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BNWL-233

AEC
RESEARCH
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REPORT

**CONTAINMENT SYSTEMS EXPERIMENT
PART III
MATHEMATICAL MODELS
OF
PRESSURE-TEMPERATURE TRANSIENTS**

B. M. JOHNSON

MAY, 1966

BATTELLE-NORTHWEST

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RICHLAND, WASHINGTON

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BATTELLE MEMORIAL INSTITUTE

for the

UNITED STATES ATOMIC ENERGY COMMISSION UNDER CONTRACT AT(45-1)-1830

PRINTED BY/ FOR THE U. S. ATOMIC ENERGY COMMISSION

BNWL-233

UC-80, Reactor Technology

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Printed in USA. Price \$5.00. Available from the
Clearinghouse for Federal Scientific and Technical Information
National Bureau of Standards
U.S. Department of Commerce
Springfield, Virginia

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CONTAINMENT SYSTEMS EXPERIMENT
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1.0 INTRODUCTION

This document summarizes the initial efforts to develop a mathematical model of pressure-temperature transients. These models, after refinement and adjustment, may provide a satisfactory simulation of test data from Containment Systems Experiment (CSE). Since it is a continuing segment of the CSE program, this document is intended to provide a manual for the complete understanding of the work through 1965 with suggestions for refinement as data becomes available. Although models of pressure and temperature transients in containment systems have been developed elsewhere, they are proprietary and are not available for unrestricted uses or detailed scrutiny.

The CSE at Battelle-Northwest was authorized in November 1963. The objective was to investigate the behavior of containment systems and related engineered safeguards under the conditions of a maximum credible accident for pressurized and boiling water power reactors. The program is closely related to several others in the nuclear safety program of the Division of Reactor Development; namely, laboratory investigations of reactor fuel meltdown and fission product release and transport behavior, the Nuclear Safety Pilot Plant at Oak Ridge National Laboratory, and the Loss of Flow Test at the National Reactor Test Station.

For power reactors of the pressurized or boiling water types, the maximum credible accident is hypothesized to involve a sudden

and complete rupture of the reactor coolant piping, either the expulsion of the coolant from the reactor via the rupture or the inadequacy of emergency core cooling provision, and melting of a significant fraction of the reactor core by fission product decay heat. A low leakage containment structure housing the reactor is relied upon to greatly reduce the possible spread of fission products to the environs. Engineered safeguards in addition to the containment structure have been utilized or proposed to further limit the potential hazard.

Although considerable experimental work has been done on various engineered safeguards, unresolved questions still exist on the performance of such systems under accident conditions. In addition there is inadequate information on the transport of fission products in containment systems.

Data on the pressure and temperature transients that occur during a loss of coolant accident are rather meager and further information on the contribution of condensation on various surfaces, entrainment of atomized liquid, as well as further fundamental understanding on transient two phase critical flow are anticipated results of the program.

The CSE facility was designed to represent a 1000 MW_e power reactor plant on about a one-fifth linear scale. Detailed descriptions of the program and facilities are presented in the two documents "Containment Systems Experiment - Part I: Description of Experimental Facilities," C. E. Linderoth and "Part II: Detailed Program Outline," G. J. Rogers (both in preparation). Guidance in extrapolating the experience gained during the program to full scale industrial application can best be provided by a mathematical model which simulates the behavior of the system. Most amenable to modeling are the pressure and temperature transients in each of the regions of the system.

2.0 SUMMARY, CONCLUSIONS, AND RECOMMENDATIONS

2.1 Summary

Several programs were developed and run utilizing the MIDAS (Modified Integral Digital Analog Simulator) generalized program. This approach utilizes the digital computer to simulate the behavior of an analog system in the solution of simultaneous differential equations. It has the particular advantage of permitting a rapid development of the problem on the analog computer (when it becomes desirable to do so) to attempt a fit between the mathematical model and experimental data by the varying of parameters.

A summary of the various programs is as follows:

- (a) A simple one zone model of the primary vessel computes pressure and temperature transients. This model simulates more closely the discharge from a boiling water reactor and a vessel such as utilized in the CSE tests rather than a pressurized water reactor. The discharge from the Bodega Bay and Humbolt Bay test is approximated rather well by this model. It is used as the mass-energy input source for all multivessel programs. A more refined model of the blowdown event allows for superheating (or the absence of phase separation) in a portion of the liquid during blowdown, with vaporization occurring from only a limited region near the top.

Behavior of a subcooled system, ie, one pressurized by a noncondensable gas, is approximated. By varying the fraction of initial mass assumed to remain in equilibrium with the vapor phase, the effect of phase separation (or what is equivalent, the effect of the ratio of vessel-to-break diameter on discharge rate) possibly can be approximated. Effects of inertia and pressure drop through the

core on the local stagnation pressure near the break were originally in the program but were subsequently broken out into a separate program due to the excessive amount of machine time required to include them during the entire blowdown. The rate of critical two phase discharge is included in the program through the use of an approximation of the initial Moody model. Other models could easily be substituted if a good agreement between calculated and measured pressure transients is not obtained.

- (b) The temperature gradients in the primary vessel wall and the effect of the heat content of the vessel upon the blowdown transients are available from a third program. This program introduces variable heat transfer coefficients in the form of empirical correlations derived from the theoretical equations for nucleate, transition, and film boiling. Steep temperature gradients are handled by differential difference equations utilizing square root spacing to give greater accuracy near the wall.
- (c) Pressure and temperature transients were calculated in dry containment with spray suppression and fission product heat approximated. Heat transfer to the wall with a fixed coefficient was included. The program could be extended in a straight forward manner to include the effect of additional heat sinks in the containment and variable heat transfer coefficients at these surfaces. Present program emphasizes behavior over a short initial period (~ 30 sec) but the analog equivalent could easily handle behavior over period of days by approximating initial energy release as an exponential function with a very short time constant.

- (d) Pressure temperature transients in a pressure suppression system (ie, the dry well-wet well concept) were calculated. The degree of carry-over (liquid water carried through the dry well into the wet well) is a parameter that at present is assumed constant but could easily be made to vary through the course of the blowdown. Calculated results, using various degrees of carry-over, brackets the experimental values obtained in the Bodega Bay and Humbolt Bay tests and agree within 5% with the curves calculated by a proprietary General Electric Company code. The CSE code does not at present have provision for heat loss to the walls of the dry well but this could be included in a straight forward manner as is done in the dry containment program.

2.2 Conclusions and Recommendations

Initially the simulation of the blowdown behavior of the primary vessel was thought to be of relatively minor interest, in fact some thought was given to expressing the mass and energy input into the containment system as an empirical function obtained from test results. However, CSE will have excellent facilities to study transient two phase flow and further work on this aspect is warranted. Two modifications on the present two zone model could be readily incorporated into the program with the possible improvement in the simulation of the blowdown rate; namely (1) the Moody Model⁽¹⁾ of maximum two phase flow from pipes and (2) the Margolis-Redfield⁽²⁾ Model of bubble disengaging velocity and the resulting computation of the fluid state at the break. In the calculation of core pressure drop the effect of the two phase flow multiplier on the drop across the core should be studied further.

The dry containment program calculates the short term transients but requires excessive computational time for long term

changes. On the analog computer the long term situation can be easily simulated by replacing the primary vessel model with a simple first order release of mass and energy with arbitrary time constants adjusted to give an initial pressure (or temperature) peak which coincides with the calculated value. Additional loops for heat sinks in the system other than the vessel walls may be incorporated readily. Fewer segments than the six used in the program could be used in the study of long term effects with little sacrifice in accuracy other than the initial peak transient. A variable condensing coefficient can be introduced. Variation may be due both to changing steam-air ratio (discussed in Section 4.4) and to changes in boundary layer thickness resulting from convection currents. Tests data would have to be obtained to guide such refinements. Elaboration in the treatment of air recirculation cooling is warranted as soon as design scope is available to direct the modeling criteria.

In the pressure suppression program, inclusion of the effects of condensation on transients would be of interest both in itself and because of the resulting calculation of temperature distribution in the walls of the dry well which would provide information on thermal stresses. Inclusion of an empirical expression for carry-over as a function of the enthalpy and pressure of the liquid from the primary vessel could be accomplished easily and the need for this refinement may be indicated by tests data.

3.0 CURRENT STATUS OF THEORETICAL MODELS DEPICTING LOSS OF COOLANT ACCIDENTS

Previous work appearing in the literature on this general topic may be divided into three categories for the purpose of discussion, critical flow models, blowdown models, and containment models. An excellent summary of work to date on these first two categories is provided by Parks⁽³⁾ and is used extensively in the discussions in this chapter.

The topic of critical flow models deals with the mass rate of flow from a break in the system. For a large fraction of the time during the blowdown event it is very likely that flow through the break will involve two phase critical flow. This phenomena has been subject to a great deal of study and is still not completely understood.

The category of blowdown models deals with the pressure-temperature transients in the primary reactor system during that period of time when fluid is escaping from the system. Pressure and temperature transients in various parts of the system, rates of flow and pressure differences between regions in the system, and a complete understanding as to the thermodynamic state existing in each of these regions are involved in blowdown modeling. These models tend to be rather particular in nature, ie they apply to a particular reactor and would require considerable modification before they would be applicable to another reactor of a different configuration.

The category of containment models comprises analytical expressions developed to predict pressure and temperature transients in the regions surrounding the primary reactor system during and following the loss of coolant accident in the reactor.

3.1 Critical Two Phase Flow

A very useful summary of the literature on critical flow phenomena in two phase mixtures was provided by Isbin, et al⁽⁴⁾ in their paper for the Third United Nations Conference in Geneva. The current status of available theoretical and experimental studies which relate to the evaluation of the rate of coolant flow through apertures is given..

Also included are brief descriptions and current results from experimental programs designed to present scale versions of reactor installations as well as experiments designed to learn more about the two phase flow phenomena. Two phase flow, ie vapor-liquid flow, exhibits maximum discharges from an opening into an enlarged downstream section that is almost independent of downstream pressure, just as is the case with single phase flow. Although individual phase velocities under the condition of maximum discharge may be considerably smaller than the corresponding sonic velocities⁽⁵⁾ (the criteria for critical flow in a single phase system), the expression for mass flow as a function of the ratio of the individual phase velocities and the pressure at the discharge point exhibits a maximum with respect to these two variables which indicates that two phase flow does experience a critical flow phenomena.

To date all models of the critical flow phenomena have made the following assumptions in their derivations.

- The two phase fluid is discharging from either a constant area duct or has been expanded in an ideal nozzle so that both phases are at the same static pressure and are in local equilibrium.
- The flow is steady and the velocity of the individual phases is uniform across the duct.

The effects that have been neglected include the possible existence of entrained water in the gas phase and the possible lack of equilibrium conditions at the exit. The models can be further grouped into either homogeneous flow models or annular (separated) flow models.

3.1.1 Homogeneous Flow Model

The model assumes that the steam and water phases are in equilibrium and that there is no slip between the phases. The mixture's

specific volume is given by

$$v = v_v + x v_{lv}. \quad (3.1-1)$$

The equation for the maximum mass velocity is obtained by the same approach as that used for the single phase system and has the identical form. The equation for the conservation of momentum and mass are written for the system using the above expression for the specific volume, and the derivative of the mass velocity with respect to pressure is set equal to zero to obtain an expression for the velocity. The resulting equation is

$$G^2 = -g_c \left(\frac{\partial p}{\partial v} \right)_s. \quad (3.1-2)$$

3.1.2 Annular Flow Models

These models assume a two phase flow pattern with a vapor core and a liquid annulus. The same expression as Equation 3.1.2 may be obtained for the critical mass rate, but the derived expression for the mixture's specific volume becomes

$$v = \frac{X^2 v_v}{\alpha} + \frac{(1-X)^2 v_l}{1-\alpha} \quad (3.1-3)$$

where α represents the void fraction, or fraction of the cross-sectional area for flow occupied by the vapor.

This approach introduces an additional degree of freedom, namely the void fraction, and in essence allows the phase velocities to take on two different values. A variety of methods has been used to estimate the void fraction and to relate it to the steam qualities and the phase velocity ratio.

Fauske's Model.⁽⁵⁾ Fauske's approach to relating the void fraction to the slip ratio and the quality under the conditions

of maximum flow is to maximize the pressure gradient along the pipe proceeding to the exit. In effect he obtains an expression for the specific volume in terms of the slip ratio and assumes that this must be maximized. This leads to the following relationship between the slip ratio and the specific volumes

$$K \text{ (slip ratio)} = (v_v/v_\ell)^{1/2}$$

and

$$\alpha = \left[1 + \frac{1-X}{XK} \right]^{-1}. \quad (3.1-4)$$

These expressions can then be substituted into Equations 3.1-2 and 3.1-3 to obtain the critical mass velocity.

Levy Model.⁽⁶⁾ In this model an expression for the specific relationship between the quality and void fraction is obtained directly from the momentum and continuity equations for two phase flow; the head and friction losses are neglected. The resulting rather complex expression for quality is then substituted into Equations 3.1-2 and 3.1-3 to obtain an expression for the critical mass velocity.

Moody Model.⁽⁷⁾ Moody's model, rather than starting from Equations 3.1-2 and 3.1-3 and proceeding to the obtained expression for specific volume in terms of the slip ratio, is derived from the continuity and energy equations written as follows for two phase annular flow.

$$G = \left(\frac{1-X}{1-X} \right) \left(\frac{U_\ell}{v_\ell} \right) \quad (3.1-5)$$

$$h = X \left[h_v + (U_v^2/2gJ) \right] + (1-X) \left[h_w + (U_w^2/2gJ) \right].$$

$$(3.1-6)$$

These equations, together with the assumption of isentropic flow, yield a rather complex relationship for G which is a function of the slip ratio K and p , the system pressure, at the point of critical flow. To obtain the maximum flow rate, one imposes the following conditions.

$$(\partial G / \partial K)_p = 0, \quad \text{and} \quad (\partial G / \partial p)_K = 0$$

The first condition is used to obtain the relationship between the slip ratio and the specific volume. This results in the expression,

$$K = (v_v / v_g)^{1/3}. \quad (3.1-7)$$

The second condition is used in conjunction with Equation 3.1-7 to obtain the critical flow. Values of $G_{\max}$ for local pressures of 25 to 3000 psi and local qualities of 0.01 to 1.00 are obtained and can be found in Reference 7. This minimizing of kinetic energy was also presented by Zevi.⁽⁸⁾

Moody⁽¹⁾ has subsequently extended his approach to include the effect of wall shear by introducing it into the momentum balance equation and by using this equation to obtain the pressure required at some upstream point to give the maximum flow rate G_m at the point of critical flow. He uses the minimum kinetic model just discussed to obtain the value of G_m at the point of critical flow. His results are presented as a series of graphs relating the maximum flow rate to the stagnation enthalpy and the stagnation pressure at the various values of the friction term fL/d .

