head Dolly	4
	3,437

Net Volume

26453
-3437
23016

TABLE IV

Values of Fission Product Decay Heat

Time Interval (minutes)			ΔQ_{fp} (BTU x 10^{-6})
0	-	2.5	0.2524
2.5	-	5.0	0.1913
5.0	-	7.5	0.1725
7.5	-	10.0	0.1597
10	-	15	0.2909
15	-	20	0.2701
20	-	25	0.2534
25	-	30	0.2413
30	-	60	1.3059
60	-	90	1.1480
90	-	120	1.0551
120	-	180	1.9259
180	-	240	1.7616
240	-	300	1.6480
300	-	360	1.5604
360	-	480	2.9206
480	-	600	2.7210
600	-	720	2.5644

d. Metal-water Reaction. The entire quantity of stainless steel cladding (569 kilograms) is assumed to react. The total reaction energy of .5713 x 10^6 BTU (569,000 gms x 253 cal/gm x .0039685 BTU/cal) is released homogeneously to the containment mixture in 12 equal increments spaced 2.5 minutes apart and beginning 2.5 minutes after the accident. This results in completion of the reaction in 27.5 minutes which is conservative compared to the criterion of a 0.05% per second reaction rate (33.3 minutes completion time).

The hydrogen evolved is assumed to burn in place as it is generated with the total recombination energy of 2.3763 x 10^6 BTU released to the containment mixture in the same manner as the reaction energy. The partial pressure of the oxygen consumed during the recombination process is removed from the total containment pressure after completion of the reaction. Credit is assumed for the fact that the recombination reaction is oxygen limited. Sufficient oxygen is present to combine with 80% of the hydrogen evolved in a 100% metal-water reaction. The remaining 20% hydrogen contributes its partial pressure to the total containment pressure.

For the calculations based on the core slumping model, the MH-1A core is divided into nine regions according to past-blowdown fuel temperature. Each region heats up adiabatically due to decaying fission products in that region. The cladding begins reacting when its temperature enters the reaction region and continues to react until sufficient heat has been added to melt the cladding. Data from the High Temperature Materials Program[17] is used for metal-water reactions for temperatures up to melting and data from reference 18 is used after reaching melting temperature and through the heat of fusion region. An unlimited supply of steam is available throughout the reaction. The reaction terminates when the cladding has melted and the stainless steel falls to the bottom of the pressure vessel and is quenched in residual water. An additional 5% of the unreacted stainless steel is assumed to react during the quenching process.

If the evolved hydrogen is assumed to collect in the containment vessel, recombination can occur when the appropriate concentrations of steam, air, and hydrogen are reached. The flammability limits for a mixture containing these three constituents are shown in Figure 6. The 300^{o}F - 100 psig limit line is used in this analysis. Calculations were performed for variable amounts of clad reacting (and, hence, hydrogen generated) up to 100%. The maximum amount of hydrogen that can react corresponds to an 80% cladding reaction. This quantity of hydrogen completely exhausts the containment oxygen. The recombination energy is transferred to the containment vessel steam, nitrogen, and that portion of the hydrogen or oxygen which does not react. The partial pressures of the reactants are removed after the combustion.

Following combustion, the superheated atmosphere rapidly loses heat to the containment vessel liner and internal components because of the large temperature difference between the atmosphere and components. Both slabs are "cold" relative to the atmosphere temperature. The model described in section (III.2.a) and the constants given in Table I are used to calculate the heat transfer to the slabs. An exception to the values listed in Table I is the use of a heat transfer coefficient of h = 4.0 BTU/hr. ft.^{2} ^{o}F for both slabs. Heat is also transferred to the water in the containment vessel. The water is conservatively not allowed to vaporize.

e. Emergency Injection Water. The injection water enters the containment vessel but does not reach the pressure vessel. The tank is pressurized five minutes after the LOCA and 200 gallons (of the 2600 gallons in the tank) are added to the containment vessel at a rate of 100 gpm. The water temperature is 100°F and specific internal energy is 69.70 BTU/lb.

f. Pressure Suppression Spray Systems. The least effective spray, the fresh water external spray system, is employed in the MHA analysis. A flow rate of 50 gpm is used beginning 15 minutes after the accident and the initial temperature is 85°F. Since the water does not vaporize (in this analysis), the final temperature is taken as 212°F. When the temperature of containment vessel liner drops below 212°F, the final temperature is taken as equal to the liner temperature.

Although the MHA considers only the fresh water external spray, additional analyses were performed in which the fresh water internal spray and salt water external spray were used. The latter system is treated identically to the fresh water external spray except for a flow rate of 300 gpm. The fresh water internal spray introduces 85°F water (specific internal energy of 53.21 BTU/lb.) to the containment vessel beginning 15 minutes after the LOCA. The flow rate is 50 gpm for containment pressures above 50 psia and 100 gpm for pressures below 50 psia.

2. RESULTS: The results of the various cases considered in this study are given in this section. The first section contains the results for the defined MHA and the second section presents the results of the supplemental studies.

a. MHA Results. The pressure and temperature of the containment vessel atmosphere as functions of time after the LOCA are listed in Table V. The time sequence of events is that described in section (II.3). The pressure-time variation is shown in Fig. 7 for the first two and one-half hours and in Fig. 8 for the first 100 hours. The temperature-time variation is shown in Figs. 9 and 10.

The peak temperature and pressure are 340°F and 138 psia, respectively, and occur immediately after the primary system rupture. The actual calculation of these quantities is discussed in Appendix B. The pressure and temperature decrease until values of 166°F and 18 psia are reached approximately 14 hours after the accident. They are assumed to remain constant at these values for the remainder of the accident. The first ten minutes in Table V contain "before" and "after" values at 2.5,

