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**METAL-WATER REACTIONS -
EFFECTS ON CORE COOLING
AND CONTAINMENT**

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ATOMIC POWER EQUIPMENT DEPARTMENT

October 23, 1968

Dr. Peter A. Morris, Director
Division of Reactor Licensing
U. S. Atomic Energy Commission
Washington D. C. 20545

Dear Dr. Morris:

**SUBJECT: General Electric Topical Report APED-5454, Metal Water Reactions -
Effects on Core Cooling and Containment.**

The ACRS letters of November 24, 1965, for Dresden Unit 2 and March 18, 1966, for the Millstone Nuclear Power Station suggested that the General Electric Company provide the results of development programs pertaining to metal-water reactions.

In response to those recommendations the General Electric Company has obtained metal water test results utilizing heated Zircaloy rods in typical accident environmental conditions and has compared those results with other data in the technical literature. These data have been utilized to evaluate the extent of the metal-water reaction in a General Electric Boiling Water Reactor under the core heatup conditions following a loss of coolant accident.

It is concluded that although the metal-water reaction phenomenon must be considered in sizing the equipment to be utilized following a postulated loss of coolant accident, the magnitude of the integrated metal water reaction is small and is not an overriding consideration in design of the facility.

Transmitted under separate cover are one hundred twenty five (125) copies of the subject report which are to be placed in the Public Document Room and identified as a General Electric Topical Report.

Sincerely yours,

S. Levy
S. Levy, Manager
Design Engineering
Mail Code 613

b1h

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Approved: A P Bray
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METAL-WATER REACTIONS - EFFECTS ON CORE COOLING AND CONTAINMENT

P. W. Ianni

ABSTRACT

The role of metal-water reactions in the evaluation of reactor loss-of-coolant accidents is discussed. It is shown that the amount of metal-water reaction expected to accompany a BWR loss-of-coolant accident is insignificant even if only one of the two core cooling systems provided functions. Test results conducted on Zircaloy and inductively heated Zircaloy rods supporting these calculations are summarized. The inherent capability of the pressure

suppression containment to accommodate postulated metal-water reactions is far in excess of the amount calculated regardless of the assumptions made concerning hydrogen burning. It is concluded that even though the metal-water reaction phenomenon must be considered in emergency core cooling system sizing, the integrated metal-water reaction occurring is not an overriding consideration in loss-of-coolant accidents.

SECTION I

INTRODUCTION

Following a postulated loss of coolant from a reactor core and subsequent core uncover, the core heats up as determined by decay heat. Depending on the temperature range, the Zircaloy-steam reaction can significantly affect the heatup rate and, hence, affect (1) the sizing of the emergency core cooling systems (ECCS) provided to prevent cladding melting, or (2) the temperature reached before the rise is terminated by the core cooling systems. The hydrogen released as a by-product can also influence the containment response if in sufficient quantities, the concern being the potential for rapid combustion. Also, the energy associated with large quantities of metal-water reactions cannot be neglected. The models used to evaluate the core heatup and metal-water reactions will be discussed as well as

their influence on equipment sizing and design evaluations.

The containment has an inherent capacity to tolerate a significant percentage reaction of the core by virtue of its free volume and high heat-storage capacity. Consistent with the high degree of redundant core cooling network provided, core melting will be prevented and, therefore, only a fraction of a percent of the core is expected to undergo a metal-water reaction. The various considerations regarding containment inerting are discussed. An arbitrary model which addresses itself only to the ability of the containment to tolerate metal-water reactions is described. This provides an index or measure of the ability of the containment to accommodate metal-water reactions. It is concluded that an inert containment atmosphere is not a necessity.

SECTION II

METAL-WATER REACTION - BASIC CONSIDERATIONS

2.1 BASIC REACTIONS

Chemical reactions between metals and steam or/and water of interest here can occur

for zirconium and stainless steel. These reactions are usually of the form:

For zirconium, the more important of the core materials, 2808 Btu/lb Zr are released along with 0.022 mole of hydrogen per pound of zirconium. For stainless steel, 279 Btu/lb are released and 0.023 mole of hydrogen per pound of stainless steel. Since Zircaloy is by far the most important constituent of the core, this report deals predominantly with the zirconium-water reaction itself.

A typical large, 3440 MWt General Electric boiling water reactor core contains 133,000 pounds of Zircaloy, 370,000 pounds of UO_2 and 25,000 pounds of stainless steel.

The rate of reaction is important in assessing the importance of metal-water reactions. Experimental measurements of the oxygen uptake has established that the reaction rate can be described by:

$$W^2 = 33.3 \times 10^6 t \exp\left(\frac{-45,500}{RT}\right), \quad (2)$$

where:

W = Weight gain, mg/cm²;
t = Time, seconds;
R = Universal gas constant; and
T = Temperature, °C.

The parabolic rate constant, K, as determined by Baker⁽¹⁾ is generally accepted as a reasonable value so that

$$W^2 = K_p t. \quad (3)$$

Figure 1 shows the relationship for Zircaloy.

The above relationship assumes that the diffusion process limits the reaction rate and implies that the steam is available at least in stoichiometric proportions. If this is not so,

as is the case in practical reactor applications, the reaction rate will be limited by steam availability. This is an important aspect since the metal-water reaction rate at temperatures above 2000°F for zirconium is sufficiently rapid that the heat thus produced, rather than the decay heat, clearly dominates the rate of core heatup. This is seen in Figure 2 which illustrates typical core heatup and metal-water reaction curves for the design basis loss-of-coolant accident.

2.2 Controlling Considerations

Practically, it is found that during heatup without emergency systems operating, the only source of steam is that due to boil-off at the bottom of the pressure vessel. The amount of steam which can thus be released, rather than the diffusion process, is found to be the limiting factor with respect to reaction rate. Furthermore, the pressure drop which must exist across the core to induce even that quantity of steam along with the hydrogen produced is far higher than is possible by natural convection. Thus, in a realistic situation the amount of metal-water reaction which can be expected will be considerably lower than simplified, conservative estimates which assume a diffusion-limited reaction.

Because a temperature threshold exists, beyond which the metal-water reaction dominates the heatup, it is important that the proper cladding temperature be determined at the end of blowdown since this will have a significant effect on when the cladding begins to melt. This, of course, involves proper assessment of the heat transfer during the blowdown itself.