3.1.3 Two Phase Flow Through Apertures

The previously discussed models all assume that both phases are in local equilibrium. For flow of initially compressed or

saturated water through a sharp edged orifice, the system behaves as a single phase; and no flashing occurs at the orifice opening. As the length-to-diameter ratio of the aperture increases, longer contact times apparently cause an increase in nucleation and approach to the equilibrium conditions assumed in the previous models.

Esleeck⁽⁹⁾ proposed a method for the analysis of leaks in reactor coolant loops using the orifice equation. If the orifice coefficient is assumed to be 0.6 and thermal and compressibility factors are assumed to be unity, the resulting equation is

$$G^{(1)} = 0.4 \sqrt{\rho \frac{\Delta p}{144}} \quad (3.1-8)$$

where the flow area is in square inches. Four situations that can be encountered in a typical blowdown are listed as follows:

- (a) Subcooled water discharges to an ambient pressure greater than the saturation pressure of inlet fluid: For this condition, the density is constant; and the pressure difference is just the upstream pressure minus the discharge pressure.
- (b) Subcooled water discharges to an ambient pressure less than the corresponding saturation pressure of inlet fluid: For this condition, the density is assumed constant; and the effective pressure differential is the difference between the upstream pressure and the critical pressure. The critical pressure ratio is taken as 0.75 for pressures up to 2000 psi. The resulting expression is $\Delta p = (p_{\text{upstream}} - 0.75p_{\text{sat}})$.
- (c) Saturated water: For this condition, the density is assumed to remain constant; and the effective pressure difference is the difference between upstream pressure and the

critical pressure. Resulting expression is

$$\Delta p = (p_{\text{upstream}} - p_{\text{critical}}) = 0.25 p_{\text{upstream}}$$

- (d) Homogeneous mixture at a temperature corresponding to the instantaneous saturation value of the upstream pressure. Analysis of this condition resulted in the following equation.

$$G^{(3)} = 0.4 \sqrt{0.25 (\gamma_1/h - \gamma_2) (p_{\text{upstream}}/144)} \quad (3.1-9)$$

where

$$\gamma_1 = h_{\ell v} / v_{\ell v}$$

$$\gamma_2 = h_{\ell v} - v_{\ell} \left[(h_{\ell v} / v_{\ell v}) \right]$$

3.1.4 Test Results

A brief discussion of test programs on the flow of flashing fluids through apertures is given in Chapter 12 of Reactor Containment Handbook.⁽¹⁰⁾ Only those results which appear to be particularly pertinent to the CSE Program are mentioned here.

Pacific Gas and Electric Company (PG&E) Tests for Humboldt and Bodega Bay Reactors. Tests have been run by PG&E to simulate loss of reactor coolant due to pipe breaks with the Humboldt Bay and Bodega Bay nuclear plants. Test setup is described in References 11, 12, and 13. In all tests performed, the average, not the instantaneous, discharge rates were measured. The evaluation of these results yields some interesting indications and conclusions.

- The size of orifice or nozzle relative to the pressure vessel diameter had an important effect on the average flow rate. Mass velocities were high with small openings

and lower with large openings. This phenomena may be related to the entrapment and sweeping out of the steam bubbles produced in the vessel with outflows through the larger openings.

- The blowdown behavior of Humboldt and Bodega vessels was very similar, although one was about 50% larger in diameter and volume than the other.
- Average vessel discharge rates were not affected significantly if the water in the vessel was subcooled initially by not more than about 30 to 40 °F. Beyond this amount of subcooling, the average discharge rate increased as the initial subcooling was increased.

General Electric, Hanford Laboratories Tests. Zaloudek⁽¹⁴⁾ observed two discreet choking phenomena in experiments with short tubes. Choking occurred at the vena contracta near the inlet of the tube if the downstream pressure was above the saturation pressure but the local static pressure was dropped to approximately the saturation pressure. Flow rates under these conditions were accurately predicted by conventional nozzle equations and using the saturation pressure as the low pressure value.

For very short types ($L/D < 6$) and low back pressure, choking occurred at the exit of the test section. The flowing water was observed to separate from the wall of the pipe at the entrance, in the vena contracta region as above, but further choking occurred at the exit if the exit pressure was sufficiently low. At intermediate values of the back pressure, metastable flow was observed. The mass velocities under conditions of downstream choking was closely approximated by models developed by Burnell,⁽¹⁵⁾ Kinderman and Wales,⁽¹⁶⁾ and Bailey.⁽¹⁷⁾ These models are reviewed

in Reference 14. Critical flow of saturated water at pressures as high as 600 psia and temperatures up to 550 °F were closely approximated by the Fauske theory.

Argonne National Laboratory Tests. Fauske⁽¹⁸⁾ has performed some very interesting experiments which provide an indication of the required L/D value necessary to approach the conditions of phase equilibrium and the applicability of the annular flow models. For a short tube with a L/D equal to 0, ie for a sharp edged orifice, the mass flow rate for an initially saturated liquid can be evaluated using the incompressible flow equation for the orifice,

$$G = 0.61 \sqrt{2 g_c (p_0 - p_1) \rho} \quad (3.1-10)$$

where

p_0 = upstream pressure

p_1 = downstream ambient pressure.

For short tubes, $L/D < 3$, Equation 3.1-10 is used in which p_1 is replaced by the exit critical pressure, p_c . However, the critical pressure ratio is an experimentally determined function of the length-to-diameter ratio of the discharge tube and is given in Reference 18. For $3 < L/D < 12$, use of Equation 3.1-10 with the observed critical pressure ratio predicts flow rates that are higher than those obtained experimentally. Under these test conditions, violent break up of the metastable liquid jet as observed in Zaloudek's⁽¹⁴⁾ studies probably account for the high pressure fluctuation (± 50 psi) that are observed in the test sections. For $12 < L/D < 40$, the flow could be predicted by Fauske's annular flow model. For very long lengths of pipe, a model such as Moody's, which takes into account friction effects and is discussed in the previous section, conceivably, would be required.

These tests, and similar ones performed at the University of Minnesota, indicate that the critical flow phenomena within the short length apertures differs from that observed for flows from pipes in which phase equilibrium conditions are closely approximated.

3.2 System Blowdown Models

3.2.1 Analytical Models

As previously stated, analytical models of system blowdown tend to be rather particular in nature, ie they apply to a particular reactor. Nahavandi⁽¹⁹⁾ made a digital computer analysis of a loss of coolant accident for a multicircuit core nuclear power plant. This program is applicable to pressure tube reactor plants such as the Carolinas Virginia Tube Reactor. The problems of flow starvation in the reactor core and steam formation in the primary pumps were of prime concern. The size of leak considered was small; the assumed accident was the severence of one of the 36 pressure tubes. The fundamental equations were developed by application of energy, continuity and momentum equations to a "control volume" consisting of a segment of the system. Twenty-five individual flow segments were considered.

An earlier paper by T. A. Harris⁽²⁰⁾ analyzed the coolant blowdown process which occurs after rupture in a pressurized water nuclear power plant.

S. Fabric (reported in Reference 3) of Kaiser Engineers has developed a digital computer program to analyze the blowdown of the LOFT Facility. This program was originally developed to determine thermal stresses in the vessel during blowdown; therefore, the detailed temperature distribution in the system was desired.

Fifty-four individual elements or segments of the system are analyzed. Heat addition from the reactor core decay heat and from

stored heat in the piping was included. Decay heat input was assumed to be constant for the first 10 sec of the blowdown but this could be easily changed to a time dependent input. Variable heat transfer coefficients were used to establish the heat input from the metal. The quality of the mixture determined the heat transfer correlation that was used. No inertia terms are included in the calculation of pressure drops. Flow of the simulated rupture was assumed to be described by the Fauske annular flow model.

The form of the equation for describing the critical flow was that derived by Nahavandi⁽¹⁹⁾ which is written as

$$G^{(3)} = \frac{p}{0.000268(h_s - 185)} \quad (3.2-1)$$

The program yields transient pressures, temperatures, and heat transfer coefficients in each segment.

A proprietary program for the analysis of the blowdown event in a pressure tube reactor has also been developed by the Stone and Webster Corporation.⁽²¹⁾ However, the approach taken in this program, called LOSCLT, and the individual elements in the system that are analyzed are not known.

General Electric, APED,⁽²²⁾ has developed a computer program to describe the blowdown from a boiling water reactor system. This is also a proprietary program and the number of elements which are individually analyzed in the system are not known.

Analytical models using the analog computer generally make use of the simplifying assumption that all the primary system coolant can be lumped into one vessel or into one vessel plus

the pressurizer. One study performed by Babcock and Wilcox Company (B&W)^(23,24) for the LOFT Program went a step further and broke the system into three regions: pressurizer; hot leg; and cold leg. Resistance to flow in the core as well as the pressurizer surge line was simulated. The rupture was assumed to occur from nozzles discharging directly from the vessel. Both top and bottom leaks were analyzed by assuming homogeneous expansion in the vessel and the critical flow equations as suggested by Esleeck. Other included assumptions were that (a) there was no heat addition (b) coolant expansion in the system was homogeneous for pressures below the initial saturation temperature (c) pressure drops in the system piping could be calculated by the Martinelli-Nelson Multipliers on the friction term and the coolant inertia term and (d) the two phase discharge could be approximated by the homogeneous critical flow model.

3.2.2 Test Results

There is very little in the way of test data with which the various analytical models can be compared. The PG&E tests in connection with the design of the Humboldt and Bodega Bay Reactors do provide curves of pressure decay during the blowdown event. Data available on the Humboldt Bay scale model tests are reported in Reference 12. The simulated reactor vessel was vertical, 32 ft in length, 20 in. in OD and Schedule 80 pipe. It had a volume of 55.8 ft³. Orifices ranging from 0.124 to 3.28 in. ID were used. Initial pressure of the vessel was 1250 psig.

Similar data in the same pressure range are available on the Bodega Bay scale model tests.⁽¹³⁾ The vessel in this case was 27 in. in ID, and 21 ft long, and 80 ft³ in volume. The simulated break size was established by orifices and nozzles ranging in size from 0.91 to 5.12 in. in ID.

Other data relative to pressure-temperature transients during blowdown are available from the Mine Safety Appliance Research Corp (MSA) and the Babcock and Wilcox Company. The pressure vessel in the MSA studies had a contained volume of 512 gal. Tests were run on the flow of high temperature water at 2000 psig in size from 0.25 to 1.0 in. Discharge was through a long section of both 4 and 2 in. pipe so that the transient time of blowdown ranged from 3 to 10 min.

Tests at the Babcock and Wilcox Company's Alliance Research Center^(25,26) utilized a primary system with a total isolated blowdown capacity of 51 ft³. The system was designed for 2000 psig at 520 °F and was operated at temperatures as high as 636 °F. It consisted of (1) a storage drum with an inside diameter of 3 ft and a volume of 38 ft³ and (2) 30 ft of 8 in. discharge pipe which led to the rupture assembly. In the latest work 26 tests were run with 2, 3, and 6 in. ruptures at various levels of submergence in the containment tank of water. Initial primary system pressures were usually at 1300 psig. Pressures were recorded in the drum, in the exit piping of the drum, and at the rupture assembly.

3.3 Containment Models

This section briefly reviews the literature dealing with analytical expressions developed to predict pressure and temperature transients in the regions surrounding the primary reactor system during and following the loss of coolant accident in the reactor. Test data available for comparison of these analytical models is also briefly reviewed.

3.3.1 Analytical Models

An analysis of the pressure buildup within the containment regions of the Pathfinder Reactor⁽²⁷⁾ involved the simultaneous solution, by the digital program, of the pressure buildup

equations for five separate regions within the plant and of the rate of flow through 14 different network segments. Condensation to a constant temperature heat sink within each one of the regions was considered.

The Stone and Webster Company has developed a proprietary program (LOCTIC) which considers the pressure temperature transients in a dry containment system following a loss of coolant accident. This is a digital computer program which considers the influence of condensing surfaces of continuously changing temperature and various engineered safeguards, such as spray cooling and air recirculation, upon the transients of the system. The mass and energy input from the reactor vessel is handled by a separate program (LOSCLT), which is run first to provide this input data.

General Electric, APED,⁽²²⁾ has developed a proprietary digital code which analyzes pressure and temperature transients within the various regions of the pressure suppression containment system. The blowdown portion of this program was briefly discussed in Section 3.2.1. In so far as is known at this writing, the program covers only the relatively short duration effects of a blowdown. That is to say that it has not been run to cover the slow build up of pressure due to fission product decay release but is terminated when the dry well and wet well regions reach equal pressure. A brief comparison of the analytical model and the test data is included in the next section.

General Electric, in connection with the design and analysis of the new production reactor at Hanford, developed a program to calculate the pressure transients within the confinement structure following a rupture in the primary coolant loop. The model consists of six regions within the confinement system. Steam from a

pipe rupture can be introduced into any one space at a time. The program simultaneously solves the pressure buildup and fluid flow within and between the six regions. No condensation is accounted for and the steam is assumed to behave as a perfect gas. Because a proprietary digital analog simulation program was used, the program was not published formally.

The Babcock and Wilcox Company⁽²⁸⁾ developed an analog simulation of the pressure and temperature transients within the LOFT Containment Systems during and following the reactor blowdown. The energy input to the containment system from the reactor vessel was assumed to be either (1) introduced instantaneously or (2) introduced at an exponential rate determined by an arbitrary time constant. Energy input from the sensible heat of the core was handled in a similar fashion, ie using an exponential expression. The decay heat from the core was introduced in the program as a function of time as was the energy input from reactor operation for a finite period of time before and during shutdown. Heat loss to the containment dome, the containment sides and floor, structural steel, and the primary shield tank were handled by dividing these structures into as many as three segments and writing differential difference equations for each segment.

The rate of heat loss to the structures is based on a time dependent coefficient of condensation ranging from 640 to 50 Btu/(ft²)(hr)(°F) for concrete.

3.3.2 Test Results

The extensive tests run by PG&E^(11,12,13) in support of the design of Humboldt and Bodega Bay Plants are the best data to date on pressure and temperature transients during a loss of coolant accident. The test facility represented a full scale

1/48 segment of the Humboldt Bay pressure suppression system and a similar scaling of the Bodega System. Instrumentation included pressure and temperature readings at approximately 10 different locations throughout the system. The following parameters were varied during the test.