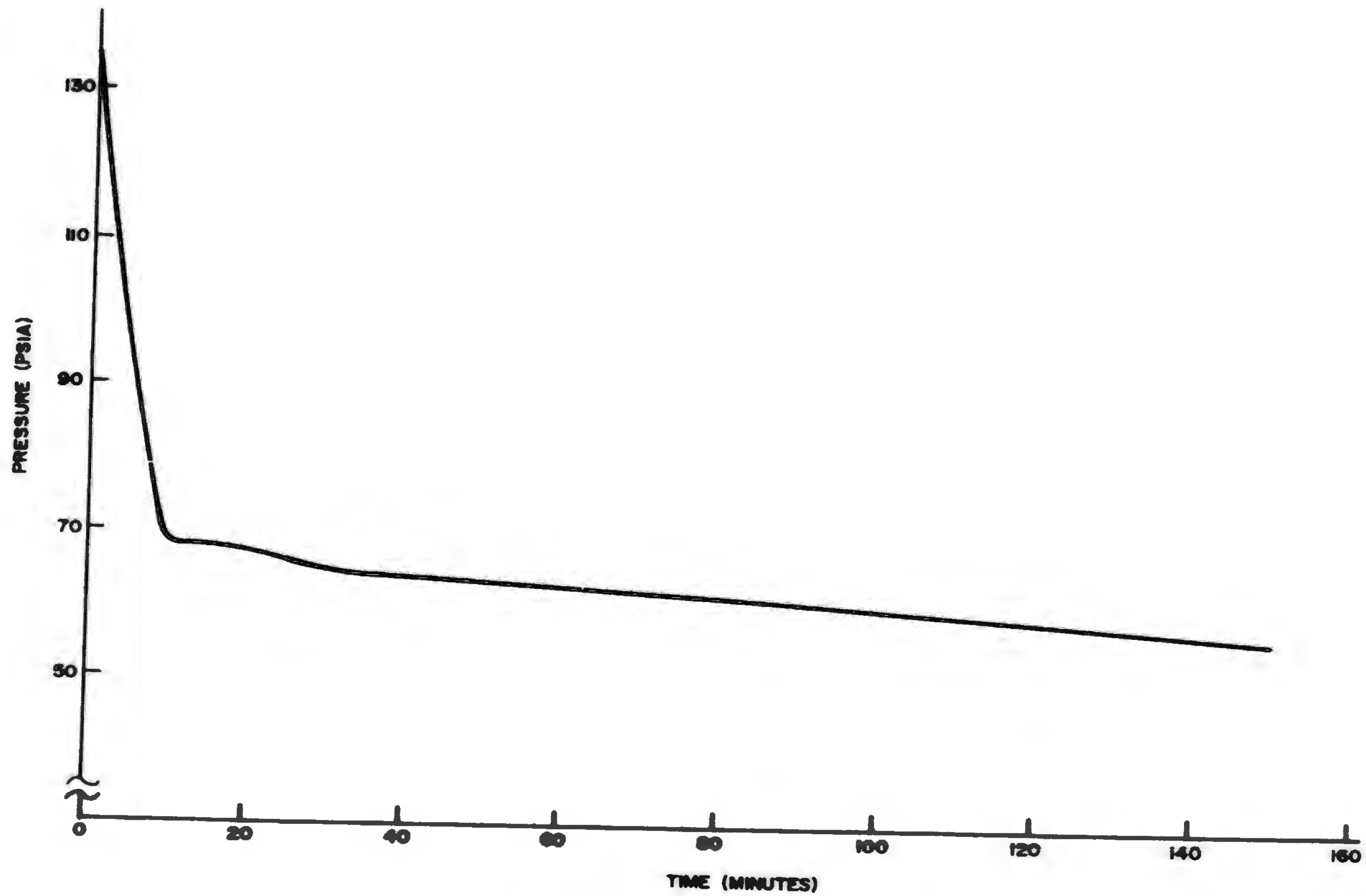

FIGURE 7
CONTAINMENT PRESSURE VERSUS TIME
(EXTERNAL FRESH WATER SPRAY, IN-PLACE HYDROGEN RECOMBINATION)

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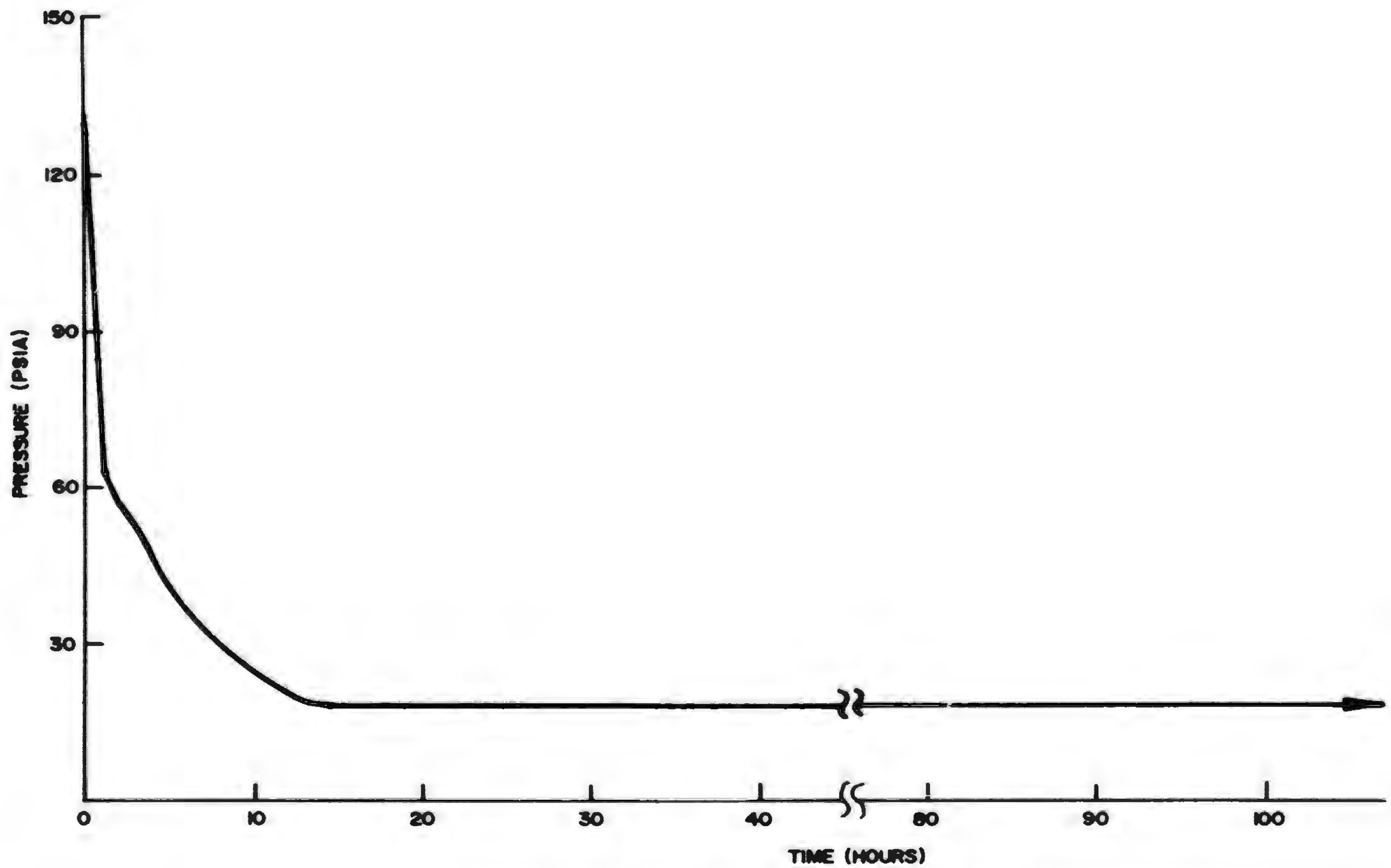