A third consideration is that, whereas most experiments are isothermal, the conditions in the reactor are not. Thus, to a certain extent the heatup within the core is a transient in both space and time, and requires the use of a relatively sophisticated

FIGURE 1. PARABOLIC RATE CONSTANT FOR ZIRCALOY

FIGURE 2. FUEL ROD CLADDING TEMPERATURE VERSUS TIME AFTER ACCIDENT

heat transfer model of the core heatup process. This, in turn, must be coupled with the flow availability as well as the pressure drop available across the core if a realistic calculation is desired.

It is nearly impossible to maintain isothermal conditions in the core except for a short period. Either the regions undergoing rapid reaction heat up at increasing rates until melting occurs, or cooling is sufficient to terminate the temperature rise.

The foregoing factors influence two key parameters; i.e., the core heatup rate which establishes ECCS sizing, and the hydrogen released, which influences the containment.

2.3 Analytical Evaluations - Extent of Modeling

The modeling of the metal-water reaction must necessarily fit into the framework of the core heatup model employed.

The fuel thermal transient during and after the blowdown is analyzed using a digital computer code which treats the core as five radial zones with five axial nodes per zone. Each radial zone is further divided into four zones. This allows accurate modeling of the power distribution by including the axial, radial, and local rod peaking factors. The fuel volume within the cladding is also nodalized radially. Decay power, stored energy in the core, the energy contributions of any chemical reaction which may occur, and the thermal radiation between fuel rods and fuel channels are considered in the code. It also accounts for any heat removed during the blowdown as a function of over-all time varying-heat transfer coefficient. The code calculates the cladding and channel temperatures, the degree of metal-water reaction, hydrogen and energy releases, and other parameters of interest on a nodal basis as a function of

time. The fuel channels are included and are treated on a nodal basis.

The basic assumption involved in the core heatup model employed by General Electric is that the core geometry remains unchanged during the heatup. Since the purpose of the model is to bound conservatively the time at which core melting begins, this assumption is a good one. After core melting and disassembly start, the confidence which can be placed in an analytical model becomes severely limited. Thus, with a known geometry, both heat transfer and metal-water reactions are amenable to analytical description.

In the current General Electric model, an unlimited supply of steam is assumed to exist and, thus, the metal-water reaction is governed by Baker's equation. This assumption conservatively neglects the limited steam availability as well as the limited pressure drop across the core with which to cause the steam to flow into the regions of interest.

At each fuel cladding node the chemical energy from the reaction is calculated along with the change in oxide thickness:

$$\Delta t_0 = \frac{B \exp^{-\frac{D_2}{\theta_c}}}{t_0} = \Delta t \quad (4)$$

where:

- Δt = Time step, seconds;
- Δt_0 = Change in oxide thickness, ft;
- θ_c = Cladding temperature, °R;
- B = Rate Constant, ft²/sec;
- D_2 = Rate constant, °F; and
- t_0 = Oxide thickness ft.

The chemical energy and hydrogen generated are proportional to the change in oxide thickness:

$$Q = \Delta t_o H \rho_c \quad (5)$$

where:

Q = Heat flux, Btu/ft²;
 H = Heat of reaction, Btu/lb;
 ρ_c = Cladding density lb/ft³;

and:

$$\Delta W_{H_2} = \Delta t_o \rho_c 2 \frac{N_{H_2}}{N_{Zr}} (R + t_c) 2\pi L, \quad (6)$$

where:

ΔW_{H_2} = Weight of H₂, lb;
 N_{H_2} = Molecular weight of H₂ = 2.0;
 N_{Zr} = Molecular weight of Zr = 91.22;
 R = Fuel radius, ft;
 Δt_o = Change in oxide thickness, ft;
 L = Node length, ft; and
 t_c = Cladding thickness, ft.

A similar calculation is performed on the Zircaloy fuel channels at each node. A heatup curve calculated with this code is shown in Figure 2. Note that only 0.6 percent of all the Zircaloy in the core is predicted to react during the period the fuel is not cooled by the emergency core cooling systems.

SECTION III

INFLUENCE OF METAL-WATER REACTIONS ON DESIGN EVALUATIONS

3.1 EMERGENCY CORE COOLING

3.1.1 Criteria and Events Evaluated

The effect of metal-water reactions on core cooling must be understood within the context of the core cooling criteria. The G.E. BWR emergency core cooling systems are designed to specific criteria as discussed in the following paragraphs.

The broad, over-all objective of the emergency core cooling systems is to prevent fuel cladding melting over the complete spectrum of possible break sizes in the reactor primary system up to the design break, i.e., the complete and sudden double-ended rupture of the largest line attached to the reactor vessel. In keeping with this over-all objective, the following design criteria are employed in the design of the emergency core cooling systems.

No Cladding Melting

Although it may be possible to arrest a gross core meltdown even after a given fraction of the core has begun to melt, it is difficult, based on information currently available, to prove that such a fraction will not propagate. Also, accurate prediction of core temperatures, once the basic core geometry begins to change, is questionable. Therefore, prudence requires that calculations with respect to temperature be performed on a known geometry to ensure results of sufficient engineering accuracy. Therefore, the criteria that no cladding melting shall occur has been adopted for all General Electric boiling water reactors as a design limit as calculated by conservative methods.

Protection Provided Across the Complete Spectrum of Possible Breaks

It is generally agreed that small primary-system breaks are more probable than the large

"design" break used as one extreme for design purposes. Furthermore, because the pressure can remain high while liquid inventory runs out, small breaks can have significant consequences with respect to cooling effectiveness. Therefore, the emergency core cooling systems should be capable of achieving the criteria above, not only for the largest break, but also for all smaller breaks; i.e., it should prevent fuel cladding melting across the complete spectrum of breaks up to the "design" break.

All Breaks Shall be Protected by Redundant Systems

To achieve a high reliability of core cooling, two independent and redundant systems are employed. The two systems must cool by means of different phenomena, i.e., spraying and flooding.

No A-C Power

This criterion requires that auxiliary "on-site" power must be provided and that the criteria specified above must be met even though an "off-site" power source is not available.

This criterion is used at this time in lieu of an over-all availability or reliability index against which to measure the total availability of all sources of power to the emergency core cooling systems.

Mechanical Effects

The core spray system components within the vessel, as well as the vessel internals themselves, are designed to ensure that they will survive the transient mechanical loadings imposed by the pressure differentials during blowdown so that the required core flow will actually enter the core as designed.