- Flow rate
- Carrying over of water through the dry well
- Depth of vent submergence in the wet well
- Initial temperature of the wet well pool
- Initial reactor vessel pressure.

The analytical blow down model predicted higher dry well pressure than were experienced, primarily because the model predicted higher than true blowdown rates, ie, rates greater than actually experienced. By varying the amount of assumed carryover of water through the dry well the calculated dry well pressure maxima could be made to coincide with the test data. The pressure and temperatures of the wet well were closely approximated by the model.

The Babcock and Wilcox Company^(25,26) performed a series of tests on vapor suppression. This system design differed from the Humboldt Bay design in that it did not involve a drywell; instead, the reactor vessel was submerged in water. The primary system was that previously discussed in Section 3.2.1 in connection with Babcock and Wilcox Company tests. The containment system was a 7 ft ID by 17 ft long cylindrical vessel. Venting of the simulated break within the containment vessel could be varied so that it occurred either above the surface of the liquid or under submergence up to approximately 10 ft.

In addition to instrumentation to record pressure and temperature transients, the vessel was equipped with accelerometers

to record shock effects. High speed movies of the ruptures were taken.

A total of 26 runs were made on 2, 3, and 6 in. breaks at several depths of submergence (including above the surface) and both with and without a horizontal baffle plate above the venting pipe. Primary system rupture pressure was held constant at 1300 psig. These tests were performed in a closed tank initially at ambient pressure. They serve as a detailed study of the behavior of a wet well pressure suppression system.

Information was obtained which allows one to predict the maximum sustained containment pressure if a baffle or an adequate depth of submergence is available. With a baffle, approximately 1 ft of submergence is adequate; without the baffle, approximately 4 to 6 ft of water is required to effectively condense the venting stream. The shock pressures generated by the rupture were shown to be small and were concluded to present no serious design problem.

4.0 THEORETICAL DEVELOPMENT

4.1 Energy Balance Equations

The procedure in the following discussion is to develop the differential equations for the rate of change of pressure and temperature in each of the regions of the containment system starting from the macroscopic differential equations of mass and energy balance as presented, for example, by Bird, et al.⁽²⁹⁾ Thermodynamic equilibrium is generally assumed to exist in each of the regions (or subregions as subsequently treated) except in one particular modified treatment of the blowdown from the primary vessel.

Figure 4.1 is a pictorial diagram of the Containment Systems Experiment Facility. Figures 4.2 and 4.3 are schematics representing the essential components of a dry containment system and a pressure suppression system. These will more clearly provide a frame of reference for the development of equations which follow.

4.1.1 Region 1 - Simulator Vessel Blowdown

The primary vessel blowdown is developed only for a simple system approximating a boiling water reactor and considers at most only two regions. It is intended to approximate the behavior of the CSE primary vessel but is inadequate to simulate the behavior of a pressure tube system with its many regions and internal resistances. Two cases are developed.

Case I is a simplified treatment which assumes a blowdown from an initially saturated condition (ie no pressurization above saturation pressure) and thermodynamic equilibrium at all points within the vessel. The fluid escaping is assumed to be liquid water followed by a varying-quality steam after all liquid has escaped.

FIGURE 4.1. Containment Systems Experiment

FIGURE 4.2. Schematic of Dry Containment

FIGURE 4.3. Schematic of Pressure Suppression System

Case II is a more complex model. It assumes the fluid can be considered in two zones within the vessel. Zone "a" contains an arbitrary weight fraction at the top which is in equilibrium with the vapor space above it. Zone "b" is the remainder of the water which is assumed to remain at its initial enthalpy during blowdown, ie this water does not lose heat to the surrounding and either enters the blowdown region as a super heated liquid (due to depressurization) or as a two phase stream which has not undergone phase separation. Most models for two phase critical flow through a break do not differentiate between these two entrance conditions. Case II was developed as an attempt to handle the apparent important effect of the ratio of break area to vessel cross sectional area on the average mass velocity of escape. This effect may be a manifestation of the lack of phase separation over a portion of the blowdown.

Case II, in addition to the two zone feature, also treats the existence of subcooled water, ie, pressures above the saturation pressure, due perhaps to pressurization from a noncondensable gas. The blowdown rate is considered to change between four different regimes in sequence, ie the escape of (1) subcooled water, (2) superheated water, (3) saturated water, and (4) high quality steam. Furthermore, the inertial effects of water and the resistance of the core are taken into account, although in the actual running of the program, these two effects are broken out into a separate program which is used only for short duration calculations because of the excessive amount of machine time required.

Case I Simple Blowdown Model. When a single zone is assumed with saturated water and vapor in equilibrium, the macroscopic energy balance equation for region 1, the simulator vessel, during liquid escape, may be written

$$\begin{aligned} \frac{dE_T}{dt} &= \frac{d}{dt} \left[X W_w h_v + (1 - X) W_w h_\ell \right] - \frac{V_1}{J} \frac{dp_1}{dt} \quad (4.1-1) \\ &= G_1^{(3)} A_B h_\ell . \end{aligned}$$

All terms in the above equations should logically carry a subscript (1) to signify the primary vessel system; however, only the mass velocity term is so noted as it will subsequently enter into the equations for the other regions. The superscript on this term signifies the expression for mass velocity of escape of liquid water in equilibrium with the system pressure.

Equation (4.1-1) states that the rate of change of the total internal energy of the primary vessel system (neglecting kinetic and potential energy terms) may be expressed in terms of the rate of change of the mass of steam and water in the system and their respective enthalpies and an expression relating enthalpies and internal energies. It further states that this rate of change is equal to the rate at which energy is leaving the system. The mass balance for the primary system is

$$\frac{dW_w}{dt} = G_1^{(3)} A_B, \quad (4.1-2)$$

The mass of steam and liquid is given in terms of the overall quality of the system X which may be related to the mass of total water in the system and the specific volumes of the steam and liquid water by the equation

$$X = \frac{V_1/W_w - v_\ell}{v_{\ell v}} . \quad (4.1-3)$$

Carrying out the indicated differentiation of the energy balance equation and expressing the time derivatives $\frac{\partial h_v}{\partial t}$ etc. as $\left(\frac{\partial h_v}{\partial p}\right)\left(\frac{dp}{dt}\right)$, one obtains the following equation upon collection of terms.

$$\frac{dp}{dt} = \frac{1}{W_w F(1) + V_1 F(2)} \frac{dW_w}{dt} \quad (4.1-4)$$

where

$$F(1) = \frac{1}{v_{lv}} \left(\frac{dv_v}{dp}\right) + \frac{1}{h_{lv}} \frac{v_v}{v_l} \left(\frac{dh_l}{dp}\right) - \frac{h_l}{h_{lv} v_l} \left(\frac{dv_l}{dp}\right) - \frac{1}{h_{lv}} \left(\frac{dh_v}{dp}\right) \quad (4.1-5)$$

$$F(2) = - \frac{1}{v_l v_{lv}} \left(\frac{dv_{lv}}{dp}\right) - \frac{1}{v_l h_{lv}} \left(\frac{dh_l}{dp}\right) + \frac{1}{v_l h_{lv}} \left(\frac{dh_v}{dp}\right) - \frac{v_{lv}}{v_l h_{lv} J}. \quad (4.1-6)$$

Tables of $F(1)$ and $F(2)$ as functions of p , the saturation pressure of the system, have been prepared and are given in Appendix A along with empirical expressions developed from these tables. Thus, the rate of change of pressure can be directly related to the rate of escape of water in the system.

After all liquid water has been expelled, ie when $X = 1$ or $W_w = V_1/v_v$, the rest of the water escapes as steam. This is an isentropic expansion and the cooling immediately condenses some water and maintains saturated conditions in the vessel. The expression for energy escape from the system becomes

$$G_1^{(4)} A_B [X W_w h_v + (1 - X) W_w h_l] \quad (4.1-7)$$

where $G_1^{(4)}$ = mass velocity of escape of near-saturated steam. The resulting relation between dp/dt and dW_w/dt after differentiation

and collection of terms is

$$\frac{dp}{dt} = \frac{1}{\frac{W_w v_\ell}{V_1} [W_w F(1) + V_1 F(2)]} \frac{dW_w}{dt} \quad (4.1-8)$$

Case II Two Zone Blowdown Model. The two zone model allows consideration of (a) subcooled blowdown and (b) nonequilibrium conditions in the escaping fluid. Figure 4.4 is a schematic representation of the model. Consider the vessel initially

FIGURE 4.4. Schematic Diagram of Two Zone Model

pressurized to some pressure p_T° at an initial temperature T_1° . The total pressure p_T is greater than the saturation pressure p . This is accomplished by adding a noncondensable gas W_A which contributes a partial pressure p_A ; thus, $p_T = p + p_A$.

The calculation of the pressure and temperature transients proceeds on the assumption that the water behaves as though divided into two zones: Zone "a", in equilibrium with the vapor, and Zone "b", discharging at the initial enthalpy of the system which has not had the opportunity to come to equilibrium during the blowdown. The relative initial size assigned to these two zones is arbitrary, ie it is an additional degree of freedom to help fit the model to experimental data. As a guide, however, it would seem logical that a higher assigned distribution of water to Zone "b" would correspond to systems having very short blowdown, ie a high ratio of break area to vessel cross section area.

The energy balance for Zone "a" is slightly different from that for Case I because the volume rather than the mass of water is changing (until Zone "b" is exhausted) and because air is present. The resulting equation is

$$\frac{d}{dt} \left(X W_{w,a} h_v + (1 - X) W_{w,a} h_\ell + W_A h_A \right) - \frac{V_a}{J} \frac{dp_T}{dt} - \frac{p_T}{J} \frac{dV_a}{dt} = 0.$$

(4.1-9)

If the various time derivatives are expressed in the form,

$$\frac{\partial h_\ell}{\partial t} = \left(\frac{\partial h_\ell}{\partial p} \right) \left(\frac{dp}{dt} \right) \left(\frac{dT}{dt} \right) \text{ etc.,}$$

it is possible to relate $\frac{dT}{dt}$ to $\frac{dV_a}{dt}$. However, the rate of change of volume $\frac{dV_a}{dt}$ is directly related to the rate of escape of water from Zone "b" by the equation

$$\frac{dV_a}{dt} = - v_\ell \frac{dW_w}{dt}.$$

The saturation pressure p is a function of T while p_A and hence p_T may be calculated via the gas law knowing W_A and T . The resulting equations for pressure and temperature transients during the period water from Zone "b" is escaping are then

$$\frac{dT}{dt} = \frac{\frac{dW_w}{dt} \left[\frac{v_\ell h_{\ell v}}{v_{\ell v}} + \left(\frac{W_A R}{M_A J} \right) \left(\frac{V_a T}{(V_a - v_\ell W_{w,a})^2} \right) \right]}{\frac{dp}{dT} [W_{w,a} F(3) + V_a F(4)] + W_A \left[C_p - \frac{V_a R}{J M_A (V_a - v_\ell W_{w,a})} \right]}, \quad (4.1-10)$$

$$\frac{dp}{dt} = \frac{dp}{dT} \frac{dT}{dt},$$

and

$$p_T = p + \frac{W_A R T / 144}{M_A (V_a - v_\ell W_{w,a})} \quad (4.1-11)$$

where

$$F(3) = \frac{h_{\ell v} v_\ell}{v_{\ell v}^2} \frac{dv_{\ell v}}{dp} - \frac{h_v}{v_{\ell v}} \frac{dv_\ell}{dp} + \frac{dh_\ell}{dp} - \frac{v_\ell}{v_{\ell v}} \frac{dh_{\ell v}}{dp}, \quad (4.1-12)$$

$$F(4) = - \frac{h_{lv}}{v_{lv}^2} \frac{dv_{lv}}{dp} - \frac{1}{v_{lv}} \frac{dh_{lv}}{dp} - \frac{1}{J}, \quad (4.1-13)$$

and

$$V_a = V_1 - v_l (W_w - W_{w,a}). \quad (4.1-14)$$

Zone "b" is exhausted when $V_a = V$.

During the exhausting of liquid from Zone "a", the energy balance equation is essentially the same as for Case I with the additional term to express the contribution of air to the internal energy of the system. The resulting equation in terms of $\frac{dT}{dt}$ as a function of $\frac{dW_w}{dt}$ for Zone "a" liquid blowdown is

$$\frac{dT}{dt} = \frac{\left[\frac{v_l h_{lv}}{v_{lv}} \right] \frac{dW_w}{dt}}{\frac{dp}{dT} [W_w F(3) + V F(4)] + W_A \left[C_p - \frac{V R}{J M_A (V - v_l W_{w,a})} \right]}. \quad (4.1-15)$$

The blowdown of the steam-air residue after all liquid water has gone is also similar to Case I. The rate of escape of air is assumed to be in proportion to the mass fraction of air remaining and the overall mass velocity assumed to be that of the pure steam. The resulting equation for Zone "a" steam-air blowdown is

$$\frac{dT}{dt} = \frac{\frac{V}{W_w} \left(\frac{h_{lv}}{v_{lv}} \right) \frac{dW_w}{dt} + C_p (T-32) \frac{dW_A}{dt}}{\frac{dp}{dT} [W_w F(3) + V F(4)] + W_A \left(C_p - \frac{R}{J M_A} \right)}. \quad (4.1-16)$$

4.1.2 Region 2 - Dry Well or Dry Containment Enclosure

The energy balance for this region is basically in a very similar form to that for the pressure vessel. The equation is somewhat more complicated because the terms involving flow of fluid from the dry well must take into account the fact that some phase separation occurs between the steam and water as they issue from the simulator, mixes in the dry well, and escapes into the wet well or large containment vessel. In the following equations the fraction of liquid water entrained in the steam-air mixture and carried out of the dry well is called the carry over (CO) and is assumed to be a constant factor during the blowdown. However, an expression relating this parameter to some variable during blowdown (eg time, energy of exit stream, or extent of flashing of exit stream) could easily be worked into the program if the experimental data from CSE tests indicated that this was warranted.