FIGURE 8
CONTAINMENT PRESSURE VERSUS TIME
(EXTERNAL FRESH WATER SPRAY, IN-PLACE HYDROGEN RECOMBINATION)

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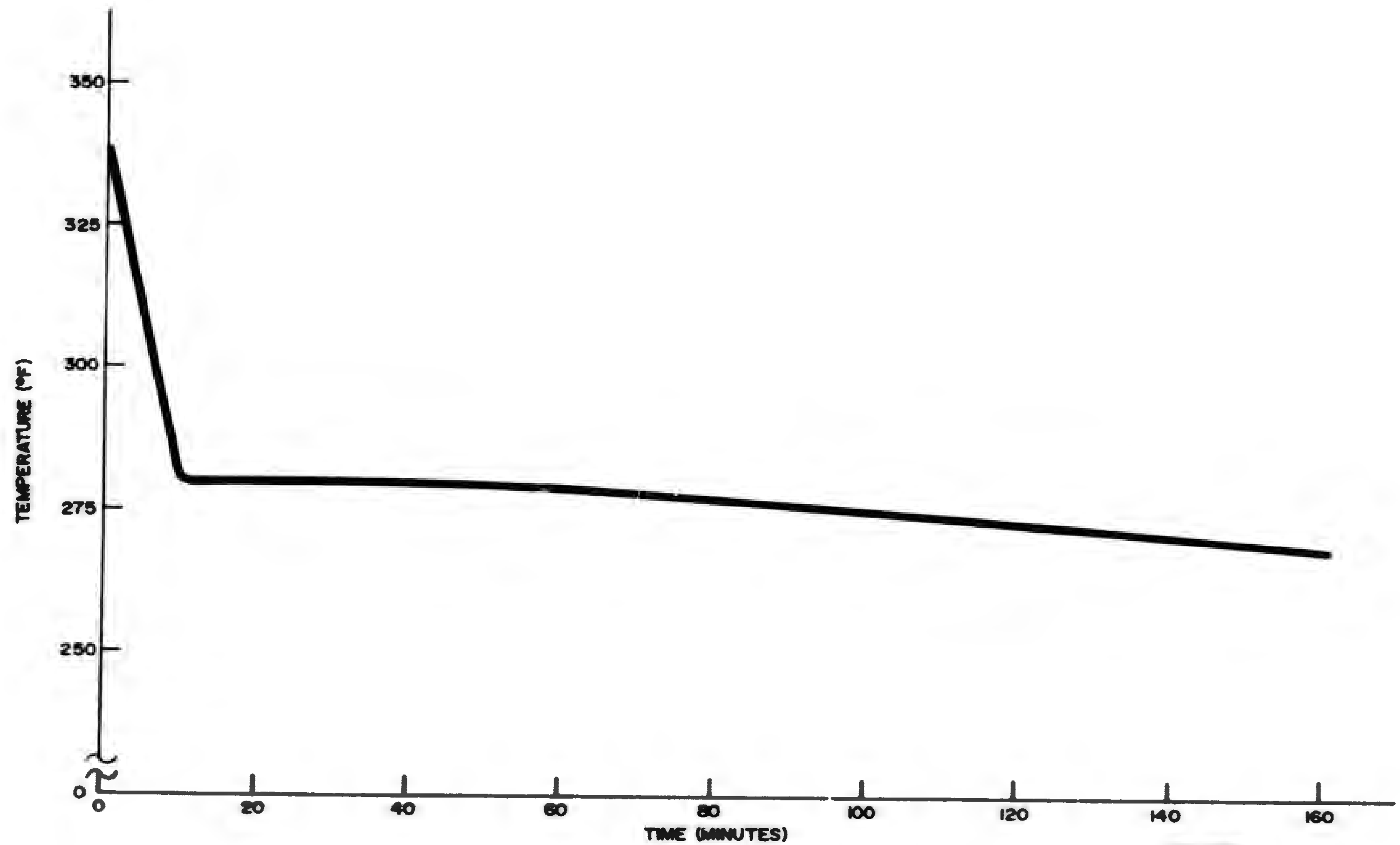