3.1.2 Description of the Emergency Core Cooling Systems

Each of the two core spray loops consists of one 100-percent-capacity pump feeding into an independent circular sparger ring inside the inner shroud just above the core. Only one loop needs to function to effectively core the core. When the reactor vessel pressure drops to 120 psig, rated flow is sprayed over the top of the core. Water sprayed into the bundles runs down the channel walls, providing a heat sink for the heat radiated from the fuel rods. Some convection cooling also occurs; steam is produced by evaporation within the assembly. However, the fuel rods themselves are not wetted until some 30 to 60 minutes later*, due to the Lienfrost effect on the hot rods; cooling is accomplished primarily by radiation with a weak convection component.

A completely independent low pressure coolant injection system (LPCIS) is also provided to flood the inner shroud within the pressure vessel in time to prevent cladding melting. The geometry of the inner shroud and jet pump diffusers permits flooding the core to 2/3 of its height. This system consists of four pumps, only three of which are required to meet the criteria. Suction is taken from the suppression pool and water is injected into the shroud through one of the two jet pump injection nozzle headers. Because small breaks can cause the core to be completely uncovered before either of the two low pressure systems can inject water, a high pressure coolant injection system (HPCIS) is also provided. This is actuated by either low-low water level or high drywell pressure. Steam is taken from the

*Without accounting for flooding due to accumulation of water in the inner shroud.

reactor and fed to a steam turbine which discharges into the suppression pool. The turbine drives a pump, suction being taken from either the condensate storage tank or the suppression pool, capable of injecting water into the vessel at pressures as high as 1150 psig or as low as 150 psig. In addition to simply making up fluid losses for small breaks, it performs the additional function of depressurizing the vessel for somewhat larger breaks, thereby permitting either the LPCIS or the core spray system to protect the core for the small breaks normally below their capability. Both the core spray system and the LPCIS require depressurization of the reactor vessel before rated flow capacities are attained.

For small breaks and without feedwater, the core uncovers while the pressure remains above the core spray and LPCI pump shutoff head if the HPCIS does not function. Hence, as a backup, upon coincident signals of "low-low water level," "high drywell pressure," and "no feedwater flow," vessel pressure relief valves are opened equal to about 40 percent of reactor rated steam flow rate which will reduce the pressure in time to permit either the core spray or the LPCIS to provide the coolant needed to prevent cladding melting. Thus, the LPCIS or core spray with the assistance of the auto-relief system will also protect the core for all break sizes.

With feedwater, the necessary depressurization occurs through the addition of cold feedwater to the vessel. Hence, in combination with feedwater, either the core spray or the LPCIS can protect the core for all break sizes, too.

To permit ease of visualization and to summarize, the results can best be presented in the form of a bar chart; Figure 3 shows the break coverage range for each of the systems. This shows that even with no external a-c

power, coverage for each break is achieved by at least two systems in conformance with the design criteria.

3.1.3 Behavior Prediction

The ECCS equipment is sized to accommodate the effects of metal-water reaction following the loss-of-coolant accident. The primary metal-water reaction effect is heatup which determines the time to reach cladding melting. Although the entire spectrum of break sizes is examined, the capacity of the LPCIS (flooder system) is directly determined by the largest credible break, the double-ended rupture of a recirculation line, since the core must be reflooded without assistance from any other system before the cladding melts. This is illustrated in Figure 4. The temperature curve (see Figure 5) is determined by the conservative General Electric heatup mode in which unlimited steam availability for metal-water reactions is assumed. The dashed temperature curve is the rise predicted by more sophisticated heatup models developed by Argonne National Laboratory and Battelle Memorial Institute in which the steam available is limited by boil-off of the residue water following blowdown. The lower dashed curve shows the heatup rate assuming no metal-water reaction at all.

These curves are shown to indicate the sensitivity of the heatup rate to the assumed metal-water reaction rate equation. Thus, it is seen that the metal-water reaction will directly affect the capacity requirements for the emergency core cooling systems.

For the smaller breaks (between 0.06 ft² and 0.15 ft²) the core will uncover for a period of time and then re-cover. The allowable time it can remain uncovered is set by the time it takes to reach cladding melting, and

FIGURE 3. TYPICAL CORE STANDBY COOLING SYSTEMS PERFORMANCE CAPABILITY

FIGURE 4. EFFECT OF METAL-WATER REACTION ON TEMPERATURE RISE RATE

FIGURE 5. EFFECT OF ZIRCONIUM-WATER REACTION ON ALLOWABLE TIME TO MELTING

this is influenced by the amount of metal-water reaction that takes place. For conservatism in determining pump heads, it has been assumed that no steam cooling occurs and that unlimited metal-water reaction takes place.

Figure 5 shows the allowable time during which the core can remain uncooled with no resulting fuel cladding melting, following an initial period of cooling after the accident. The dashed curve shows the time allowed with no metal-water reaction and the solid curve with unlimited metal-water reaction. As an example, if the fuel is cooled for 100 seconds following the scram, no fuel cladding melting would occur until 450 seconds following the scram. The methods used to calculate the heatup are indeed conservative in assessing the influence of metal-water reaction.

More refined calculations are used to determine the actual temperatures which might be expected. These include the effect of flow washdown immediately following the break, the effect of liquid swell during depressurization, using the Moody level model, and core cooling as a function of actual level within the vessel. These phenomena which were not included in the ultra conservative ECCS sizing calculations discussed earlier. These calculations for the design break result in the fuel cladding temperatures shown in Figure 6 for the case of the LPCI alone working and Figure 7 for the case of core spray alone.

The peak fuel cladding temperature across the break spectrum are shown in Figure 8. The metal water reaction associated with these temperatures using Baker's equation is shown in Figure 9. Thus it is concluded that for the temperatures which might actually occur in the reactor the metal water reaction is not large nor a significant contributor to the core heatup.

3.1.4 Experimental Observations

No experiments which correctly simulate a reactor core heatup have been performed. The ones which approach closest to simulating a core heatup are the single-rod steam-atmosphere tests in which Zircaloy rods were heated in a steam atmosphere under various axial pressure gradients. These are reported in Section IV of this report. The measured surface temperature and time of the test were used to estimate the amount of reaction as predicted by use of Baker's equation. This was compared to the reaction that actually occurred.

The tests were conducted with various pressure drops across them, which in effect results in variable quantities of steam flowing through the test section. This essentially simulates the degree of steam availability. The maximum reaction rate observed was always well under that predicted. In fact, as shown in Figure 10, the degree of reaction could be related to the pressure gradient (steam availability). The significant point is that for the pressure gradient expected across the core the amount of metal-water reaction is only about 15 percent of the quantity which would be predicted with Baker's equation. Therefore, in the reactor less than 0.1 percent reaction would realistically be expected rather than 0.6 percent.