The macroscopic energy balance equation for Region 2; assumed here to be the dry well, may be written

$$\begin{aligned}
 \frac{d}{dt} [X W_w h_v + (1 - X) W_w h + H_A W_A] - \frac{V_2}{J} \frac{dp_T}{dt} \\
 = - A_2 \left\{ \left(\frac{W_A}{W_A + W_{w,m}} \right) G_2 C_p (T-32) \right. \\
 \left. + \left(\frac{W_{w,m}}{W_A + W_{w,m}} \right) G_2 [X_m h_v + (1 - X_m) h_l] \right\} \\
 + G_1 A_b h_b - Q_c
 \end{aligned} \tag{4.1-17}$$

where X_m = quality (weight fraction steam) in the vapor space

$$= \frac{1}{1 + CO \left(\frac{V_2/W_w - v_v}{V_2/W_w - v_\ell} \right)}, \quad (4.1-18)$$

$W_{w,m}$ = weight of water (liquid and vapor) in the vapor space

$$= \frac{(X)(W_w)}{X_m},$$

and

Q_c = heat lost by condensation.

Inserting these relationships, differentiating as before, and collecting terms, one obtains a relationship between dT/dt and dW_w/dt in Region 2. The equation for the program of rupture directly into the vessel (ie neglecting any effect of the dry well vessel because of discharge through an immediate adjacent port directly into the containment vessel) is the same.

$$\frac{dT}{dt} = \frac{-\frac{dW_w}{dt} [F(5)] - \frac{A_2 G_2}{\alpha + \beta + \gamma} [C_p (T-32)\alpha + h_v\beta + h_\ell\gamma] + G_1 A_b h_b - Q_c}{\frac{dp}{dT} [W_w F(3) + V_2 F(4)] + W_A \left(C_p - \frac{R}{JM_A} \right)} \quad (4.1-19)$$

where

$$F(5) = \left[h_\ell - h_{\ell v} \left(\frac{v_\ell}{v_{\ell v}} \right) \right] \quad (4.1-20)$$

$$\alpha = W_A v_{\ell v} \quad (4.1-21)$$

$$\beta = V_2 - W_w v_\ell \quad (4.1-22)$$

$$\gamma = -CO(V_2 - W_w v_v) \quad (4.1-23)$$

Appendix A lists values of $F(5)$ as well as the empirical functions which have been developed to relate α, β, γ , and $F(5)$ to the saturation pressure of the region.

4.1.3 Region 3 - Containment Vessel or Wet Well

The energy balance for the containment vessel or the wet well enclosure is essentially the same as Equation 4.1.19, except terms involving G_2 are not involved (no gas escapes from the enclosure. In addition, the program for pressure-temperature transients in dry containment include terms accounting for the addition of decay heat into the system, temperature and pressure reduction by spray suppression and air recirculation, and the removal of heat by condensation on the enclosure walls. These will be discussed subsequently. The terms involving the addition of heat due to fission product decay (and also reactor heat during the scram shutdown) and the removal of heat via condensation on the walls could be easily included into the calculation of the dry well pressure-temperature transients. They would appear in the numerator of Equation 4.1-19, which has the units of heat input per unit time (Btu/sec). The denominator has the units of heat input per unit of temperature rise (Btu/°F).

The energy balance for the wet well (or the containment vessel via discharge through the dry well) is

$$\frac{dT}{dt} = \frac{-\frac{dW_w}{dt} [F(5)] + \frac{A_2 G_2}{\alpha + \beta + \gamma} [\alpha C_p (T_2 - 32) + \beta h_v + \gamma h_\ell] + Sh_S + Q_D - Q_R - Q_C}{\frac{dp}{dT} [W_w F(3) + V F(4)] + W_A \left(C_p - \frac{R}{JM_A} \right)} \quad (4.1-24)$$

As previously noted all terms could logically carry the subscript (3) unless otherwise noted, indicating reference to conditions in Region 3. The last four terms are those previously mentioned; S is the rate of spray water addition, Q_D the rate of heat added to the system via decay heat, Q_R the amount of heat removed via the recirculation loop, and Q_C the amount removed by condensation.

4.2 Blowdown Flow Rates

The mass velocity of two phase flow from the primary vessel has been calculated by using a model developed by F. J. Moody.⁽⁷⁾ There is reason to believe that the predicted rates are too high, but essentially any model from which one can develop an empirical expression for mass velocity as a function of stagnation pressure and enthalpy could be fitted into the present program. The expressions developed from the Moody model for the four regimes that exist in sequence during blowdown are as follows.

- Subcooled water

$$\begin{aligned} G^{(1)} &= 0.95 \left[\frac{2g_c}{v_\ell} (p_T - p^\circ)(144) \right]^{1/2} \\ &= 590 (p_T - p^\circ)^{1/2}, \end{aligned} \quad (4.2-1)$$

where

p° = initial value of the saturation pressure corresponding to T° , the initial temperature;

- Superheated water or steam-water mixture

$$G^{(2)} = 13,350 + 6.31 p_T - 22 h_o, \quad (4.2-2)$$

where

h_o = initial enthalpy of water in Zone "b";

- Saturated water from Zone "a"

$$G^{(3)} = 4550 + 3.23 p_T \quad (4.2-3)$$

- Steam (plus air) discharge

$$G^{(4)} = 2.06 p_T + 100. \quad (4.2-4)$$

Actually, during the subcooled portion of the blowdown, the rates calculated by $G^{(1)}$ and $G^{(2)}$ are compared; and the switchover occurs when $G^{(2)}$ becomes greater than $G^{(1)}$.

Similar types of expressions have been developed by Nahavandi⁽¹⁹⁾ utilizing the Fauske⁽⁵⁾ model.

A later model by F. J. Moody⁽¹⁾ based on discharge through pipe sections of finite length would seem to be a logical candidate for an improved set of expressions.

4.3 Core Pressure Drop

The stagnation pressure in the region of the break, signified by p_L is actually somewhat different from p_u because of the static head, pressure drop through the reactor core, and inertial effect just subsequent to the rupture. The relationship between p_L and p_u may be obtained by the following equation which is an expression for the macroscopic momentum balance in the vertical direction as stated by Bird, et al⁽²⁹⁾ (refer to Figure 4.5).

$$\frac{d}{dt} \left(\frac{1}{g_c} \int \rho \bar{U} dV \right) = 144 (p_u - p_L) A_v + \frac{g}{g_c} (W_u + W_L) - 144 A_T R_c \theta \left(\frac{dW_u}{dt} \right)^2 \quad (4.3-1)$$

This equation states that the rate of change of momentum of the fluid in the entire volume is equal to the forces exerted on the fluid, namely the difference in pressure top to

bottom, the weight of the fluid, and the resistance to flow through the reactor core. The left hand side of the equation may be further developed into the expression,

$$\frac{d}{dt} \left(\frac{1}{g_c} \int \rho \bar{U} dV \right) = \frac{d}{dt} \left\{ \frac{1}{g_c} \left(-\frac{dW_u}{dt} \right) \left[L_T + \frac{1}{A_V} \left((W_u + W_L) v_\ell - L_T A_T \right) \right] \right\} , \quad (4.3-2)$$

which states that the momentum of the fluid is due to velocity in the core and in the vessel. Carrying out the differential and combining with the right hand side, one obtains the equation

$$\frac{d^2 W_u}{dt^2} = \frac{1}{B} \left[A \left(\frac{dW_u}{dt} \right)^2 - C(p_u - p_L) + (W_u + W_L) \right] \quad (4.3-3)$$

where

$$A = 144 A_T g_c R_c \theta$$

$$B = L_T \left(1 - \frac{A_T}{A_V} + \frac{L_V}{L_T} \right)$$

$$C = 144 g_c A_V$$

$$Re = (4fL/D)/2g_c \rho_\ell A_T^2$$

f = fraction factor for flow resistance of the core.

This equation gives the rate of water flow from the upper region (above the core) of the primary vessel. The pressure

and temperature in the upper region varies as calculated by Equation A.2-2. In the lower region, either saturated or sub-cooled conditions may exist. The quantity of water below the core is governed by the equation

$$\frac{dW_L}{dt} = - G_1 A_B - \frac{dW_U}{dt} . \quad (4.3-4)$$

If the quantity W_L is such that $W_L v_\ell < V_L$, then saturation conditions exist and the equation for the pressure transient is

$$\frac{dp_L}{dt} = \frac{1}{W_L F(1) + V_L F(2)} \left[\frac{dW_L}{dt} + \frac{v_\ell v}{h_{\ell v} v_\ell} \frac{dW_U}{dt} (h_{\ell U} - h_{\ell L}) \right] . \quad (4.3-5)$$

If $W_L v_\ell > V_L$, the pressure transient is calculated from the compressibility of liquid water which can be expressed as

$$\frac{dp_L}{dW_L} = \frac{1}{V_L} \frac{dp_L}{d\rho_\ell} \quad \text{or} \quad \frac{dp_L}{dt} = \frac{1}{V_L} \frac{dp_L}{d\rho_\ell} \frac{dW_L}{dt} , \quad (4.3-6)$$

An empirical expression for $\frac{dp_L}{d\rho_\ell}$ can be written as

$$\frac{dp_L}{d\rho_\ell} = 2570 - 0.8 p_{\text{saturation}} \quad (4.3-7)$$

These expressions have been included into the program for Case II but have been used only for short periods right after the break because of the excessive machine time required. However, their inclusion in the program does allow one to calculate the maximum core pressure drop.

4.4 Heat Transfer

Heat transfer considerations enter into the development of the transient equation at several points: heat transfer from the primary vessel wall and core to the fluid during blowdown; heat transfer via condensation and conduction on the walls of the various vessels and heat sinks within the system; heat transfer of fission product decay heat from the reactor core to the system subsequent to blowdown; heat removal in the air recirculation loop; and heat removal via spray suppression.

4.4.1 Conduction Through Walls

The treatment of transient heat conduction through a wall is basically similar in both the primary vessel and the containment vessel. It involves the simultaneous solution of the differential difference equations for a slab with film resistance at one or both walls. A constant value of the condensation film coefficient in the containment vessel has been used to date, but in a subsequent section the development of a variable condensation coefficient, as well as the variable boiling coefficient used in the primary vessel, will be discussed.

Figure 4.5 is a schematic of the system to which the following differential difference equations apply. Utilizing the approach developed by Dusenberre,⁽³⁰⁾ one may write the differential equation for the temperature at the surface and the centers of the slabs into which the wall is assumed to be divided.

$$\frac{dT_o}{dt} = \alpha \left(\frac{N}{L} \right)^2 \left[2T_1 - 2T_o \left(1 + \frac{h_{eff}}{k} \frac{L}{N} \right) + 2 \left(\frac{h_{eff}}{k} \frac{L}{N} \right) T_{steam} \right]$$

(4.4-1)

Temperature Gradients Through Wall

$$\text{Flux at Wall (Outside)} = h_{\text{eff}} (T_s - T_o)$$

$$\text{Flux at Wall (Inside)} = \frac{k}{\Delta X} (T_o - T_1)$$

Capacity of Outer
Half of Section (1)

$$\frac{\Delta X}{2} \rho C_P \frac{d(T_o + T_1)}{2dt} \approx \frac{\Delta X}{2} \rho C_P \frac{dT_o}{dt}$$

$$\text{Equation: } \frac{hL}{kN} (T_s - T_o) - (T_o - T_1) = \frac{L^2}{2\alpha N^2} \frac{dT_o}{dt} \text{ for } T_o$$

FIGURE 4.5. Schematic of Heat Transfer Through Vessel Wall

$$\frac{dT_1}{dt} = \alpha \left(\frac{N}{L} \right)^2 (T_o - 2T_1 + T_2) \quad (4.4-2)$$

$\frac{dT_2}{dt}$ through $\frac{dT_5}{dt}$ are similar to $\frac{dT_1}{dt}$

$$\frac{dT_6}{dt} = \alpha \left(\frac{N}{L} \right)^2 \left[2T_5 - 2T_6 \left(1 + \frac{h_o L}{kN} \right) + 2 \left(\frac{h_o L}{kN} \right) T_{air} \right] \quad (4.4-3)$$

where

$\alpha = k/c_p \rho$ for the wall material

N = number of increments used.

The heat loss from the system is calculated via the equation

$$Q_c = \frac{h_{eff} A}{3600} (T_{steam} - T_o). \quad (4.4-4)$$

A single loop to calculate Q_c is included in the program of pressure-temperature transients in the dry containment vessel. Heat loss to heat sinks of different sizes and shapes could be approximated by assuming that their effects were essentially the same as a number of standard shapes of equal mass, thermal diffusivity, and surface area. Additional loops could be added to account for the behavior of these stand-in shapes. A similar loop could also be added to the program for transients in the dry well.

During blowdown of the primary vessel, the vessel walls are a source of energy because of their considerable mass and heat capacity. If the rupture were a small one and the blowdown occurred over a relatively long period, the energy input from the walls could constitute almost 30% that of the liquid in the vessel.

The questions of (1) the significance of this source during a rapid blowdown and (2) the temperature gradients that would develop in the vessel walls during blowdown led to a study of heat transfer during this event. The approach was essentially the same as previously described except that instead of equally spaced increments of distance through the wall, square root spacing was used to give greater accuracy near the wall, ie the equations were written for equal increments of $Y = \sqrt{X}$ where X is the distance through the wall. The equations to be solved then become

$$\frac{dT_o}{dt} = 2 \frac{\alpha N^4}{L^2} \left[T_1 + \frac{h_b L}{kN^2} T_s - T_o \left(1 + \frac{h_b L}{kN^2} \right) \right] \quad (4.4-5)$$

$$\frac{dT_n}{dt} = \frac{\alpha N^4}{2L^2} \left[\frac{1}{n(2n+1)} (T_{n+1} - T_n) - \frac{1}{n(2n-1)} (T_n - T_{n-1}) \right] \quad (4.4-6)$$

for $n < N$ (the number of slabs into which the system is divided).

$$\frac{dT_N}{dt} = - \frac{\alpha N^4}{2L^2} \left[\frac{1}{N(2N-1)} (T_N - T_{N-1}) \right] \quad (4.4-7)$$

These equations actually assume an adiabatic wall at a distance $(N + 1/2)$. This assumption has no effect over the time span involved in these calculations.

4.4.2 Heat Transfer Coefficients

Condensation Coefficients. As stated in Section 4.4.1 a constant value is used for the film (or condensation) coefficient

at the containment vessel wall. An expression for a variable coefficient is developed in this section taking into account the ratio of condensibles to noncondensibles in the containment atmosphere. Variations in the effective film thickness, due to convection velocity over the surface, can be accounted for by varying the values of the respective coefficients for heat and mass transfer through a film, but this aspect is not treated in detail here.