FIGURE 9
CONTAINMENT TEMPERATURE VERSUS TIME
EXTERNAL FRESH WATER SPRAY, IN-PLACE HYDROGEN RECOMBINATION

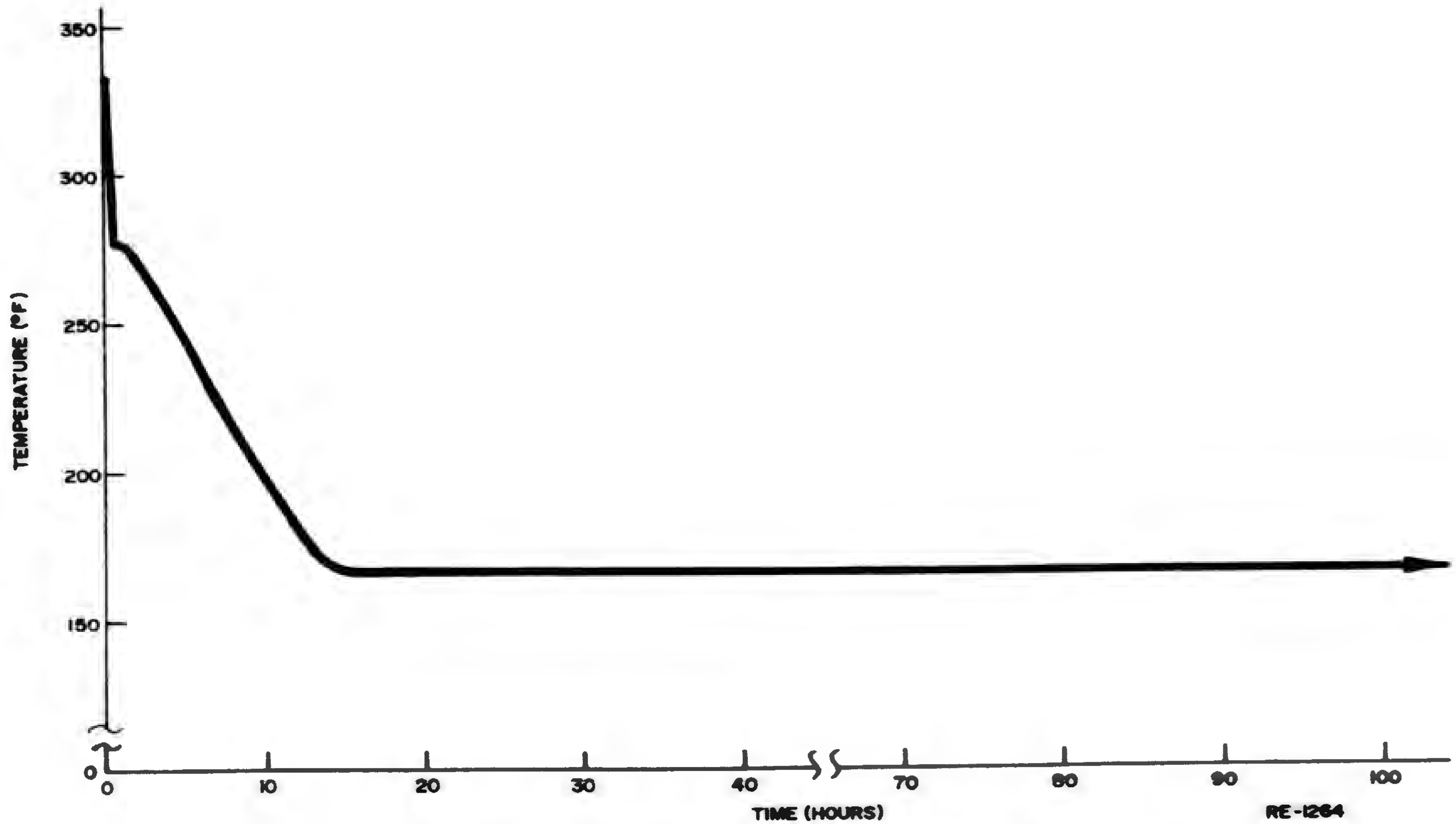

FIGURE 10
CONTAINMENT TEMPERATURE VERSUS TIME
EXTERNAL FRESH WATER SPRAY, IN-PLACE HYDROGEN RECOMBINATION

TABLE V

Containment Pressures and Temperatures

As Functions of Time for MHA

Time (Min)		Temperature (°F)	Pressure (psia)
0.0		340	138
2.5	(before)	316	105
2.5	(after)	323	113
5.0	(before)	302	89
5.0	(after)	309	96
7.5	(before)	292	79
7.5	(after)	293	79
10.0	(before	280	68
10.0	(after)	281	68
15		280	68
20		279	67
25		278	66
30		281	65
60		278	63
90		276	61
120		273	58
180		265	53
240		256	48
300		247	42
360		236	37
480		214	29
600		195	23
720		180	20
840		166	18

5.0, 7.5, and 10.0 minutes. The difference is explained as follows. After the peak conditions are established, a net amount of heat is removed from the atmosphere during the first 2.5 minutes via the two slabs. The temperature and pressure listed at 2.5 min (before) reflect this heat loss. They are obtained according to the model described in section (III.2.a) and illustrated in Appendix B. At the end of the time step, heat sources for the next 2.5 minute time step (fission product heat, metal-water reaction energy, and hydrogen recombination energy) are added to the containment mixture. Any heat sinks active over the time step just finished (none in this particular time step) are also added at this time. The addition of heat raises the temperature and pressure as can be seen at 2.5 min (after) in Table V. This same process is repeated for the next three time steps. Note that the pressures shown in Figs. 7 and 8 are "after" pressures.

At the end of 10 minutes (after) the containment temperature is 281°F which is very close to the final equilibrium temperature (T_{sf} of section (III.b.1)). Recall that T_{sf} is the temperature which would be attained by the containment vessel atmosphere and two slabs after a sufficiently long time (neglecting subsequent heat sinks and sources). Thus, it is assumed that the containment temperature and final equilibrium temperature are equal even though the two slabs are not yet in equilibrium with the containment atmosphere. This assumption is conservative because, after 10 minutes, the cold slab is still removing heat from the atmosphere faster than the hot slab is adding it.

b. Supplemental Results. The hydrogen combustion after accumulation in the containment vessel is treated according to Eq. (19). The peak temperature and pressure after combustion as a function of percentage of cladding reacted are shown in Fig. 11. The values shown occur immediately after the assumed combustion. Within five minutes the heat of combustion is completely redistributed among the containment vessel liner and all internal constituents, and the entire system is again at thermal equilibrium. Note in Fig. 11 that the peak temperature and pressure stay essentially constant for cladding percentages above 80% due to the exhaustion of containment oxygen.

The core slumping calculations, based on an adiabatic heat up model, resulted in a 55% cladding reaction. This figure includes the additional 5% reaction during the quenching process.