In addition, use of Baker's equation apparently over-predicts the reaction rate. As discussed in Section IV, a correlation involving three different rate equations should provide more precise predictions. Use of such a set of equations results in a lower predicted reaction as shown in Figure 11. The steam availability effect is still strongly present. This, too, indicates that the steam availability is still limiting the total reaction such that less than one-half as much as that theoretically predicted by the three rate equations can occur. (See Figure 6.)

FIGURE 6. DESIGN BREAK ACCIDENT, LPCI ONLY, REFINED ANALYSIS

FIGURE 7. DESIGN BREAK ACCIDENT, CORE SPRAY ONLY, REFINED ANALYSIS

FIGURE 8 PEAK CLADDING TEMPERATURE VERSUS LIQUID BREAK AREA, REFINED ANALYSIS

FIGURE 9 LOSS-OF-COOLANT ACCIDENT, METAL-WATER REACTION, REFINED ANALYSIS

FIGURE 10. EFFECT OF PRESSURE GRADIENT ON EXTENT OF METAL-WATER REACTION

FIGURE 11. REACTION RATE OF ZIRCONIUM AS A FUNCTION OF TEMPERATURE

3.2 CONTAINMENT CAPABILITY WITH RESPECT TO METAL-WATER REACTIONS

3.2.1 Criteria and Events Evaluated

Before discussing the metal-water aspects, the containment design should be reviewed briefly. The pressure suppression containment design meets the following criteria:

- a. Withstand the peak transient pressure which could occur following rupture of the largest line in the primary system;
- b. Channel the flows to the pressure suppression chamber and withstand local impingement loadings;
- c. Limit leakage out of the containment to values substantially lower than that which would give off-site doses equal to 10CFR100 guidelines; and
- d. Be capable of being flooded, following a design basis accident, to a height which permits unloading the core.

To appreciate better what the pressure suppression system provides, it should be noted that the transient peak pressure during blowdown lasts for only a few seconds, then reduces to 30 psig and decreases with time. Since the containment is designed for 62 psig, this additional margin makes it possible to accommodate significant quantities of hydrogen gas or other noncondensibles.

Also, because of the large heat capacity afforded by the pool, large quantities of energy can be easily accommodated without exceeding the design pressure. For instance, the containment can be left uncooled for as long as 8 hours without exceeding its design pressure. Thus, any additional energy released due to metal-water reaction, or even from burning any hydrogen formed, can be

accommodated by the pressure suppression system.

3.2.2 Energy and Mass Sources in Containment Capability

The initial mass and energy loads which must be accommodated by the containment are the mass and energy loads originally within the vessel. A typical large BWR contains about 660,000 pounds of water and steam representing 370×10^6 Btu. In addition, heat stored in the core, about 35×10^6 Btu is released during the blowdown. After the blowdown the energy release consists of decay heat, the vessel stored energy of about 35×10^6 Btu, and the energy due to any metal-water reaction. The only other mass release consists of any accompanying hydrogen.

With either of the two core cooling systems in operation the maximum expected metal-water reaction calculated is 0.6 percent of the zirconium in the core. For the largest BWR cores, 0.6 percent reacted represents 800 pounds of Zircaloy with an energy release of 2.2×10^6 Btu or about 0.5 percent of the loss-of-coolant energy release. Only 18 moles of hydrogen would be released, representing an approximate 2.5 percent molar mixture with all the air in the containment, well below the 5 percent flammability threshold. In view of the conservative metal-water reaction calculations employed and recalling that the actual reaction may only be 15 percent of that calculated, the actual molar mixture will be only 3/8 of 1 percent.

Thus, the expected metal-water reaction is not a significant contributor to either the pressure or energy loads on the containment for any break size (see Figure 8).

However, the proposed AEC regulatory criteria require that the containment must include "a considerable margin for effects from metal-water or other chemical reactions that could occur as a consequence of failure of

FIGURE 12. PERCENT METAL-WATER REACTION VERSUS COOLING TIME (BREAK SIZE)

emergency core cooling systems." Without a specified event it is not possible to calculate either the duration or the quantity of metal-water reaction.

The concept of "containment capability" is used as an arbitrary but meaningful measure or index of the ability of the containment to tolerate postulated metal-water reactions. This concept is described in the next section.

3.2.3 Capability of Containment

Model Description

The basic approach used in evaluating the containment capability is to assume that energy and gas are liberated uniformly from the core region over an arbitrary period. Capability is measured in terms of the maximum percent of fuel channel and fuel cladding material which can enter into a metal-water reaction without the containment design pressures being exceeded. Since the percent metal-water reaction capability varies with the duration of the uniform energy and gas release, the percent metal-water reaction capability is plotted against the duration of release. This constitutes the containment capability for a particular system.

Assumptions

- a. The drywell and suppression chambers are at the same pressure.
- b. Energy and gas are released uniformly from the core region during an arbitrary period.
- c. All gases are saturated with water vapor.
- d. Suppression chamber gases are at suppression chamber temperature.
- e. The suppression chamber pool is the only body in the system which stores energy.
- f. The mass of water in the suppression pool is constant after blowdown of the primary vessel is completed.
- g. The heat capacity of water is constant.
- h. Internal energy of the reactor vessel and associated piping is constant during the process.

Basic Equations

It can be shown that if the drywell and suppression pool temperatures are known, and the system pressure is stipulated, the number of moles of noncondensable gas in the system is known in accordance with the following relationship (see Nomenclature):

$$M_T = \left\{ \frac{P_D - P_{(H_2O)D}}{R^*} \right\} = \frac{\frac{\theta_D + 460}{V_D} + \frac{\theta_C + 460}{V_C}}{\left(\frac{\theta_D + 460}{V_D} \right) \left(\frac{\theta_C + 460}{V_C} \right)} + \frac{P_{(H_2O)D} - P_{(H_2O)C}}{R^* \frac{\theta_C + 460}{V_C}} \quad (7)$$

The total number of gas moles in the system (M_T) is directly related to the percent metal-water reaction.