Colburn and Hougen^(31,32) expressed the heat removal in the presence of simultaneous heat and mass transfer as

$$h_g (T_s - T_i) + k_g M_s \lambda (p_s - p_i) = h_{eff} (T_s - T_o).$$

(4.4-8)

Figure 4.6 is a schematic of the situation. From this, one can

Heat Transfer Through Air Film

$$h_g(T_s - T_i) + k_g M_s \lambda (P_s - P_i) = h_{eff}(T_s - T_o)$$

$$h_{eff} = h_g \left[1 + \left(\frac{0.9 \lambda M_s}{C_{pm} M_m} \right) \frac{\ln \left[\frac{p_t - p_i}{p_t - p_s} \right]}{T_s - T_o} \right]$$

FIGURE 4.6. Condensation Through an Air Film

obtain the equation

$$h_{eff} = h_g \frac{h_c - h_{eff}}{h_c} + k_g M_s \lambda \frac{(p_s - p_i)}{(T_s - T_o)}$$

$$= \frac{h_g h_c}{h_c + h_g} \left(1 + \frac{k_g}{h_g} M_s \lambda \frac{p_s - p_i}{T_s - T_o} \right) . \quad (4.4-9)$$

If one further assumes the Colburn analogy is applicable in this situation, ie

$$J_H = \frac{h_g}{C_p G} \left(\frac{C_p \mu}{k} \right)^{2/3} = J_D = \frac{k_g p_{bm}}{G_M} \left(\frac{\mu}{p D_v} \right)^{2/3} , \quad (4.4-10)$$

one obtains

$$\frac{k_g}{h_g} = \frac{1}{p_{bm} C_{pm} M_m} \left(\frac{k}{D_v C_p \rho} \right)^{2/3} . \quad (4.4-11)$$

Since $h_c \gg h_g$ and the ratio of the Prandtl and Sherwood numbers is roughly a constant near 0.8, the approximate equation reduces to

$$h_{eff} = h_g \left[1 + \left\{ \frac{0.9 \lambda M_s}{C_{pm} M_m} \right\} \ln \left(\frac{p_T - p_i}{p_T - p_s} \right) \right] \frac{1}{(T_s - T_o)} \quad (4.4-12)$$

The term in the brackets may be considered constant during a blowdown while the log pressure and the temperature difference terms are constantly changing but in a manner that is known continuously by other calculations in the program. Thus, the effect of varying fractions of noncondensibles can be introduced. The effect of a changing boundary-layer condition, due to convection streams set up by the blowdown, would have to be introduced in an

arbitrary fashion in the value of h_g . This may be an even more significant factor than the effect of changing steam-air ratio. A representative value of h_{eff} , based on the above equation and having the value of h_g equal to 5 Btu(hr)(ft²)(°F), is about 50 Btu(hr)(ft²)(°F).

Boiling Coefficients. The program for calculating temperature gradients in the primary vessel during blowdown was initially run using a constant of h (boiling) = 1000 Btu(hr)(ft²)(°F). However, it was subsequently realized that much higher rates of heat transfer might occur so an attempt was made to estimate what the values might be. For film boiling, the Rohsenow⁽³³⁾ equation was used.

$$\frac{C_L (\Delta T)}{h_{lv}} = C_{sf} \left[\frac{q/A}{\mu_l h_{lv}} \sqrt{\frac{g_c \sigma}{g (\rho_l - \rho_v)}} \right]^{1/3} (Pr_l)^{1.7} \quad (4.4-13)$$

The burnout (or critical) heat flux was approximated by the equation given by Zuber⁽³⁴⁾

$$\frac{q}{A} = 0.13 \rho_v h_{lv} \left[\frac{\sigma (\rho_l - \rho_v) g}{\rho_v^2} \right]^{1/4} \left[\frac{\rho_l}{\rho_l + \rho_v} \right]^{1/2} \quad (4.4-14)$$

The minimum flux for film boiling and the temperature difference at which it occurs is given by Rohsenow⁽³⁵⁾ as respectively

$$\frac{q}{A} = 0.09 \rho_{lv} h_{lv} \left[\frac{g (\rho_l - \rho_v)}{(\rho_l + \rho_v)} \right]^{1/2} \left[\frac{\sigma}{\rho_l - \rho_v} \right]^{1/4} \quad (4.4-15)$$

and

$$\Delta T_{\min} = 0.127 \frac{\rho_{\ell V} h_{\ell V}}{k_{\ell V}} \left[\frac{g(\rho_{\ell} - \rho_V)}{\rho_{\ell} + \rho_V} \right]^{2/3} \left[\frac{g_C \sigma}{g(\rho_{\ell} - \rho_V)} \right]^{1/2} \left[\frac{\mu_{\ell}}{g_O(\rho_{\ell} - \rho_V)} \right]^{1/3} \quad (4.4.16)$$

In order to utilize these equations they had to be reduced to empirical functions of the saturation pressure and temperature. This was done by calculating the values relative to the value at $p = 1000$ psia and keying to an experimentally known value in each case. The results were as follows

$$\text{Nucleate Boiling} \quad h = 5.42 p^{0.56} \Delta T^2 \quad (4.4-17)$$

$$\text{Critical Heat Flux } h\Delta T_{\max} = 1.7 \times 10^6 F(p) = Q_{\max} \quad (4.4-18)$$

$$\text{Transition Boiling} \quad h = Q_{\max} \Delta T \left(\frac{\Delta T_{\max}}{\Delta T} \right)^{62/p^{0.64}} \quad (4.4-19)$$

for $h > 530$

$$\text{Film Boiling} \quad h = 530 \quad (4.4-20)$$

$F(p)$ is an empirical function of the pressure and is tabulated in Appendix B.2. These empirical relationships were used in conjunction with the calculated surface and fluid temperature and the saturation pressure to give a more realistic picture of the fluxes and temperature gradients that might develop in the primary vessel during blowdown. The results are discussed in Chapter 5.

4.4.3 Rough Heat Transfer Approximations

Several sources and sinks with respect to the energy inventory of the system have been treated in very crude fashion. Some of these warrant further development as the CSE program develops.

The contribution of fission product decay heat is one that is very difficult to model because of the variety of ways in which this heat can be transferred to the system. If a source of fluid continued to be supplied to the core even after the blowdown had been completed, this energy could be modeled as a stream of known, but decreasing, enthalpy. If the core became dry, meltdown would possibly occur and much of the decay heat would go to increasing the sensible heat of the reactor vessel system and finally extend into the system via a combination of radiation, conduction, and convection from the vessel.

Because of this uncertainty, the decay heat was simply included as a source of energy which was constant over an initial period (during which time it was assumed that the energy in excess of that introduced into the program went into increasing the sensible heat of the primary system) and then decreases exponentially in accordance with the behavior of mixed fission products. Specifically, as the program stands, the heat input is constant during the first 50 sec at a rate equal to the decay heat rate at 50 sec after shutdown and then decays at a rate given by the equation

$$Q_D = 0.1135 P t^{-0.225} \quad (4.4-21)$$

where P = the power level of the reactor.

This equation is a fit of the decay heat curve after 50 sec from a reactor which has previously operated at a power level P for an infinite length of time.

The heat removed by the air recirculation loop is at present considered to be just a constant rate which is actuated after an arbitrary delay time. When the loop has been further defined (equipment size, type, etc.), this term should be modeled to fit the operation more realistically.

The effects of spray suppression are introduced simply as a quantity of water which is assumed to come to immediate equilibrium with the system temperature because of the great heat transfer area of the spray droplets. At first glance this term appears to increase the energy input to the system, which is correct; however, it also increases the mass and therefore contributes even greater to the denominator of the energy balance equation.

4.5 Dry Well Downcomer Flow

A study of transient flow behavior of steam and air leaving the drywell must address itself to three questions: (1) what quantity of water must be cleared from the downcomer pipes before venting to the wet well takes place; (2) how long does it take to clear the downcomers; and (3) what governs the flow through the downcomers?

4.5.1 Equivalent Downcomer Submergence

Moody⁽²²⁾ presents a quantitative discussion of the dynamic effects of water expulsion from a downcomer which results in an "extra mass of water" or "extra length" of downcomer submergence. The procedure involves equating the total kinetic energy of a fluid system in potential flow emanating from a point source to the kinetic energy of a column of fluid in a downcomer of the proper equivalent length. The results, presented in Figure 4.7, gives the downcomer equivalent length for two cases, a rectangular pipe in a semiinfinite channel and a circular pipe in a semiinfinite ocean.

FIGURE 4.7. Downcomer Equivalent Length

4.5.2 Downcomer Clearing Time

The differential equation for the depth of submergence as a function of time is obtained from a force balance of the column of fluid remaining in the downcomer. Figure 4.8 is a schematic of the situation. Equating the rate change of momentum of the column of fluid to the net force acting on a verticle downcomer (neglecting friction) results in the equation

$$\frac{d}{dt} \left[\frac{1}{g_c} \rho (L + \Delta L) A_2 \cdot \left(-\frac{dL}{dt} \right) \right] = \frac{1}{g_c} \rho_\ell (L + \Delta L) A_2 + (p_2 - p_3) (144) A_2$$

$$+ \frac{1}{g_c} \rho_\ell (L + \Delta L)^0 A_2$$

FIGURE 4.8. Schematic of Vent Clearing

which may be expanded to give

$$\frac{d^2L}{dt^2} = -g - \frac{1}{(L + \Delta L)} \left[\frac{144g_c (p_2 - p_3)}{\rho_l} + \left(\frac{dL}{dt} \right)^2 - (L + \Delta L)g \right]. \quad (4.5-1)$$

This equation differs slightly from that given by Moody⁽²²⁾ in that it includes the term $(dL/dt)^2$. However, this does not appear to markedly decrease the calculated vent-clearing time.

4.5.3 Ideal Gas Flow in Vents

If one assumes that the steam-atomized liquid material flowing in the vent behaves as an ideal gas in a constant area

pipe flowing adiabatically with friction, the results of theoretical developments relating pressure drop to mass velocity of flow may be utilized (eg Reference 36). These results may be expressed as three equations; one relates the mass velocity of flow to the entrance mach number, and the other two relate the entrance mach number to the frictional pressure drop and the pressure ratio. Thus, they are

$$G_3 = \psi(M_e) \sqrt{\frac{p_3}{v_{3m}}} ; \quad \psi(M_e) = \frac{M_e \sqrt{kg_c}}{\left(1 + \frac{k-1}{2} M_e^2\right)^{\frac{(k+1)}{2(k-1)}}} \quad (4.5-2)$$

$$\frac{p_3}{p_2} = \frac{M_e}{M_3} \sqrt{\frac{\left(1 + \frac{k-1}{2} M_e^2\right)^{\frac{(1+k)}{(1-k)}}}{\left(1 + \frac{k-1}{2} M_3^2\right)}} \quad (4.5-3)$$

$$\frac{4fL}{D} = \frac{1-M_e^2}{kM_e^2} + \frac{1-M_3^2}{kM_3^2} + \frac{k-1}{2k} \ln \frac{M_e^2}{M_3^2} \left(\frac{1 + \frac{k-1}{2} M_3^2}{1 + \frac{k-1}{2} M_e^2} \right) \quad (4.5-4)$$

A direct relationship expressing G_2 as a function of p_3/p_2 , $4fL/D$, and v_{2m} is required for the program. Because of the very unwieldy nature of the above equations, this relationship was obtained as an empirical approximation. First, values of M_e were obtained at specific values of $4fL/D$ and p_2/p_3 from a graphical representation of the simultaneous solution of the last two equations (Figures D-1, D-2, Appendix D). This allowed values of $\psi(M_e)$ to be calculated. The results can be approximated to within 5% by the relationship,

$$\psi = [1.18 - 0.269 \ln(4fL/D)] F \psi \left(\frac{p_3}{p_2} \right) \quad (4.5-5)$$

up to a limiting value of $p_3/p_2 = 1.9 \times 0.31(4fL/D)$. Above this value, ψ is kept constant at the limiting value. Tabular values of $F \psi \frac{p_3}{p_2}$ are given in Appendix D. The limiting value of p_3/p_2 exists due to the onset of sonic flow in the downcomer at this pressure ratio.

5.0 EVALUATION OF ANALYTICAL MODEL

Inasmuch as the CSE is not operational as yet, direct comparison between the mathematical model and the system has not been made. However, a preliminary comparison between the analytical model and an experimental system can be made in the case of the blowdown and pressure suppression tests run by PG&E in support of the design of the Humboldt and Bodega Bay reactors. There was insufficient time in this study to exhaustively search the literature for other data involving blowdown and either pressure suppression or pressure containment against which the model could be compared. This chapter discusses principally the characteristics of the solutions generated by the model and compares the solutions to experimental data where they exist.

5.1 Primary System Model

5.1.1 Comparison with Measured Blowdown Rates

Data obtained from PG&E's Bodega Bay Reactor (Test No. 16) and the Humboldt Bay Reactor (Test No. 17) are plotted in Figures 5.1 and 5.2. Also plotted in these figures are the solutions generated by the analytical model. If the break area used in the analytical model is equal to the nozzle area used in the tests, the calculated rate of pressure decay is considerably faster than that indicated by the data. Incidentally, this situation was also true for the proprietary model developed by F. J. Moody for General Electric (henceforth referred to the GE model).

The faster theoretical pressure decay is believed to be traceable to the model used for the critical flow through the break although conceivably it could result from incomplete

FIGURE 5.1. Theoretical and Measured Pressure Transients in Primary Vessel Bodega Test No. 16

FIGURE 5.2. Theoretical and Measured Pressure Suppression Transients Humboldt Test No. 17

ruptures in the nozzle region. The theoretical pressure decay for a hypothetical smaller break area is included in Figure 5.1 and corresponds fairly close to the data. The calculated curve has a knee in the pressure decay curve at approximately 950 psi, which is indicative of an initial liquid flow from the break, followed by the homogeneous mixture flow when most of the liquid has been removed from the system. If the flow during the blowdown were homogeneous, no knee would exist. Although a knee is evident in the test data curve, it does not appear to be as pronounced as the theoretical curve; this characteristic indicates that possibly homogeneous flow did occur while a considerable amount of liquid still remained in the system.

As mentioned in Section 4.2, there is basis for the belief that the rates predicted by the Moody model are too high. His model predicts rates considerably higher than those predicted by the Fauske⁽⁵⁾ model for a saturated liquid at approximately 1000 psi. A later paper by Moody revises his original model to include the effect of friction. This work has not been incorporated in the present analytical model but may be the basis for an improved blowdown rate prediction.

Figure 5.1 also gives the result of analytical calculations based on the two zone blowdown model, still using the original Moody model for critical flow from the break. The three curves developed from the two zone model use a break area equal to the nozzle area of the test. All of them give pressure decay curves which are faster than the data. However, the three curves do demonstrate the extra degree of freedom that exists in the two zone model and do show that, as one reduces the volume of liquid that is assumed to maintain thermodynamic equilibrium during the blowdown, the original pressure decay is more rapid but the overall length of blowdown is increased.