Additional calculations were performed to study the effectiveness of the pressure suppression spray systems. The MHA calculations (external fresh water spray) were repeated with the internal fresh water and external

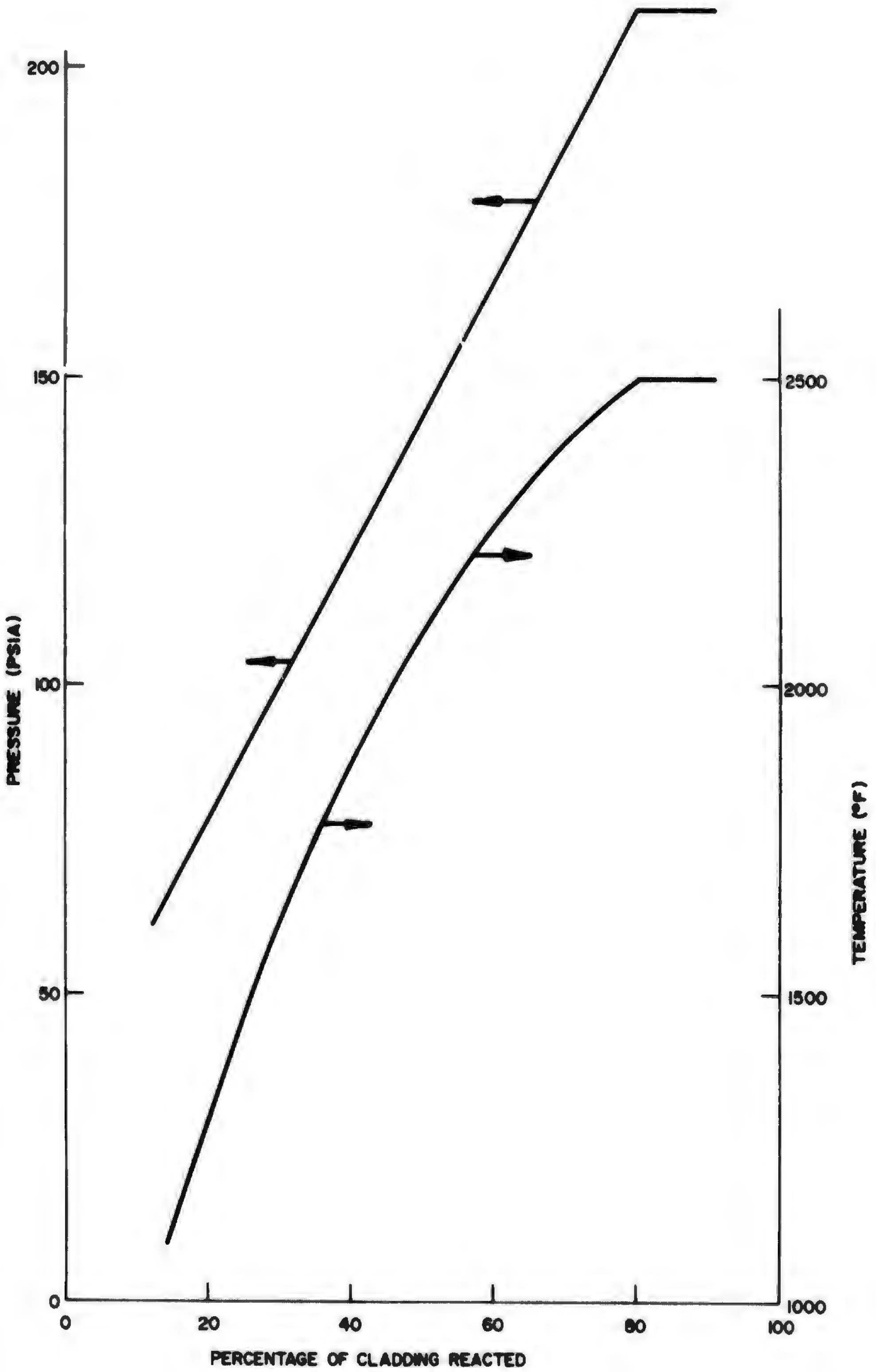

FIGURE 11. PEAK CONTAINMENT PRESSURE AND TEMPERATURE VERSUS PERCENTAGE OF CLADDING REACTED (HYDROGEN ACCUMULATION WITH SUBSEQUENT COMBUSTION)

RE-1291

salt water sprays considered operable. The initial pressure and temperature are the same as those for the MHA. However, they attain their final values of 18 psia and 160°F after only 75 minutes (compared to 14 hours for the MHA).

As a back up to the metal-water reaction rate used (0.05% per second), calculations were performed to determine the maximum possible reaction rate consistent with a design pressure of 155 psia. The total metal-water reaction energy (.5713 x 10^6 BTU) and energy from 80% hydrogen recombination (2.3763 x 10^6 BTU) were added homogeneously to the steam-water-air mixture in the rate calculations. The results indicated that an infitite reaction rate (step input) 2.5 minutes after the blowdown would yield a peak pressure and temperature of 162 psia and 355°F, respectively. A reaction rate of 2.8% per second (completion time of 35 seconds) would yield a peak pressure and temperature of 155 psia and 353°F, respectively. The latter reaction rate, which is approximately 60 times greater than the one conservatively used in the MHA analysis, is considered incredible but does, nevertheless, place an upper bound on the allowable metal-water reaction rate consistent with the MH-1A containment vessel design pressure.

V. DISCUSSION

There are three questions resulting from this analysis to which satisfactory answers must be supplied. First, can the containment vessel withstand the peak pressures predicted by the analysis? Second, will the peak temperatures obtained adversely affect the containment vessel, its penetrations, or the engineered safeguards. Third, does the leak rate derived from the pressure and temperature vs time curves represent an upper limit on the expected leak rate? The first question is answered by a discussion of allowable stresses in the containment, the second by a discussion of containment vessel and internal component temperature characteristics, and the third by a discussion of conservatism employed in the MHA analysis.

1. CONTAINMENT VESSEL INTEGRITY: References 1 and 8 present a detailed description of the containment vessel design and construction including the electrical and hydraulic penetrations and all access ports. A brief summary of this design follows.

The containment vessel is an all welded assembly in the shape of a right, circular cylinder with hemispherical ends. The cylindrical portion is 31 feet in diameter by 13 feet 7 inches in length. Total length of the vessel is 44 feet 7 inches. The vessel was designed and constructed as an unfired pressure vessel in accordance with Coast Guard Regulation CG-115 and Section VIII of the ASME Boiler and Pressure Vessel Code. The basic allowable stress used was S_A = 17,500 psi with temperatures of $-20^{o}F$ to $650^{o}F$. The vessel was manufactured from SA212, Grade B carbon steel having a specified yield strength of 38,000 psi, a minimum tensile strength of 70,000 psi, and an elastic modulus of 28 x 10^6 psi. The vessel and its penetrations were designed to withstand an internal pressure of 155 psia and an external pressure corresponding to 150 feet of submergence. The vessel was tested at a hydro-static pressure of 205 psia while prototypes of each type of electrical penetration were tested to pressures of 270 psia with no leakage.