$$M_T = M_A + \frac{H(\%R)}{100} \quad (8)$$

To determine the temperature of the drywell and suppression chamber it is assumed that the gases in the suppression chamber are at the temperature of the suppression pool. Since it is assumed that the suppression pool is the only body in the system which stores energy, any energy released from the core region is absorbed by the suppression pool. Energy is removed from the pool by heat exchangers which reject heat to service

water. Since the energy release is uniform and the service water temperature and exchanger flow rate are constant, the temperature response of the pool can be found in closed form:

$$\theta_C = \left(\theta_{CO} - \theta_S - \frac{q_{in}}{K} \right) e^{-\frac{kt}{M_C C}} + \frac{q_{in}}{k} + \theta_S \quad (9)$$

The temperature of the drywell is determined by an energy balance on the water flows through the drywell:

$$q_{in} = W_{DS} C (\theta_D - \theta_i - \theta_{TTD}) + W_{CS} C (\theta_D - \theta_{CS} - \theta_{TTD}) \quad (10)$$

Consider an energy balance on the heat exchanger:

$$K (\theta_C - \theta_S) = W_{DS} C (\theta_C - \theta_i) \quad (11)$$

Eliminate θ_i and solve for θ_D .

$$\theta_D = \frac{q_{in}}{C(W_{DS} + W_{CS})} + \frac{1}{(W_{DS} + W_{CS})}$$

$$\left\{ W_{DS} \left[\theta_C + \theta_{TTD} - \frac{k}{W_{CS} C} (\theta_C - \theta_S) \right] + W_{CS} (\theta_{CS} + \theta_{TTD}) \right\} \quad (12)$$

Now, consider the power input (q_{in}) from the core region.

$$q_{in} = \left\{ (\text{Integral of decay energy over assumed duration}) + (\text{Total chemical energy from metal-water reaction}) + (\text{Initial internal energy of core}) \right\} / \left\{ \text{Duration of energy release} \right\} \quad (13)$$

Sufficient relationships have now been defined to allow a calculation by computer of

the percent metal-water reaction allowable for any assumed duration of energy release. For each particular value of duration the percent metal-water reaction is iterated upon until the design pressure of the containment is reached. The calculations are made at the end of the energy release duration, not transiently, because the number of moles in the system are at a maximum and the pool temperature is higher at this time than at any other time during the energy release.

Throughout the preceding discussion, it is assumed that none of the drywell spray is boiled by the energy release. This condition allows the noncondensibles to occupy both the drywell and wetwell and not be swept over into the suppression chamber by steam flow from the drywell. This condition does not exist for all durations. For short durations of energy releases, steam is produced. Therefore, a second condition is calculated based upon all of the noncondensable gases being forced into the suppression chamber due to boiling of the drywell spray. The capability is then determined in the same manner, but assuming that all of the noncondensibles are stored in the suppression chamber gas volume; i.e., the total permissible moles in the system is found from the following equation:

$$M_T = \frac{V_C}{R(\theta_C + 460)} [P_D - (P_{H_2O})_C] \quad (14)$$

As before, Equation (8) is solved for the percent metal-water reaction (%R). An iterative procedure is used to find solutions for (%R) at each duration as in the previous solution. Finally, the larger percent reaction from the two solutions is used at any time and this represents the capability of the containment.

Results

Three cases of interest in which arbitrary amounts of hydrogen are postulated to be present will now be considered to determine the consequences to containment. First,

the containment's capability of simply storing hydrogen and absorbing any associated energy will be examined using the model just described. Second, the capability using the same concept and model above, but assuming that the hydrogen is burned as it forms, will be determined. Finally, the effect on the containment of the most severe assumption, i. e., igniting and burning arbitrary amounts of hydrogen within the drywell, will be investigated. These three cases encompass the possible events that could occur after postulating that hydrogen is generated and released to the containment. For all three cases the containment can withstand hydrogen releases far in excess of that expected for a loss-of-coolant accident.

Case I

The capability of the containment, assuming that hydrogen is stored in the containment and that no burning occurs, is shown in Figure 13 with and without operation of the containment spray. This case assumes that hydrogen leaves the reactor vessel below the self-ignition temperature of 520 - 580°C.

Calculations involving heat transfer from the hot hydrogen leaving the core to the vessel walls and to the length of ruptured pipe through which it exits indicate that hydrogen could enter the containment atmosphere at temperatures of less than 600°C. If it is assumed that some water is present for additional cooling, this would be the case even for breaks having a minimum of heat transfer area to a heat sink. It should be noted for this case that even without containment spray, the inherent capability of the containment is nearly a factor of 25 higher than what is calculated to occur for any loss-of-coolant accident up to the circumferential rupture of the largest recirculation line.

Case II

Given a break near the core outlet, and/or an absence of water to cool the hydrogen, the hydrogen entering the containment may, shortly after it begins to

flow, burn as it leaves the break. Since hydrogen leaves the core at very high temperatures, it could self-ignite in the air if the heat losses are low. Thus, in this case, the hydrogen could burn as it is formed.

The burning of the hydrogen would reduce the total moles of oxygen in the containment system, thereby providing a greater magnitude of allowable metal-water reaction. Again, for each particular value of duration of the metal-water reaction, the percent metal-water reaction is iterated upon until the design pressure of the containment is reached. For the case of no drywell spray, all of the noncondensable gases are conservatively assumed to be stored in the suppression chamber. The burning of hydrogen gas in the drywell as evolved would increase the allowable metal-water reaction as shown in Figure 13. This capability is a factor of over 40 greater than the metal-water reaction calculated over the entire spectrum of breaks.

Although the burning of the hydrogen increases the energy content of the containment system, the total moles of noncondensable gases is substantially reduced and, thus, the capability for metal-water reaction actually increases. The curves with containment spray assume rated operation of only one of the two loops.

Case III

The most severe case would be one in which the hydrogen first accumulates and then is somehow ignited. However, both ignition and combustion are difficult to initiate and sustain in large volumes. Even if this is postulated, however, and rapid burning is assumed to occur, the containment's integrity is not jeopardized over a wide band of postulated hydrogen release, based on analyses conducted up to the date of this report.

FIGURE 13. TYPICAL CONTAINMENT CAPABILITY

The subsequent expansion of gases in the drywell during the burn would expel noncondensibles into the suppression pool. The sensible heat in the drywell atmosphere would be transferred to the walls and to the drywell spray. The peak pressure within the drywell will vary inversely with the burning time and degree of release through the pool vents, and directly with the amount the hydrogen assumed to be ignited.