Decreasing the volume of liquid that is in equilibrium with the vapor above the system is equivalent to increasing the mass of liquid that is assumed to never reach equilibrium during the blowdown situation; hence, a small equilibrium volume more nearly simulates a very rapid blowdown in which equilibrium is not achieved throughout much of the liquid. The extra degree of freedom introduced by the two zone model may allow it to account for the difference in average blowdown rate observed from systems involving large breaks and those involving small breaks. A possible further modification and potential improvement in the model would be the adaptation of the Margolis-Redfield calculation of bubble disengaging velocity and the resulting computation of the fluid state at the break.

The PG&E tests shown in Figures 5.1 and 5.2 indicate a very sudden initial drop in vessel pressure, followed by partial recovery. Moody discusses this phenomena and attributes it to delay in initial steam formation. Expanding vapor bubbles must accelerate surrounding liquid before vapor formation and decompression are near equilibrium. The pressure can, therefore, be partially recovered at first. There does not appear to be any mechanism predicted by the theoretical model that would duplicate this behavior.

5.1.2 Effects of Metal Energy

Heat transfer occurs from the primary vessel to the remaining steam-water mixture as the saturation temperature drops. The quantitative effect of this heat addition was evaluated through the use of the program developed to calculate temperature gradients in the vessel walls. When a term expressing the energy input from the walls of the reactor vessel is included in the macroscopic energy balance for the primary system, the resulting equation is

$$\frac{dp}{dt} = \frac{1}{W_w F(1) + V_1 (F_2)} \left[\frac{dW_w}{dt} + \left(\frac{v_{lv}}{h_{lv} v_l} \right) h_B A_s (T_s - T_{st}) \right] \quad (5.1-1)$$

where $v_{lv}/h_{lv} v_l$ can be expressed as an empirical function as listed in Appendix B.

In this equation, T_s is the surface temperature of the reactor wall and T_{st} is the temperature of the water or steam within the primary vessel. The surface temperature is calculated by means of a series of differential difference equations as discussed in Section 4.4.1. The value of T_{st} is obtained from a function generator expressing the saturation temperatures as a function of the saturation pressure within the system.

The heat transfer coefficient h_B was obtained from the various empirical equations as discussed in Section 4.4.2. These relationships gave the heat transfer coefficient as a function of the system pressure and the temperature difference $T_s - T_{st}$. The value of the area of heat transfer A_s was arbitrarily assumed to be proportional to the amount of liquid water remaining in the system; ie it was assumed that heat was transferred only to liquid water and not to steam because of the very much higher rate of heat transfer to the former.

The percentage contribution to the total energy by heat transfer was easily obtained from the output of the program by comparing the magnitude of the two terms in the brackets of the above equation. For the rapid blowdown conditions which were evaluated using this program, the magnitude of the heat transfer contribution, the second term in the brackets, was never greater than 10% that of the first term and generally was very much less

than the first term. Under these conditions it was computed that the contribution of stored energy in the reactor walls to the blowdown behavior was very slight during a rapid blowdown. The heat transfer effect, however, would be more pronounced in blowdowns requiring longer times. As an example of the potential contribution, one may sight data given by Moody on the total steam-water energy and vessel metal energy initially in the system for Humboldt Test No. 17. The steam-water energy was 1,050,000 Btu, the vessel metal energy was 200,000 Btu.

5.1.3 Temperature Gradients

The possible deleterious effects of extremely steep temperature gradients in the wall of the primary vessel during the rapid blowdown has received some discussion in connection with both the CSE and the LOFT tests. The program mentioned in the previous section was developed principally to allow a quantitative calculation of these gradients to be made.

The result of a typical calculation is given in Figure 5.3. The calculational parameters used are those typical of a rapid blowdown from the CSE vessel. One shortcoming of the program is that it neglects the existence of the 1/8 in. stainless steel cladding of the inside surface of the vessel. Two sets of curves are given in the figure. The first was calculated by using a heat transfer coefficient of 1000 and 6 equally spaced slabs through the thickness of the wall, and the second was calculated by using constantly changing values of h_p as outlined in Section 4.4.3 and using 6 slabs with square root spacing through the thickness of the walls. The latter allows greater accuracy of calculation near the surface of the wall.

Both of these calculations indicated that, at the time at which all water was expelled from the vessel, the temperature

However, on two program runs comparing the model with Bodega Test No. 16, one calculation was made assuming no noncondensable gas and an initial temperature sufficient to give initial pressure at saturation. A second run was made using a noncondensable gas and an initial temperature such that saturation pressure was 100 psi below the initial pressure, and the difference was supplied by the noncondensable gas. The pressure decay curves for the two runs were nearly identical, (Figure 5.1) ie the presence of the noncondensable gas had no detectable effect.

However, in cases with very considerable overpressurization with noncondensable gas, ie considerable subcooling, the pressure decay would be very rapid at first and would closely follow the isotopic expansion curve for the gas.

5.1.5 Core Pressure Drop

The total force on the reactor core during the blowdown incident is of very great concern. Conceivably, the magnitude of the dynamic head acting on the core would be sufficient to cause the core mountings to fail and the core to become dislocated. One version of the two zone blowdown model includes terms which allow the calculation of the core pressure drop as outlined in Section 4.3. In principle this program could be used to calculate the entire pressure decay curve as well as the core pressure drop. However, the machine time involved in this calculation is so great that one would probably only use it to calculate the initial pressure drop and use the other simplified version of the two zone model to calculate the entire pressure decay curve.

In keeping with the purpose of the study reported herein, no systematic study was made of the effect of system parameters on the maximum value of the fluctuating core pressure drop. The results plotted in Figure 5.4 were based on what were assumed to be typical values for the CSE vessel equipped with a simulated

FIGURE 5.4. Typical Core Pressure Drop Calculation

reactor core. In this particular case the system is subcooled by virtue of pressurization to 2500 psia, while the saturation pressure is 1800 psia. Despite the rapid efflux of water from the region below the core, this region remains subcooled during the period covered by the calculation. The oscillations noted by Parks are observed though they appear to be rapidly damped out.

5.2 Dry Well Model

5.2.1 Comparison with Measured Pressures

Figure 5.2 shows the comparison between the calculated pressure relationships and the test data from PG&E Humboldt Test No. 17. Figure 5.5 presents the same type of comparison with PG&E's Test No. 16 for the Bodega Plant. The results show that the model duplicates the test results fairly well; and also follows rather closely the results from the proprietary GE Model. The dry well pressure and temperature depends on both the blowdown and vent flow rates; and, consequently, deficiencies in either or both of these flow models are reflected in the transient pressures.

5.2.2 Effect of Vent Carry-Over

Figure 5.5 shows that the theoretical dry well maximum pressure increases with increasing carry-over in the vent; the assumption of air-vapor mixture flow with zero carry-over results in the lowest dry well pressure. Carry-over is defined as that fraction of the liquid water entrained in the steam-air mixture that is carried out of the dry well. Thus, zero carry-over assumes that all liquid water, including all atomized liquid which issues from the rupture of the primary vessel, is deentrained within the dry well and does not pass over into the wet well. A 100% carry-over

FIGURE 5.5. Effect of Assumed Vent Carry-Over on the Calculated Pressure Transients in the Dry and Wet Wells: Bodega Test No. 16

then assumes all water issuing from the primary vessel is carried right out through to the wet well with no liquid remaining in the dry well.

Since the net flow rate is based on the behavior of a perfect gas and on the relative quantity of air and steam escaping^{*}, the higher that the specific enthalpy of the steam-water mixture is, the more rapid the egress of the energy from the system. As the fraction of carry-over increases, the specific enthalpy of the steam-water stream decreases. Consequently the total energy in the dry well at any particular time will be higher and the quantity of carry-over through the vents into the wet well will be higher.

Figure 5.5 indicates that the test conditions for Bodega Test No. 16 would most closely be approximated by something between 20 and 100% vent carry-over. All these calculations assume a constant vent carry-over; actually, one would expect the carry-over to change with time: being highest initially when the energy of the issuing stream of the primary vessel is at its highest and, consequently, probably most finely atomized and then decreasing as the blowdown event proceeds. Conceivably an expression for carry-over, perhaps, in terms of the degree of superheat of the liquid as it issues from the primary vessel or some other criteria, could be developed and utilized in the program.

5.2.3 Effect of Blowdown Rates

Although not studied systematically during the development of the program, the effect of blowdown rate can be deduced from consideration of Figure 5.1. The blowdown rate vary directly

* The relative quantities of escaping air and steam is based on the relative weights of each present in the dry well at a particular time.

effects the maximum pressure and the history of the transient pressure in the dry well. More rapid blowdowns will cause higher peak pressures and cause the peak to occur at a shorter time after the event. It is evident that right after the liquid water is expelled from the primary vessel the pressure in the dry well starts to drop. This strong dependence of dry well pressure on the rate of blowdown dictates the need for a good blowdown model.

5.2.4 Other Effects

Dry Well Size. The peak pressure that occurs in a dry well is quite dependent upon the size of the system. However, as one decreases the dry well size the effect on the pressure transient is more pronounced during the time of vent clearing. During this period of time the entire energy put into the dry well is retained there; consequently, the size of the system is of primary importance. For a very small dry well, such as in a marine reactor installation, relatively high pressures are developed initially but then dropped immediately after the vents have been cleared of water.

Figure 5.6 gives the calculated results of a typical proposed CSE run (characterized as Case 3). One run was made using identical parametric conditions except the dry well volume was taken as one tenth the normal dry well volume for the CSE system. The resulting dry well maximum pressure was 101 psi, and this was achieved during the period before the vents had been cleared. This curve is included in Figure 5.6.

Vent Submergence. Increased vent submergence results in a very slight increase in the length of time for vent clearance and, consequently, would increase the pressure buildup during that period. For very small dry wells this might have a very pronounced

FIGURE 5.6. Calculated Temperature and Pressure Transients for Typical CSE Blowdown with Large and Small Dry Well

effect. The vent clearing times calculated by the present model correspond rather closely to those indicated by the test data.

The equivalent downcomer submergence, ie the mass of water that must be accelerated, includes water not actually in the downcomer. The calculations of this quantity require certain assumptions that are difficult to substantiate. Also the present model uses a slightly different equation for vent clearing time than that used by Moody; the rate of change of momentum term includes the effect of the rate of change of total mass in the system as well as the velocity of the system. However, this change does not seem to effect the results noticeably.

Vent Downcomer Flow. The present model for vent downcomer flow is based on an ideal gas equation of state. Use of this equation in the present situation where some liquid is probably entrained in the vent flow may be subject to question. This liquid is present as the result of explosive decompression of the liquid and vapor as it goes into the dry well and may cause a departure from the ideal gas state. However, no alternative to this mode of calculations was deemed feasible at this time.

Vent Downcomer K Factor. The vent downcomer K factor is determined by the overall flow resistance between the dry well and wet well. It includes both friction and geometric resistance factors. As Moody points out in a discussion of both the Humboldt Bay and Bodega Bay Reactors, geometric factors are far more important than frictional factors. The latter may contribute as little as 5% to the total resistance to flow.

Moody further points out that the flow rate is not particularly sensitive to the K factor. For example, under critical flow conditions and a dry well pressure of 100 psia, a 100% increase in K would decrease the flow rate by approximately 25%. The effect would be even less pronounced at higher K values and at lower than critical flow.

Possible Heat Transfer Effects. The present model has no provision for condensation on the walls of the dry well. This effect could easily be incorporated in the program in the same fashion as discussed in Section 4.4.1. The result would be to decrease the peak pressures experienced during the blowdown but the magnitude itself is not known at this time. The GE proprietary code did not include this effect. They were primarily interested in peak pressures and "worst cases".

Thus, if the rupture of the primary vessel were to be prefaced by a slow leak which purged the dry well of air, the walls of the dry well might conceivably be up to near saturation temperature of the interior and provide no surface condensation. For the purpose of presenting some data on possible hoop stresses in the region where the dry well and wet well join calculation of the temperatures in the wall of the dry well would be of interest. This has not been done to date.

5.3 Wet Well Model

5.3.1 Comparison of Theoretical and Measured Pressures

Again the only test data to which the theoretical model has been compared are those obtained in the PG&E tests for the Humboldt Bay and Bodega Bay Reactors. The model agrees rather well with the test data as seen in Figures 5.2 and 5.6. The model does not predict the initial pressure oscillation observed in the test data. Moody discusses these briefly and indicates a surging of the water in the pool and momentary compression of wet well air was probably the cause of the oscillations.

The model closely approximates the delay time before wet well pressure rises. This effect is due to the time required for expulsion of the water from the vent.

The present model closely follows the calculated value of pressure determined by the GE model. Both models calculate a pressure that rises faster than measurements and level out slightly higher than the measured peak wet well pressure. The model assumes the uniform temperatures in the wet well although it is quite possible that gradients exist under test conditions.

5.3.2 Various Effects

Vent Carry-Over. Higher carry-over of liquid water from the dry well results in a decreased rate of pressure build up in the wet well since the carry-over decreases the rate of energy transfer from the dry well to the wet well. However, the final pressure in the wet well is slightly higher with increased vent carry-over because the total quantity of energy transferred is greater in this case.

Condensation. The model assumes complete condensation of vapor in the wet well water. The PG&E tests as well as earlier tests by Sargent and Lundy⁽³⁰⁾ indicated that complete condensation does occur when the conditions are equivalent to those used in the tests. In fact, the Sargent and Lundy tests indicated complete condensation occurred even when the discharge was above the surface of the water but within 3 ft and pointing directly at it. Conceivably, vent flow rates could be sufficiently high that incomplete condensation could occur, but under these conditions the wet well pressure would be very much higher than were observed in any of the PG&E tests.

5.4 Dry Containment Model

5.4.1 Theoretical Pressures and Temperatures

The model was not compared with any test data for the case of blowdown into a dry containment vessel. Figure 5.7 gives the results of several calculations of pressure temperature transients

FIGURE 5.7. Calculated Pressure and Temperature Transients During Blowdown into Dry Containment System

in a dry containment system. Some thought had been given to comparing the results of the model with pressure-temperature transients calculated by the proprietary Stone and Webster Program.⁽²⁴⁾ However, this was not done.

5.4.2 Effect of Condensation

The temperature and pressure transients, for the relatively short period of time as shown in Figure 5.7, show only the effect of condensation and the addition nuclear decay heat at a constant rate. The cooling effect of spray suppression and air recirculation within the dry containment system is not included as the assumption was made that the safety features would not come into play during the period of time shown on the graphs.