As described in the Containment Vessel Design Report (Reference 9) a number of reinforcements were added to the vessel in order to meet the 150 feet of water submergence criterion. None of these reinforcements were taken into account in the internal pressure calculations.

The required internal design pressure is 155 psia which results in a stress equal 46% of the yield stress. Since the peak accident pressure is 138 psia, it is concluded that the integrity of the containment vessel is not impaired by the pressures resulting from the MHA.

2. <u>TEMPERATURE EFFECTS</u>. As stated in section (IV. 2), the maximum temperature of the containment vessel atmosphere during the MHA is $340^{o}F$ and occurs at the end of the primary system blowdown. Since the blowdown is assumed to be instantaneous, this represents a conservative upper limit on the temperature of the containment vessel atmosphere. In reality, the primary system blowdown will occur over a period of about 10 to 15 seconds during which time the pressure and temperature of the containment vessel atmosphere will increase. The peak temperature and pressure will be less than those predicted by the instantaneous blowdown model due to the effect of heat sinks over the actual blowdown time.

The effects of this maximum temperature on the integrity of (1) the containment vessel, (2) its penetrations and (3) the engineered safeguards that are required to operate following the onset of a MHA have been investigated and are discussed below.

a. Containment Vessel. The design temperature range of the containment vessel is -20°F to 650°F for an allowable stress of 17,500 psi. Since the containment vessel acts as a heat sink during the MHA, it is concluded that its maximum temperature will always be less than that of the maximum containment vessel atmosphere temperature of 340°F. Thus, the vessel temperature is always within its design range and its integrity is not affected by the temperature conditions during the MHA.

b. Penetrations There are three types of penetrations in the MH-1A containment vessel; namely, piping and/or hydraulic penetrations, electrical penetrations, and access ports.

The pipe penetrations (including welds) are designed to withstand temperatures of 350°F and greater. Typical pipe penetration designs are given in Chapter III of the Final Safeguards Report (Reference 1).

The electrical penetrations each have connectors with conductor pins or terminals attached to the connector by fused glass seals. All the solder joints within the connector are capable of withstanding temperatures greater than 400°F. Prototypes of each type of multiconductor penetration were tested to 365°F and 270 psia with no leakage over an eight hour period.

There are two access openings in the containment vessel; the main access port (for refueling and major maintenance operations) and the personnel access port (for normal entry). The main access port is sealed with a rubber-asbestos material capable of withstanding temperatures up to 700°F. The personnel access port is sealed with silicone rubber which can withstand temperatures up to 450°F.

c. Engineered Safeguards. As described in section (II.2), the following safeguards systems are assumed to operate during the MHA: Containment Isolation Valves, Emergency Injection System, Containment Vessel External Fresh Water Spray System and the Purge System. With the exception of the External Fresh Water Spray, which is manually operated, all the systems are automatically activated by the primary coolant pressure detectors (designed for 617°F and 1750 psia) or by the high containment pressure switches (designed for 350°F and 165 psia). Each of the safeguards systems can also be manually activated. However, once activated the system circuits "lock in" and can only be manually deactivated. All the circuitry associated with the automatic and/or manually operation of these systems is located external to the containment vessel and is thus not affected by the conditions inside the containment vessel.

The components of the external purge systems are not affected by the conditions inside the containment vessel. The internal fan and motor are enclosed in an armored casing and are designed to operate under conditions of 165 psia and 350°F.

Based upon the foregoing summary of the design limitations and test conditions of the vessel, its penetrations, and safeguards systems, it is concluded that the containment vessel integrity will not be violated as a result of the temperatures (and associated pressures) following a MHA.

3. CONSERVATISM. An exact, analytical description, in space and time, of the containment vessel temperature and pressure after a MHA is an extremely difficult problem. To date, it has been beyond the capability of available techniques.

Thus, one approximates the real process by a model to which a solution can be obtained with a reasonable amount of time and effort. At the same time, one strives to insure that the assumptions used in the approximation do not lead to under prediction of the pressure and temperature magnitudes or over prediction of the rate of decay. This is where the philosophy of conservatism enters the picture. By selecting appropriate values of parameters and rates of heat introduction or removal, one can confidently predict that the calculated pressure and temperature over a time period of interest represent an upper bound on the expected values. For example, overestimating the effect of a heat source and underestimating the effect of a heat sink are conservative application of these quantities.

It would be cumbersome to list each individual conservatism used in this analysis. The great majority of them are either pointed out as used or inherently obvious from the context. Furthermore, the relative effect of one conservatism versus another is often difficult to assess. But, taken as a whole, the conservatism employed is sufficient to back up the assertion that the results obtained do represent an upper limit on the expected containment vessel pressure and temperature after a MHA.

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APPENDIX A

Derivation of Slab Temperature Distribution

This appendix contains the solution to the one dimensional, time-dependent heat conduction equation subject to a specified set of initial and boundary conditions. Justification for use of this model is given in Section (III. 2). The solution is based on information contained in reference 4.

Consider a slab of thickness a, surface area A, mass M, density p, heat capacity C_p, and thermal conductivity k. The surface dimensions are very large compared to the thickness so that heat conduction can be treated as one dimensional. One side of the slab is perfectly insulated. The entire slab is initially at temperature T_{so}. At time t = 0 the slab is immersed in an atmosphere at a temperature greater than T_{so} and the temperature of the non-insulated slab surface is assumed to increase exponentially to the final temperature eventually attained by the entire slab.

Quantitatively we solve the equation:

$$\frac{\partial T_s(x,t)}{\partial t} = \frac{k}{\rho c_p} \frac{\partial^2 T_s(x,t)}{\partial x^2} \equiv \alpha^2 \frac{\partial^2 T_s(x,t)}{\partial x^2}, \qquad \text{(A. 1)}$$

subject to the conditions:

$$T_s(x,o) = T_{so}, \qquad \text{(A. 2a)}$$

$$\frac{\partial T_s(a,t)}{\partial x} = 0, \qquad \text{(A. 2b)}$$

$$T_s(o,t) = T_{so} + \Delta T_s(1 - \exp\left[-t/\beta\right]). \qquad \text{(A. 2c)}$$

The slab decay constant β is determined as part of the procedure outlined in section (III. 2. e).