Again, to obtain an index, the capability concept can be used in slightly modified form. Assuming some given duration of release, the containment pressure and temperature can be established as in Case I. Then the hydrogen assumed to be released during this duration is allowed to burn at a rate as implied by Pigford⁽²⁾ and the transient peak pressure, thus determined. A typical response is shown in Figure 14. Hence, for a given burning rate, the amount of hydrogen ignited which results in a pressure equal to the containment design pressure is the capability of the containment for that specific duration and burning rate. The results of such trial-and-error calculations are shown in Figure 15 where the capability for Case III is compared with the other two cases.

In calculating the peak pressure, adiabatic burning was assumed. Perfect mixing was used; it was assumed the mass expelled from the vents consisted of the mixture at any given time. Uniform burning over the burn time was used. The burn time depends on the flame front velocity which is a function of the mixture properties. A reasonable velocity of 2 to 8 ft/sec for air-hydrogen-steam mixture appears to be conservative.⁽²⁾ Based on the containment dimensions, this results in a burn of between 3 and 12 seconds. The effect of assumed burn time on containment pressure is illustrated in Figure 14.

Both during and before the burn, for all the assumed quantities of hydrogen within the capability curve shown in Figure 10 for Case III the air-steam-hydrogen mixtures were outside the theoretical detonation limits (see Figure 16).

The concern about hydrogen pocketing does not appear justifiable. Pocketing cannot occur because the drywell does not contain enclosed spaces or rooms. It is purposely laid out to provide enough flow area between regions in the drywell so that, for all pipe break locations, the drywell behaves as a single volume. Thermal convection currents and agitation caused by spray action will cause mixing within the drywell as a whole.

For Cases II and III in which hydrogen burning is postulated the drywell wall temperature rise was examined. In Case II the energy released from the hydrogen burning would be transferred to the suppression pool via the spray or by steaming and, therefore, the increase in drywell wall temperature would be insignificant. For Case III with burn following termination of the metal-water reaction, the gas temperature would increase significantly in the drywell, but since the heat capacity of the gases is small the total increase in temperature of the drywell still would be less than 50° F, thereby posing no problem to structural integrity of the containment. Furthermore, since the burning would take several seconds, most of the burn energy would be transmitted to the suppression pool water as the gases expand and are vented into the pool.

3.2.4 Inerting

The question of inerting arises from the concern of possible detonation or rapid burning of hydrogen-air mixtures within the containment. This requires that a sufficient hydrogen concentration be postulated within the containment. It has been shown in the preceding sections that not enough hydrogen is produced to even reach the flammability threshold of 5 percent mole fraction.

Refined calculations indicate that the maximum metal-water reaction in the case is less than 0.1 percent over the entire break spectrum. This results in only a 0.4 percent mole fraction of hydrogen in the containment

FIGURE 14. CONTAINMENT CAPABILITY

FIGURE 15. TYPICAL CONTAINMENT CAPABILITY

FIGURE 16. FLAMMABILITY LIMITS OF STEAM-AIR MIXTURES

air, a factor of nearly 13 below the flammability threshold. Even the most conservative design calculations employing 100 percent steam availability and assuming only one ECCS works result in at least a factor of 2 to 3 below the flammability limits. Therefore, on this basis alone, inerting of the containment is not warranted.

Finally, it has been shown that even postulating an arbitrary source of hydrogen the containment is capable of accommodating large quantities under any condition which might be postulated - even ignition of accumulated hydrogen in the drywell. Therefore, as a degenerated case, should more hydrogen be generated than is calculated and somehow it becomes ignited, the containment integrity will not be jeopardized. Thus, even in such a severe case, inerting is not required to ensure containment integrity.

In fact, inerting, in an effort to protect against the consequences of an extremely low probability chain of degenerated events, can actually impair the day-to-day safety of plant operation. Inerting makes routine inspections of plant components much more difficult because of the access problem. Well disciplined and routine inspections have long been recognized as the cornerstone of safe plant operations and the best protection against potential accidents of all types by early detection of faults.

In conclusion, inerting is not required because it is, first of all, not technically consistent with the low quantities of hydrogen consistent with the core cooling networks provided. Furthermore, it interferes with optimum safe operating practices and could actually be detrimental to safety.

SECTION IV

EXPERIMENTAL OBSERVATIONS

4.1 INTRODUCTION

To obtain a bench mark on the extent of metal-water reaction to be expected for a realistic core heatup, Dr. H.H. Klepfer and Dr. J.E. Hensch of General Electric Company conducted a series of tests in 1965 and 1966 simulating these reactor conditions. These tests, which were conducted under conditions of direct application to a postulated loss-of-coolant accident in a General Electric Boiling Water Reactor, will be described in the following sections.

4.2 EXPERIMENTAL EQUIPMENT AND PROCEDURE

Basically, the 35 psia test section is composed of four parts:

- a. The steam generating pot at the bottom,
- b. The primary flow passage which contains the Zircaloy heater rod,
- c. The secondary flow passage, and
- d. The condenser.

The operation of the test section consists of boiling water in the "pot" at the bottom to generate a flow of steam past the Zircaloy heater in the primary flow passage, and flow through the secondary flow passage. The purpose of the secondary flow passage is to stabilize the pressure drop across the heater by forcing most of the flow through the secondary passage so that the large temperature changes in the primary side do not affect the over-all pressure drop. This arrangement was devised so that the pressure drop across the heater could be held steady throughout the test.

The pot heaters, used for generating steam, were six 500-watt Calrod bayonet heaters, capable of being varied in power from 0 to 100 percent. This was accomplished by controlling one heater with a variac, and the other five heaters with individual switches. All exposed metal in the test section, except for the heaters and electrical leads, was Type-304 stainless steel. The annular space between the primary and secondary flow passages was filled with a special high temperature thermal insulating material, Johns-Manville Thermoflex.^(R)

The Zircaloy 2 heaters were made from standard reactor fuel cladding, with the normal autoclave oxide film. The tubing size was 0.5625-inch outside diameter, 0.035-inch wall.

The system pressure was maintained constant at 35 psia with a hydrogen bottle and pressure-regulating valve arrangement. This system worked very well, and only very small quantities of hydrogen were required by the system.