Decay heat addition is assumed to be a constant value during this period of time as discussed in Section 4.4.3. Condensation effects were taken into account through the use of a series of differential difference equations to account for the temperature behavior in the wall and through the use of a constant heat transfer coefficient to describe the condensation or film coefficient of the wall surface. A variable heat transfer coefficient could be included in the program as discussed in Section 4.4.2.

The assumed value of the condensation coefficient in early runs was rather high in the range of 500 to 1000. Later consideration of the heat transfer situation led to the conclusion that a value of approximately 50 would be more consistent with the situation. The effect on the pressure and temperature transients is shown in Figure 5.7.

It is probable that the actual condensation coefficients in the test system will differ from point to point and with the orientation of the surface on which condensation is occurring. Air

velocities caused by convection currents may have a marked effect on the local values of the condensation coefficients. One of the very interesting test areas that will be covered in the CSE program is the calculation of the condensation coefficients at various points in the system using the method of Powell and Price, briefly discussed in Section 5.1.3.

The present model allows for either an adiabatic outer wall of the containment vessel or provision for heat lost to the environs. Over the 30 sec interval covered in the calculation, temperature of the outer surface of the wall did not increase above its initial value, consequently the effect of an insulated containment vessel versus an uninsulated vessel was not a factor.

5.4.3 Suppression Effects--Long Term Treatment

The present program has elements that allow for the inclusion of the effect of spray suppression, air recirculation, and fission product decay heat upon the transients of the system. However, because of the rather long computational time involved in such calculations, use of the program to convert the model to a true analog simulation is recommended. Under this arrangement the time constant of the system can be changed conveniently to allow for both the detailed investigation of the short range effects as was done with the computer program and the response of the system to long term effects such as the interaction of decay heat and heat loss of the system.

In adapting the MIDAS program to an electronic analog simulation it is recommended that the detail of the blowdown model be eliminated; and that in its place the addition of energy and mass into the overall containment system be handled by means of simple exponential terms, ie terms which stipulate that

energy and mass are added to the system over a very short span of time with an arbitrary time constant. The time constants could be altered then to result in the same peak temperature and pressure as calculated in the more detailed MIDAS program.

The advantage of this approach is simplicity of setup on the electronic analog program and the ease with which the time constant of the system can be changed to allow investigation of the long term pressure and temperature transients. Decreasing the energy release time constants increases the peak temperature and pressure; as the time constant is allowed to go to zero, the peak pressure and temperature approach the noncondensation value as determined by the MIDAS program calculation. With the assumption of instantaneous energy release, condensation will subsequently effect the temperature and pressure transients but the values will be somewhat in error because the initial peak values will be too high.

The setup of the analog simulation system is very straightforward once the MIDAS program has been operated, since all scaling difficulties have been defined by the limiting values of each component listed on the output of the program.

The elements of the program which account for the effects of spray suppression and air cooling by recirculation have been developed only crudely. The cooling due to spray suppression is probably adequately described by the assumption of instantaneous equilibrium between the water droplets and the atmosphere if a fine spray is used. However, for some types of "deluge" nozzles, the droplet size may be sufficiently large that this assumption would be subject to question.

In the case of air recirculation, no sophisticated model of this subsystem was developed. However, as the design of the

subsystem is worked out and one is actually constructed, modeling should very easily be accomplished and conveniently included in the electronic analog simulation setup.

6.0 ACKNOWLEDGMENTS

The author wishes to thank R. D. Benham and personnel associated with the Containment Systems Experiment program, particularly E. R. Irish, C. E. Linderoth, G. J. Rogers, and N. Vieli for their help and encouragement during the course of this work.

The work of F. J. Moody should also be acknowledged as influencing the development of the pressure suppression model. In particular, the vent clearing times and flow rates were calculated by methods outlined by him and the model for critical two phase flow through a break used in calculating blowdown rates was developed by him.

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APPENDIX A

BLOWDOWN MODELS

A.1 SIMPLE MODEL

The desired information from the blowdown model is primarily the mass velocity of flow from the break and, secondarily, the pressure and temperature transients within the vessel. Figure 4.4 is a schematic representation of the two zone model but serves to illustrate the situation assumed in the simple model if one pictures Zone "a" comprising the whole vessel.

If the pressure and stagnation enthalpy of the fluid in the vessel at any instant in time are known, the rate of flow from the nozzle (break) could be calculated by any one of several models. For example, Equation 3.2-1 is Nahavandi's⁽³¹⁾ empirical equation based on Fauske's model. Equations 4.2-1 through 4.2-4 are the equations used in this work and are based on Esleeck's equation for subcooled blowdown (Equation 4.2-1) and Moody's model for two phase critical flow. For the simple blowdown program, only the equivalent of Equations 4.2-3 and 4.2-4 are used. The stagnation enthalpy is assumed to be that of saturated liquid water until all is expelled.

The final fluid is steam of varying quality determined by the isentropic expansion. These relationships are introduced through the use of function generators based on values obtained from Figure A.1, a graphical representation of Moody's results. Equation 4.2-4, representing critical flow during the isentropic expansion of the final steam remaining in the vessel after all liquid water has been expelled, should express a dependence on the specific entropy of the system (in effect a dependence on the system pressure at the instance this isentropic expansion commenced). However, over the range of pressures at which the

transition is likely to occur (1100-700 psia) this dependence may be neglected (as was done in Equation 4.2-4) without introducing an error greater than about 7%.

FIGURE A.1. Maximum Steam-Water Flow Rate and Local Stagnation Properties

The equations to be solved then are for $W_w \geq V_1/v_v$ (liquid water remaining)

$$\frac{dW_w}{dt} = -GA_B \approx A_B (4550 + 323p) \quad \begin{array}{l} \text{(actually introduced} \\ \text{by a function genera-} \\ \text{tion)} \end{array} \quad (\text{A.1-1})$$

$$\frac{dp}{dt} = \frac{1}{[W_w F(1) + V_1 F(2)]} \frac{dW_w}{dt} \quad (A.1-2)$$

and for $W_w < V_1 v_v$

$$\frac{dW_w}{dt} = GA_B \approx A_B (2.06p + 100) \quad (A.1-3)$$

$$\frac{dp}{dt} = \frac{1}{\frac{W_w v_L}{V_1} [W_w F(1) + V_1 F(2)]} \frac{dW_w}{dt} \quad (A.1-4)$$

where $F(1)$ and $F(2)$ are given by Equations 4.1-5 and 4.1-6. The functions were evaluated from thermodynamic data for water and steam and involved graphical differentiation to obtain the derivative relationships involved. Table A.1-1 lists representative values.

TABLE A.1-1. Calculated Values of the Functions $F(1)$ and $F(2)$ Equations 4.1-5 and 4.1-6

<u>P</u> <u>psia</u>	<u>F(1)</u>	<u>F(2)</u>
1800	0.00115	0.0163
1400	0.00178	0.0253
1000	0.00349	0.0407
800	0.00508	0.0534
400	0.0184	0.1115
200	0.0575	0.2351
100	0.161	0.501
60	0.350	0.859

These functions can be represented by the empirical equations

$$F(1) = 708 p^{1.77} \quad (A.1-5)$$

$$F(2) = 76.5/p^{1.09} \quad p < 1100 \quad (A.1-6)$$

$$= 3160/p^{1.62} \quad p > 1100$$

Table A.1-2 lists other approximations for specific thermodynamic function used in various program developed in this study.

TABLE A.1-2. Approximations for Thermodynamic Functions

Quantity	Formula	Units	Range of Validity (p, psia)
v_v	$1/0.002275 p$	ft^3/lb	0-1200
v_ℓ	$0.0177 + 3.9 \times 10^{-6} p$	ft^3/lb	200-1800
h_v	$18.33 \ln p_s + 1102$	Btu/lb	0-200
h_ℓ	$68.2 \ln p_s - 13$	Btu/lb	0-200
Suggested by Reference (6) but not used in present program			
h_v	$1.227 - p/30$	Btu/lb	500-1400
h_ℓ	$377 + p/6$	Btu/lb	500-1350
$\frac{1000}{h_{\ell v}}$	$\frac{0.020p + 49.6}{45}$	lb/Btu	500-1500

The four equations were solved by means of the MIDAS (Modified Integral Digital Analog Simulator) Program which provides a method for obtaining digital solutions for systems of ordinary differential equations.⁽³⁹⁾ This program makes the digital computer operate much like an analog computer. As a result, the programming techniques for both the analog and MIDAS systems are similar.

A problem is prepared for MIDAS by drawing a block diagram showing how various mathematical operations should be interconnected to simulate the mathematical model. The outputs of any or all of these operations can be printed out for a detailed picture of the system. The various operations used in the CSE program include:

Integrate

Sum

Negative

$$B = -A$$

Multiply

$$C = A \times B$$

Potentiometer

$$C = A \times B$$

(B = constant)

Divide

$$C = A/B$$

Square Root

$$B = +\sqrt{A}$$

Exponential

$$B = e^A$$

Natural Log

$$B = \ln A$$

Input Relay

$$D = A \quad C \geq 0$$

$$D = B \quad C < 0$$

The block diagram for the simple blowdown model is included in Figure B.2, the diagram for the program which calculates temperature gradients in the vessel wall. The notation for the various constants (constant for multiple runs) and parameters (variable for multiple runs) was changed as this model was inserted into various larger programs.

Table A.1-3 lists the two notations used. A complete tabulation of the program is included at the end of this section. The constants to be used are listed first, followed by the circuit hookup instructions, then the readout statements and finally the values of the function generators.

TABLE A.1-3. Constants Notation-Simple Blowdown Model

Constant	Notation in Particular Program	
	Suppression	Dry Containment and Temperature Gradient
A_B/W_w°	C1	BREAK
$V_1 \times 0.002275/W_w$ (NOWAT $\times p = V_1/v_v W_w^\circ$)	C2	NOWAT
3.9×10^{-6} (used in empirical calculation of v_ℓ)	C3	SLOPE
V_1	C4	VOL
$1/V_1$	C5	INVOL
Term in Equation 4.2-4	C6	2.06
Area of rupture, A_B	C10	ABREAK
0.0177 (used in empirical calculation of v_ℓ)	C51	0.0177
Term in Equation 4.2-4	C52	100
	C53	1
<u>Initial Values</u>		
I1	p° : saturation pressure	
I2	$(W_w/W_w^\circ)^\circ$: ratio of water present to the initial inventory	

OPTIONS		C.BLOWDOWN MODEL	CASE 2 AND 3 - BODEGA TESTS NOS 16 AND 30
000	CON	TR,2.06,SLOPE,.0177,100,1	
001	PAR	12,STOP,ZERO	
002	PAR	11,BREAK,NOMAT,VOL,INVOL,PSTOP	
003	PAR	HAR,2.668,.668,2,1.8,120	
004	IC	121,122,123,124,125,126	
030	NEG1	11	
040	CG1	11	
050	CG2	11	
060	CG3	11	
070	P2	NEG1,NOMAT	
080	S3	12,P2	
090	M2	CG2,12	
100	P4	CG3,VOL	
110	S2	P4,M2	
120	P3	11,SLOPE	
130	S1	P3,.0177	
140	M3	S1,12	
150	P5	M3,INVOL	
155	IR2	1,P5,S3	
160	M4	IR2,S2	
170	P6	11,2.06	
180	S4	P6,100	
190	IR1	CG1,S4,S3	
200	P1	IR1,BREAK	
210	NEG2	P1	
220	I2	NEG2	
2201	CG4	11	
2202	NEG20	I20	
2203	P28	NEG20,2.668	
2204	P29	CG4,.668	
2205	P30	I21,2	
2206	S20	P28,P29,P30	
2207	P20	S20,1.8	
2208	I20	P20	
2209	NEG21	I21	
2210	P31	NEG21,2	
2211	S21	P31,I20,I22	
2212	P21	S21,1.8	
2213	I21	P21	
2214	NEG22	I22	
2215	P32	NEG22,2	
2216	S22	P32,I21,I23	
2217	P22	S22,1.8	
2218	I22	P22	
2219	NEG23	I23	
2220	P33	NEG23,2	
2221	S23	P33,I22,I24	

LINE	ITEM	QTY	UNIT	PRICE	TOTAL
2222	P23 S23,1.8				
2223	I23 P23				
2224	NEG24 I24				
2225	P34 NEG24,I2				
2226	S24 P34,I23,I25				
2227	P24 S24,I1.8				
2228	I24 P24				
2229	NEG25 I25				
2230	P35 NEG25,I2				
2231	S25 P35,I24,I26				
2232	P25 S25,I1.8				
2233	I25 P25				
2234	NEG26 I26				
2237	S26 NEG26,I25				
2238	P26 S26,I1.8				
2239	I26 P26				
2240	S7 CG4,NEG20				
2241	NEG4 S7				
2242	P7 NEG4,MAR				
2243	D2 I2,I1				
2244	M5 D2,P7				
2245	S6 NEG2,M5				
230	D1 S6,M4				
240	I1 D1				
241	NEG3 I1				
242	S5 NEG3,PSTOP.				
HRR TIME,WEIGHT,PRESS,DW/DI,HEAT,TSURF					
HDR	J1,I2,J3,I4,I5				
R0	TR,I1,I2,I1,NEG2,M5,I20				
R0	IR,ZERO,I21,I22,I23,I24,I25,				
FIN	S5,STOP				
END					
2,000E-01	2,060E 00	3,900E-06	1,770E-02	1,000E 02	1,000E 00
1,000E 00	1,750E 03	0.			
1,800E 03	2,246E-05	5,620E-05	2,470E-02	4,050E 01	1,800E 03
2,565E-01	5,080E 00	3,080E 00	2,000E 00	8,450E-02	6,220E 02
6,220E 02	6,220E 02	6,220E 02	6,220E 02	6,220E 02	6,220E 02
9,000E 00					
2,000E 02	3,800E 03	4,000E 02	5,500E 03	6,000E 02	6,300E 03
8,000E 02	7,120E 03	1,000E 03	7,900E 03	1,200E 03	8,580E 03
1,400E 03	9,150E 03	1,600E 03	9,680E 03	1,800E 03	1,020E 04
1,400E 01					
3,000E 01	3,500E-01	1,000E 02	1,610E-01	2,000E 02	5,750E-02
3,000E 02	3,000E-02	4,000E 02	1,830E-02	3,000E 02	1,200E-02
6,000E 02	8,500E-03	7,000E 02	6,400E-03	8,000E 02	5,000E-03
1,000E 03	3,450E-03	1,200E 03	2,300E-03	1,400E 03	1,750E-03
1,600E 03	1,400E-03	1,800E 03	1,160E-03		
1,400E 01					
6,000E 01	8,590E-01	1,000E 02	5,010E-01	2,000E 02	2,350E-01
3,000E 02	1,500E-01	4,000E 02	1,120E-01	5,000E 02	8,700E-02
6,000E 02	7,200E-02	7,000E 02	6,150E-02	8,000E 02	5,350E-02