The first step in the solution is to solve Eq. (A. 1) for a slab initially at a temperature of zero and whose non-insulated surface is subjected to a step increase of unity at time t = 0. Quantitatively, the conditions are:

$$T_s(x, o) = 0, \tag{A. 3a}$$

$$\frac{\partial T_s(a, t)}{\partial x} = 0, \tag{A. 3b}$$

$$T_s(o, t) = 1. \tag{A. 3c}$$

The temperature distribution is expressed as the sum of two distributions, one of which represents the steady-state distribution, and the other of which represents the transient distribution (which approaches zero as t approaches infinity):

$$A_s(x, t)^* = T_{ss}(x) + T_T(x, t). \tag{A. 4}$$

The steady-state solution must satisfy Eqs. (A. 3b) and (A. 3c) and thus:

$$T_{ss}(x) = 1. \tag{A. 5}$$

The transient solution is a particular solution to Eq. (A. 1) which must satisfy the boundary conditions $T_T(o, t) = 0$ and $T_T'(a, t) = 0$ as well as the limiting conditions $T_T(x, \infty) = 0$. Product solutions of the form,

$$T_T(x, t) = X(x)\ T(t), \tag{A. 6}$$

---

* The notation $A_s(x, t)$ is adopted to avoid confusion in notation later in the appendix.

are assumed and, inserting Eq. (A.6) in Eq. (A.1), we have:

$$\frac{T'}{T} = \alpha^2 \frac{X''}{X} = -\gamma^2. \tag{A.7}$$

The general solutions are:

$$T(t) = Ae^{-\gamma^2 t} + Be^{\gamma^2 t}, \tag{A.8a}$$

$$X(x) = C \sin \frac{\gamma x}{\alpha} + D \cos \frac{\gamma x}{\alpha}. \tag{A.8b}$$

The condition $T_T(x, \infty) = 0$ means that the coefficient B = 0; the condition $T_T(o, t) = 0$ means that D = 0; and the condition $T'_T(a, t) = 0$ means that

$$\frac{\gamma C}{\alpha} \cos \frac{\gamma a}{\alpha} = 0,$$

or,

$$\gamma = \frac{n \pi \alpha}{2a}; \quad n = 1, 3, 5, \ldots \tag{A.9}$$

Defining $\lambda = (2A/\pi\alpha)^2$ and combining the results of Eqs. (A.4) - (A.9), we have:

$$A_s(x,t) = 1 + \sum_{n \text{ odd}} A_n \sin \frac{n\pi x}{2a} \exp(-\frac{n^2 t}{\lambda}). \tag{A.10}$$

The coefficients A_n are determined from the initial condition (Eq. (A.3a));

$$T(x,0) = 0 = 1 + \sum_{n \text{ odd}} A_n \sin \frac{n\pi x}{2a}. \tag{A.11}$$

Multiplying Eq. (A.11) by $\sin \frac{m\pi x}{2a}$ and integrating the result over the slab thickness, the expression for A_n (after invoking the orthogonality of the functions $\sin \frac{n\pi x}{2a}$ is:

$$A_n = \frac{4}{n\pi} . \tag{A.12}$$

Thus, the solution to Eq. (A.1) subject to Eqs. (A.3a) - (A.3c) is:

$$A_s(x,t) = 1 - \frac{4}{\pi} \sum_{n \text{ odd}} \frac{1}{n} \sin \frac{n\pi x}{2a} \exp\left(- \frac{n^2 t}{\lambda}\right). \tag{A.13}$$

The second and final step in the solution is obtained with the help of the superposition integral (4). This integral states that if the temperature of the non-insulated slab surface varies with time as F(t), the slab temperature distribution is given by:

$$T_s(x,t) = C_1 + F(0)\, A_s(x,t) + \int_0^t A_s(x, t-\tau)\, F'(\tau)\, d\tau , \tag{A.14}$$

where C_1 is a scale factor to account for a non-zero initial slab temperature ($C_1 \equiv T_{so}$), $F(t) = \Delta T_s(1 - \exp\left[-t/\beta\right])$, and $A_s(x,t)$ is given by Eq. (A.13). Performing the indicated operations in Eq. (A.14) yields the desired result:

$$T_s(x,t) = T_{so} + \left[\Delta T_s(1 - \exp\left[-t/\beta\right]) - \frac{4}{\pi} \sum_{n \text{ odd}}^{\infty} \sin \frac{n\pi x}{2a} \right.$$

$$\left. \frac{\exp(-t/\beta) - \exp(-n^2 t/\lambda)}{n(n^2(\beta/\lambda) - 1)} \right] . \tag{A.15}$$

APPENDIX B

Numerical Methods

This appendix presents sample calculations to illustrate application of the method. Calculations of the initial temperature and pressure, equilibrium temperature, and heat exchange between slab and atmosphere for the MHA are described.

1. INITIAL TEMPERATURE AND PRESSURE. These quantities are determined according to Eqs. (7) and (8):

$$M\bar{u}_o = \Delta Q_{air}(T_{a1}) + Q_{ws}(T_{a1}, M), \tag{7}$$

$$P_{air_1} = P_{air_o}\left[\frac{T_{a1} + 460}{T_{ao} + 460}\right]. \tag{8}$$

The left side of Eq. (7) (in units of 10^6 BTU) is 11.3950. The right side of Eq. (7) is evaluated at successive values of T_{a1} until the input energy is bracketed, e.g.,

at $T_{a1} = 340.62^\circ F, P_{ws} = 119.0$ psia,

$Q_{ws} = 11.3704$

$Q_{air} = .0596$

11.4300 high

at $T_{a1} = 339.99^\circ F$, $P_{ws} = 118.0$ psia,

$Q_{ws} = 11.3218$

$Q_{air} = .0595$

11.3813 low

By linear interpolation, the temperature and steam partial pressures are:

$$T_{a1} = 340.2^{o}F$$

$$P_{ws_1} = 118.2 \text{ psia.}$$

The air partial pressure is,

$$P_{air_1} = 14.7 \times \frac{340.2 + 460}{120 + 460} = 20.3 \text{ psia,}$$

and the total pressure is,

$$P_1 = 118.2 + 20.3 = 138.5 \text{ psia.}$$

The calculation of $\Delta Q_{air}(T_{a1})$ is straight forward and is done by hand. The calculation of $Q_{ws}(T_{a1}, M)$ is also straight forward but more time consuming. Thus, a computer code, HEAT CONTENT, was written to facilitate the calculation. The code contains a library of specific internal energies and volumes (taken from the steam tables) and, for input values of containment free volume (V) and primary fluid mass (M), calculates and prints out in tabular form values of Q_{ws} over a wide range of values of atmosphere temperature T_{a1}.