The heater pressure drop was measured with an inclined manometer, using Meriam Blue Oil manometer fluid, specific gravity 1.75. The manometer range was 0 - 2 inches, capable of being read within ± 0.010 inch. The manometer lines were filled with degassed water, and maintained full with a simple purging system. The pot heater power was measured with portable voltmeters and ammeters, with accuracy of about $\pm 1/2$ percent of full-scale deflection. The power to the Zircaloy heater was recorded continuously on a Sanborn Recording Wattmeter, No. 150-2300. The Zircaloy heater temperatures were measured continuously with platinum/platinum - 13 percent rhodium thermocouples on the opposite wall, and inside the heater. (The thermocouples inside the calrod-type heaters were platinum - 6 percent, rhodium/platinum - 30 percent rhodium.) The thermocouple output was continuously

recorded on a Sanborn-150 recorder, using Sanborn No. 150-1500 low-level preamplifiers.

The test procedure for the heatup test was as follows: first, the power to the pot heaters was turned on and the entire loop warmed up. The pressure regulating system was then adjusted so that a minimum flow rate of hydrogen was required. The power to the Zircaloy heater was then turned on and adjusted so that the surface temperature was 500 - 600° F. The pot heater power was then adjusted to obtain the desired heater pressure drop. Once the desired pressure drop was attained, the system was allowed to sit for about 30 minutes to ensure steady state conditions. The power to the heater was then increased in steps at 2-minute intervals to get the desired temperature rise rate, until the heater failed, usually around 3000° F. The Zircaloy heater power was then shut off and the system was cooled down as rapidly as possible to minimize metal-water reaction during cooldown.

4.3 TEST RESULTS

4.3.1 Discussion

Figures 17 through 23 show typical profiles of metal-water reaction for the Zircaloy rods for different flow conditions. Typical of all the runs (Table 1) is the low reaction rate at the bottom of the rod, with the extent of reaction increasing to some ultimate value toward the top of the tube. This lower reaction at the flow inlet is undoubtedly caused by the cooling effect of the saturated steam on the tube temperature. As the steam rises up the tube, the extent of reaction will ultimately reach a steady value. This fully established value is then divided by the value predicted with the parabolic rate equation, and plotted against pressure gradient in Figures 6 and 20. As the pressure gradient increases, the steam flow increases, and the fully established reaction rate should increase.

FIGURE 17. EXTENT OF REACTION FOR A ZIRCALOY-2 TUBE IN A SINGLE-ROD TEST SECTION:
 PRESSURE GRADIENT 0.540 lb/ft³

FIGURE 18. EXTENT OF REACTION FOR A ZIRCALOY-2 TUBE IN A SINGLE-ROD TEST SECTION

FIGURE 19. EXTENT OF REACTION FOR A ZIRCALOY-2 TUBE IN A SINGLE-ROD TEST SECTION

FIGURE 20. EXTENT OF REACTION FOR A ZIRCALOY-2 TUBE IN A SINGLE-ROD TEST SECTION

FIGURE 21. EXTENT OF REACTION FOR A ZIRCALOY-2 TUBE IN A SINGLE-ROD TEST SECTION

FIGURE 22. EXTENT OF REACTION FOR A ZIRCALLOY-2 TUBE IN A SINGLE-ROD TEST SECTION

FIGURE 23. EXTENT OF REACTION FOR A ZIRCALOY-2 TUBE IN A SINGLE-ROD TEST SECTION

TABLE 1

HEATUP TEST RESULTS

Run No.	Date	ΔP (Inches of Water)	$\frac{\partial P}{\partial y}$ (lb/ft ³)	$\frac{\partial T}{\partial t}$ (° F/min)	Maximum Temperature (° F)	Reaction (°C)			$\frac{A}{B}$	$\frac{A}{C}$
						(A) Actual (Measured)	(B) Predicted (Baker's Equation)	(C) Predicted (Klepfer's Equations)		
1	6/3/65	0.33	0.540	150	3380	7.2	11.2	7.4	0.641	0.975
2	6/4/65	0.225	0.395	150	3300	5.4	13.4	6.55	0.403	0.825
3	6/10/65	0.48	0.748	100	3000	4.4	10.5	4.95	0.42	0.89
4	12/15/65	0.116	0.2445	150	2460	4.0 5.0	6.53	4.52	0.613 0.766	0.885 1.1
5	12/18/65	0.375	0.603	150	2450	7.1	11.43	7.9	0.62	0.9
6	12/18/65	0.075	0.188	100	2870	5.1	18.27	11.1	0.279	0.46
7	12/20/65	0.0675	0.1774	150	3030	8.5	20.95	11.5	0.406	0.74
8	12/27/65	0.150	0.2895	100	2440	6.5 7.6	11.95	9.17	0.545 0.635	0.716 0.83
9	1/4/66	0.520	0.797	150	2960	20.0 30.0	39.65	21.9	0.505 0.757	0.915 1.37
10	1/8/66	0.075	0.188	150	2870	10.9	35.5	18.2	0.307	0.60
11	1/13/66	0.150	0.2895	250	3550	14.0	42.42	20.8	0.33	0.672

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FIGURE 24. EFFECT OF PRESSURE GRADIENT ON EXTENT OF METAL-WATER REACTION

This general trend is verified in these experiments, as shown in Figures 6 and 20. The scatter of data seems to be slightly greater in Figure 24, where the experimental results are normalized to that predicted with Baker's equation, than in Figure 10, where the normalizing reaction is calculated with a three-region parabolic rate constant (see Subsection 4.3.6). This tends to verify the greater accuracy of the three-region solution over the one-region solution of Baker, the latter giving a reaction extent which is too high, and therefore, conservative.

4.3.2 Accuracy of Experimental Data

Figures 17, 18, and 19 are metal-water reaction profiles for Zircaloy-2-clad Calrod-type heaters, while Figures 20 through 23 are profiles for Zircaloy-2 tubes heated directly by passing current through the tube wall. The data obtained with the Calrod-type heaters are more uniform than those obtained with direct heating. This is because the heat generation with the Calrod heaters is independent of the reaction rate occurring on the surface of the Zircaloy tube, while with the directly heated rods it is not; i. e., when electric current is being conducted through a tube, the local heat flux is a function of the local wall thickness, thinner spots having a higher heat flux. This affects the stability of the surface temperature during a metal-water reaction experiment; as a local spot reacts, the remaining metal becomes thinner, causing higher heat flux and higher temperature, increasing the local reaction rate, which makes the remaining metal thinner, and so on. Thus, once a hot spot forms during a metal-water reaction experiment, it tends to deteriorate very quickly, causing failure at that point. For this reason, the extent of reaction in the vicinity of a failure is not representative of the whole tube, and was disregarded in the analysis of results.

4.3.3 Comparison of Results With Parabolic Rate Law

The experimental evidence obtained in this study indicates that steam starvation in

some form occurs in a real geometry in various degrees. Predictably, lower pressure gradient (flow rate) through the reacting section yields a lower extent of reaction relating to that predicted by the parabolic rate law. In fact, at a pressure gradient which is expected during a loss-of-coolant accident, equal to the density of saturated steam at 35 psia, the extent of reaction is only about 20 percent of that predicted by Baker's equation, and about 40 percent of that predicted by the three-region equation discussed below. (See Figures 10 and 20.)

4.3.4 Metallurgy of the Zircaloy-Water Reaction

Douglass⁽³⁾ has recently pointed out that the widely accepted phase diagram⁽⁴⁾ for ZrO_2 can be refined by the 1961 work of Gebbhardt, et al.⁽⁵⁾ Douglass gives the phase diagram shown here as Figure 25. This new diagram is more satisfying, as it has long been known that ZrO_2 can exist in three allotropic forms; the earlier diagram did not reflect this important fact.

4.3.5 Oxide Microstructure

The important new feature determined by Gebbhardt et al., is the eutectoid decomposition of cubic ZrO_2 giving tetragonal ZrO_2 with the rejection of oxygen-saturated alpha zirconium at 1577°C. The correct interpretation of the microstructures of the oxide layers on Zircaloy-2 metal-water reaction samples is possible with the new phase diagram. As many as three layers of different microstructures are observed on Zircaloy-2 samples exposed to steam up to the melting point; these layers can now be seen to correspond to monoclinic, tetragonal, or cubic oxide formed at the appropriate temperatures. When examined at room temperature, oxide formed above 1577°C will show metal rejected on the grain boundaries and on certain preferred planes in the oxide. These features

COMPARISON OF Zr-O PHASE DIAGRAMS

- DOMAGALA AND McPHERSON
- GEBHARDT et al
- + ⊕ ⊗ STRUCTURE INVESTIGATIONS
- □ RESISTANCE MEASUREMENTS

FIGURE 25. COMPARISON OF ZrO₂ PHASE DIAGRAMS

have been used to assist in quantitative metallographic analysis of oxygen in Zircaloy-2 metal-water reaction samples.

4.3.6 Temperature Dependence of the Metal-Water Reaction Rate Constant

The fact that different allotropic forms of zirconium dioxide are formed at different temperatures would lead one to anticipate different activation energies for oxide growth for each of the three oxide forms. Baker⁽¹⁾ proposed an activation energy of 45,500 cal/mole over the entire temperature range:

$$W^2 = K_p t, \quad (15)$$

where W is weight of zirconium reacted in mg/cm^2 , t is time of exposure in seconds, and K_p is the parabolic rate constant whose temperature dependence is given by:

$$K_p = 33.6 \times 10^6 \exp(-45,500/RT) \times (\text{mg}/\text{cm}^2)^2/\text{sec}. \quad (16)$$

Baker's choice of this equation was based on a best-fit to experimental data for molten zirconium droplets reacting with water and to isothermal reaction data reported by Lemmon⁽⁶⁾ and by Bostrom⁽⁷⁾ for reaction above 1000°C. Baker pointed out that his fit to the droplet data actually was sensitive only to the reaction rate near the melting point; in fact, Baker's plot of Lemmon's data fits the line for Equation 16 only approximately. Equation 16, furthermore, predicts a reaction constant at low temperatures, e.g., 400°C, which is two orders of magnitude too low to be consistent with common Zircaloy-2 autoclaving test results.

Pemsler⁽⁸⁾ has recently reported a change in activation energy for steam-zirconium reactions at about 900°C. Below 900°C an activation energy of about 29,000 kcal/mole is accepted for the formation of monoclinic oxide; Pemsler suggests about 35,000 kcal/mole above 900°C where tetragonal oxide is formed. Another change in activation energy at about 1577°C would be expected as cubic oxide forms above this temperature. Replotting Baker's reaction rate constant at the melting point and Lemmon's data and anticipating changes in slope at the allotropic transformation temperatures, one gets the curve shown as Figure 7. The parabolic rate constants are given by the equations below as derived by Dr. H.H. Klepfer.

$$K_p = 5.52 \times 10^4 \exp(-29,000/RT) \text{ from room temperature to } 890^\circ\text{C} \quad (17)$$

$$K_p = 3.58 \times 10^5 \exp(-33,500/RT) \text{ from } 890 \text{ to } 1577^\circ\text{C} \quad (18)$$

$$K_p = 1.04 \times 10^{11} \exp(-78,800/RT) \text{ from } 1577^\circ\text{C to the melting point} \quad (19)$$

These equations are consistent with low-temperature autoclaving data, Pemsler's observations, Lemmon's results, and Baker's rates near the melting point. (Similar activation energies would fit Bostrom's data, but the curve would be shifted to lower temperatures by about 100°C). The use of Baker's equation in computing molten droplet reaction percentages should be quite acceptable, but for predicting percent reaction on slow heatup of cladding, Equations 17, 18, and 19 appear more precise and use of Baker's equation is therefore quite conservative.

NOMENCLATURE

B	Rate constant	ft^2/sec
D	Rate constant	$^{\circ}\text{R}$
P	Pressure	lb/ft^2
R*	Universal gas constant	$\frac{\text{ft} - \text{lb}}{\text{lb}_m - ^{\circ}\text{F}}$
V	Volume	ft^3
θ	Temperature	$^{\circ}\text{F}$
T	Temperature	$^{\circ}\text{C}$
M	Moles	--
q	Flux	Btu/sec
t	Time	seconds
Pc	Density of cladding	lb/ft^3
C	Heat capacity	$\text{Btu}/\text{lb}_m^{\circ}\text{F}$
K	Heat exchanger capability	$\text{Btu}/\text{sec} - ^{\circ}\text{F}$
d	Differential operator	--
W	Flow rate	lb_m/sec
%R	Metal-water reaction rate	%
H	Total possible moles of hydrogen	--

SUBSCRIPTS

H_2O	Refers to water vapor or water
D	Drywell
C	Suppression chamber
IN	Energy from core region
S	Service water
TTD	Terminal temperature difference
DS	Drywell spray
CS	Core spray
O	Out
A	Air
i	In

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TECHNICAL CONTRIBUTIONS

The results reported in this document represent the efforts of several General Electric Company technical personnel. In particular, the core cooling analysis by A.E. Rogers. The containment evaluation by D.J. Liffengren, and the experimental work by Dr. J.E. Hench and Dr. H.H. Klepfer deserve special mention.