2. EQUILIBRIUM TEMPERATURE. The equilibrium temperature, T_{sf}, is used in the calculation of heat exchange between the atmosphere and a slab. It is defined as the temperature which would eventually be attained by the steam-water- air mixture, hot slab, and cold slab for a given heat input.

Hence,

$$Mu_o = \Delta Q_{air} + Q_{ws} + \Delta Q_{\substack{cold \\ slab}} - \Delta Q_{\substack{hot \\ slab}},$$

where (in units of 10^6 BTU),

$$\Delta Q_{air} = (MC_v)_{air} (T_{sf} - 120) = .00027 (T_{sf} - 120),$$

$$Q_{ws} = M(u_w + \frac{V/M - v_w}{v_{ws}} u_{ws}),$$

$$\Delta Q_{\substack{cold\\slab}} = (mC_p)_{cs} (T_{sf} - 120) = .68410 (T_{sf} - 120),$$

$$\Delta Q_{\substack{hot\\slab}} = (mC_p)_{hs} (500 - T_{sf}) = .27280 (500 - T_{sf}).$$

Successive values of T_{sf} are tried until the value of $M\bar{u}_o$ is bracketed, e.g.,

at $T_{sf} = 273.05^\circ F$,

+ 0.0414	
+ 7.1004	
+10.4702	
- 6.1912	
11.4208	high

at $T_{sf} = 271.64^\circ F$,

+ 0.0410	
+ 7.0297	
+ 10.3737	
- 6.2297	
11.2147	low

By linear interpolation, the equilibrium temperature is:

$$T_{sf} = 272.9^\circ F.$$

3. HEAT EXCHANGE BETWEEN SLABS AND ATMOSPHERE. This procedure is illustrated for the first time step of the MHA Values of the slab parameters are given in Table I. A time step of 2.5 minutes is used and a value of T_{a2} = 320.2°F (temperature of atmosphere at end of 2.5 minutes) is assumed.

a. Cold Slab. A value of β is to be determined such that Eqs. (14) and (17) both predict the same quantity of heat transferred to the slab. The quantity $F_s(\tau, \beta, \lambda)$, which appears in Eq. (15), is time consuming to evaluate. Thus, a computer code, HEAT SLAB, was written to expedite the calculations. The code requires values of λ and τ as input and calculates and prints out values of $F_s(\tau, \beta, \lambda)$ over a wide range of values of β. Table VIII contains values of $\Delta Q_s(\tau)$, in units of 10^6 BTU, from Eqs. (14) and (17) for several values of β.

TABLE VI

$\Delta Q_s(\tau)$ for the Cold Slab

β	Eq. (14)	Eq. (17)
.04440	4.39117	1.57424
.12910	1.89452	1.89105
.12945	1.89004	1.89159
.13190	1.85924	1.89532

The value of β which satisfied both equations is .1292 hrs and the consistent value of $\Delta Q_s(\tau)$ is 1.8914 x 10^6 BTU.

b. Hot Slab. The hot slab is treated in the same way as the cold slab. Table IX gives values of $\Delta Q_s(\tau)$ for several values of β.

TABLE VII

$\Delta Q_s(\tau)$ for the Cold Slab

β	Eq. (14)	Eq. (17)
.20960	0.29588	0.12334
.34890	0.18333	0.13007
.41855	0.15400	0.13181
.4870	0.13310	0.13306
.4882	0.13279	0.13307

The consistent set of values is,

$$\Delta Q_s(\tau) = 0.1331 \times 10^6 \text{ BTU},$$

$$\beta = 0.4870 \text{ hr.}$$

c. Heat Content of Atmosphere. The guessed value of T_{a2} (320.2°F) also determines the heat content of the steam-water-air mixture. In other words, the values of $\Delta Q_s(\tau)$ for the hot and cold slabs must satisfy Eq. (9) for the assumed value of T_{a2}:

$$M\bar{u}_o \overset{?}{=} \Delta Q_{\substack{cold\\slab}} - \Delta Q_{\substack{hot\\slab}} + \Delta Q_{air}(T_{a2}) + Q_{ws}(T_{a2}). \qquad (9)$$

For $T_{a2} = 320.2^\circ$F, we have

$$11.3950 \overset{?}{=} 1.8914 - 0.1331 + 0.0541 + 9.8760$$

$$= 11.6884 \qquad \underline{\text{high}}$$

Thus, the assumed value of T_{a2} is too high and the analysis must be repeated for a lower value of T_{a2}. The result, for $T_{a2} = 316.2^{o}F$, is

$$11.3950 \overset{?}{=} 1.8739 - 0.1346 + 0.0530 + 9.6110$$

$$= 11.4033 \qquad \underline{\text{high}}$$

Since the second guess is very close to satisfying Eq. (9), it is reasonable to extrapolate to the correct result. Therefore, we have:

$$T_{a2} = 316.1^{o}F,$$

$$P_{ws_2} = 84.8 \text{ psia},$$

$$P_{air_2} = 19.7 \text{ psia},$$

$$P_2 = 104.5 \text{ psia},$$

as the values at the end of the first time step.

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13. ABSTRACT The Maximum Hypothetical Accident for the MH-1A nuclear reactor has been reanalyzed. The variation with time of the containment vessel temperature & pressure has been obtained.

The model used is intentionally conservative. Following a double-end shear in one of the primary system pipes, the entire quantity of primary system fluid is instantaneously exposed to the containment vessel free volume & thermal equilibrium is attained among the primary fluid (saturated water-steam) and containment vessel air. The temperature & pressure of the containment vessel atmosphere then change with time according to the contribution of various heat sources & sinks. The effects of the containment vessel liner & internal components, fission product decay heat, metal-water reaction, hydrogen recombination, emergency core cooling systems, and pressure suppression spray systems are taken into account.

Regarding the metal-water reaction, 100% of the cladding is assumed to react with steam. The evolved hydrogen is assumed to burn in place as it is generated.

The peak pressure of 138 psia occurs immediately after the rupture & is within the design pressure of 155 psia. The peak atmosphere temperature of 340°F also occurs immediately after the rupture. It is shown that this temperature does not impair the integrity of the containment vessel or its penetrations nor induce any unsafe malfunctions in the engineered safeguards systems.

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