1.0000E 03	4.0500E-02	1.2000E 03	3.1800E-02	1.4000E 03	2.5400E-02
1.6000E 03	2.0400E-02	1.8000E 03	1.6300E-02		
1.3000E 01					
5.0000E 01	2.8100E 02	7.5000E 01	3.0760E 02	1.0000E 02	3.2780E 02
1.5000E 02	3.5840E 02	2.0000E 02	3.8180E 02	3.0000E 02	4.1730E 02
4.0000E 02	4.4460E 02	6.0000E 02	4.8620E 02	8.0000E 02	5.1820E 02
1.0000E 03	5.4460E 02	1.2000E 03	5.6720E 02	1.5000E 03	5.9620E 02
2.0000E 03	6.3580E 02				

A.2 TWO ZONE BLOWDOWN MODEL

The two zone model, illustrated in Figure 4.4, is described in Section 4.1.1. The introduction of the second zone concept, plus consideration of the existence of noncondensibles, result in the following system of equations to be solved:

$$\frac{dW_w}{dt} = A_B \left\{ \begin{array}{ll} G^{(1)} = A_B (590) (p_T - \circ)^{1/2} & (a) \\ G^{(2)} = A_B (13,350 + 6.31)p_T - 22h_0 & (b) \\ G^{(3)} = A_B (4550 + 323)p_T & (c) \\ G^{(4)} = A_B (2.06 p_T + 100) & (d) \end{array} \right\} \quad (A.2-1)$$

$$\left. \begin{aligned} \frac{dT}{dt} &= \frac{\frac{v_\ell h_{\ell v}}{v_{\ell v}} + \left[\frac{W_A R}{M_A J} \right] \left[\frac{V_a T}{(V_a - v_\ell W_{w,a})^2} \right]}{\frac{dp}{dT} [W_{w,a} F(3) + V_a F(4)] + W_A \left[C_p - \frac{V_a R}{J M_A (V_a - v_\ell W_{w,a})} \right]} \frac{dW_w}{dt} \quad (a) \\ \frac{dT}{dt} &= \frac{\frac{v_\ell h_{\ell v}}{v_{\ell v}}}{\frac{dp}{dT} [W_w F(3) + V F(4)] + W_a \left[C_p - \frac{V R}{J M_A (V - v_\ell W_{w,a})} \right]} \frac{dW_w}{dt} \quad (b) \\ \frac{dT}{dt} &= \frac{\frac{V}{W_w} \left(\frac{v_\ell h_{\ell v}}{v_{\ell v}} \right) \frac{dW_w}{dt} + C_p (T - 32) \frac{dW_A}{dt}}{\frac{dp}{dT} [W_w F(3) + V F(4)] + W_a \left(C_p - \frac{R}{J M_A} \right)} \quad (c) \end{aligned} \right\} *$$

$$\frac{dp}{dt} = \left(\frac{dp}{dt} \right) \left(\frac{dT}{dt} \right) \quad (A.2-3)$$

$$\frac{dW_A}{dt} = \left(\frac{W_A}{W_A + W_w} \right) \left(\frac{dW_w}{dt} \right) \quad (A.2-4)$$

* Equation (A.2-2)

where p_T and V_a are calculated by the equations:

$$p_T = p + \frac{W_A RT/144}{M_A (V_a - v_\ell W_{w,a})} \quad (A.2-5)$$

$$V_a = V_a^\circ + v_\ell (W_w - W_{w,a}) \quad (A.2-6)$$

The various alternates, (a), (b), (c), etc., of Equations (A.2-1) and (A.2-2) are used over the following regions:

For $V_a \leq V_1$ Equation (A.2.1): Compare (a) and (b) and use the larger.

Equation (A.2-2): (a)

Equation (A.2-4): $dW_A/dt = 0$

For $\begin{cases} V_a = V \\ \frac{VW_w}{v_v} > 1 \end{cases}$ Equation (A.2-1): (c)

Equation (A.2-4): $dW_A/dt = 0$

For $\begin{cases} V_a = V \\ \frac{VW_w}{v_v} = 1 \end{cases}$ Equation (A.2-1): (d)

Equation (A.2-2): (c)

Equation (A.2-4): as written

Core Pressure Drop. The description of equations to calculate core pressure drop are discussed in Section 4.3. In addition to Equations (A.2-1) through (A.2.6), the equations required to calculate the local pressure in the vicinity of the break and hence the core pressure drop include:

$$\frac{d^2 W_u}{dt^2} = \frac{-144 g_c (p_u - p_L)}{\frac{L_T}{A_v} \left(1 - \frac{At}{A_v} + \frac{Lv}{L_t}\right)} + \frac{144 g_c R_c \theta}{\frac{L_T}{A_t} \left(1 - \frac{At}{A_L} + \frac{Lv}{L_t}\right)} \left(\frac{dW_u}{dt}\right)^2 - \frac{W_u + W_L}{L_T \left(1 - \frac{At}{A_v} + \frac{Lv}{L_t}\right)}$$

for $p_L = P$ saturated, ie $W_L v_{\ell}(\text{saturation}) \leq V_L$

(A.2-7)

$$\frac{dp_L}{dt} = \frac{1}{W_L F(1) + V_L F(2)} \left[\frac{dW_L}{dt} + \frac{v_{\ell v}}{h_{\ell v} v_{\ell}} \frac{dW_u}{dt} (h_{\ell u} - h_{\ell L}) \right] \quad (A.2-8a)$$

for $p_L > p$ (saturated), ie $W_L v_{\ell}(\text{saturation}) \geq V_L$

$$\frac{dp_L}{dt} = \frac{1}{V_L} \frac{dp_L}{d\rho_{\ell}} \frac{dW_L}{dt} \quad (A.2-8b)$$

$$\frac{dW_L}{dt} = -GA_B - \frac{dW_u}{dt} \quad (A.2-9)$$

From charts of the thermodynamic properties of water, one can calculate values of $F(3)$, $F(4)$, dp/dt , etc. at saturation.

TABLE A.2-1. Calculated Values of the Various Functions Appearing in the Equations of the Two Zone Model

P (saturation)	$\frac{v_{\ell} h_{\ell v}}{v_{\ell v}}$	$\frac{dp}{dT}$	$F(3)$	$F(4)$
1800	64.0	12.94	0.0738	1.046
1400	47.6	10.65	0.085	1.22
1000	32.65	8.14	0.114	1.33
600	19.60	5.56		
200	6.83	2.36	0.392	1.60
100	3.57	1.363	0.575	1.79
50	1.878	0.785	1.21	1.93
20	0.805	0.371	2.58	2.07
10	0.424	0.209	4.68	2.24
5	0.223	0.117	8.50	2.52
1	0.0501	0.0308	34.15	2.80
0.5	0.0264	0.016	56.75	3.05

From these values one can develop the following empirical relationships.

$$\frac{h_{\ell v}}{v_{\ell v}} = 3.426 p^{0.883}, \quad (A.2-10a)$$

$$\frac{v_{\ell}^h v_{\ell v}}{v_{\ell v}} = \frac{p}{30} \quad , \quad (A.2-10b)$$

$$\frac{dp}{dt} F(3) = 0.94/p^{0.011} \quad , \quad (A.2-11)$$

$$\frac{dp}{dt} F(4) = 0.398p^{0.161} \quad \text{for } p > 1100, \quad (A.2-12a)$$

$$= 0.0962p^{0.691} \quad \text{for } p < 1100, \quad (A.2-12b)$$

$$\frac{dp}{dt} = 0.0373p^{0.781} \quad \text{for } p > 100, \quad (A.2-13)$$

and for pressure above saturation

$$\frac{dp}{d\rho_{\ell}} = 2570 - 0.8p_{\text{saturation}} \quad (A.2-14)$$

In the program as shown, Equation (A.2-12a and b) is calculated by means of a linear extrapolation function (designated G-1) which lists the natural log of the equation as a function of $\ln p$ and feeds into an element which calculates "e" raised to the input power.

Figure A.2 is the block diagram for the two zone blowdown model with core pressure drop. Table A.2-2 lists the input constants and parameters. Complete tabulations of the program, both with and without core pressure drop are included at the end of the section.

FIGURE A.2. Block Diagram of the Two Zone Blowdown Model with Core Pressure Drop

TABLE A.2-2. Input Parameters and Constants—Two Zone Blowdown Model

Constants	Value	Significance
C1	0.0685	R/J
C2	3.43	Constant in Equation (A.2-10a)
C3	6.31	Constant in Equation (4.2-2)
C4	-590.	Constant in Equation (4.2-1)
C6	-3.23	Constant in Equation (4.2-3)
C7	2.06	Constant in Equation (4.2-4)
C8	0.002275	Constant in Equation $v_v = 0.002275p$
C9		Not applicable
C18	30.	Constant in Equation (A.2-10b)
C19	0.25	$C_p(\text{Air})$
C20		Not applicable
C21		Not applicable
C22		Not applicable
C24		Not applicable
C25	0.0373	Constant in Equation (A.2-13)
C26		Not applicable
C27	0.37	R/M_A
C31	0.781	Constant in Equation (A.2-13)
C32	0.691	Constant in Equation (A.2-12b)
C33	0.16	Constant in Equation (A.2-12a)
C34	0.882	Constant in Equation (A.2-10a)
C37	0.0177	Constant in Equation (A.2-14)
C38	3.9×10^{-6}	Constant in Equation (A.2-14)
C39	30.	Constant in Equation (A.2-10b)
C104	100.	Constant in Equation (4.2-4)
C102	1.	
C106	-1.	
C112	32.	Reference temperature
C113	460.	$^{\circ}\text{R} = 0^{\circ}\text{F}$
C123	-4550.	Constant in Equation (4.2-3)
C127	2570.	Constant in Equation (A.2-15)

TABLE A.2-2. (Cont.)

Parameters	Typical Value	Significance
C17	-0.0243	$-v_l^\circ$: Initial specific volume of water
C22	0.95	$0.88p_s^{0.011}$ (average)--assumed a constant
C35	60.	V_L : Volume of vessel beneath the core ft^3
C36	0.0167	$1/V_L$
C40	775.	$(144g_c)/\{(L_T/A_V)[1 + (L_V/L_T) - (A_T/A_V)]\}$
C41	0.0465	$(R_c \theta)(144g_c)/\{(L_T/A_T)[1 - (L_V/L_T) - (A_T/A_V)]\}$
C103	-1800.	$-P_s^\circ$: Initial saturation pressure, psia
C107	-950.	$13,500 - 22h_\ell^\circ$; term in Equation (4.2-2)
C108	0.0872	A_B : Area of break, ft^2
C109	5950.	W_{wa} : Weight of water in Zone "a"
C115	5950.	W_w° : Weight of water in entire vessel
C116	648.	h_ℓ° : Initial specific enthalpy
C117	150.	V_a° : Volume in Zone "a"
C126	-3260.	$-144g_c(p_L - p_u)^\circ/\{(L_T/A_V)[1 + (L_V/L_T) - (A_T/A_V)]\}$
<u>Initial Values</u>		
I1		V_a° : Volume of Zone "a"
I2		p° : Saturation pressure above core
I3		T° : Temperature above core
I4		W_w° : Water in system
I5		W_A° : Air in system

TABLE A.2-2. (Cont.)

<u>Initial Values</u>	<u>Significance</u>
I6	p_L° : Total pressure below core
I7	$(dW_u/dt)^\circ$: Rate of removal of water from above core
I8	W_L° : Water below core

The expression for the resistance R_c through the core is inserted in the program as C41. At present no provision exists to utilize a variable two phase multiplier θ which accounts for increased resistance due to two phase flow through the core. This could be inserted as an empirical function of the quality and saturation pressure. The resistance term R_c has the units of $(lb_f/ft^2)/(lb_m/sec)^2$ and is obtained from the relation,

$$R_c = \frac{4fL_T}{2g_c \rho_l D_T A_T^2}$$

The value of $4fL_T/D_T$ should include not only frictional resistance within the tube but also entrance and exit effects which may actually predominate.


```

OPTION3
C MODIFIED RLOWDOWN ANALYSIS (TWO ZONE MODEL WITHOUT CORE PRESSURE DROP)
001 CON C1,C2,C3,C4,C6,C7
002 CON C8,C9,C18,C19,C20,C21
003 CON C23,C24,C25,C26,C27,C31
004 CON C32,C33,C34,C102,C104,C106
005 CON C112,C113,C123,ZERO,MININT
006 PAR C22,C103,C107,C108,C109,C17
007 PAR C110,C115,C116,YR,STOP,C117
008 IC 11,14,15,16,17
009 M24 C17,C110
010 P32 16,C17
011 NEG1 C113
012 P33 NE1,C17
013 P33 C117,C32,P33
014 S1 M24,S22
015 S19 15,C113
016 L1 S1,C109,ZERO
017 L2 S22,C109,ZERO
018 D8 S19,L1
019 P27 D8,C27
020 M19 P27,17
021 S25 M19,14
022 S4 S25,C103
023 SOR1 S4
024 NEG7 S25
025 P3 NEG7,C3
026 M8 P3,C108
027 S1M2 C108,C107
028 S5 M8,M9
029 NEG3 S5
030 P4 SOR1,C4
031 M7 P4,C108
032 S6 M7,NEG3
033 T81 S5,M7,S6
034 P4 S25,C4
035 P7 S25,C7
036 S8 P6,C123
037 NEG5 S22
038 S11 NEG5,C109
039 M16 S8,C108
040 IR2 IR1,M86,S11
041 S9 P7,C104
042 S10 16,17
043 D2 16,S10
044 NEG4 D2

```

0331M15	S9,C108
034 M5	M15,NEG4
035 NEG6	I4
036 P8	NEG6,C8
0361M14	P8,C109
037 S12	M14,I6
038 IR3	IR2,M5,S12
039 I6	IR3
040 S13	D2,C106
041 M6	S13,S9
042 IR9	ZERO,M6,S12
043 I7	IR9
063 LN1	I4
064 P31	LN1,C31
065 G1	LN1
067 P34	LN1,C34
068 E1	P31
069 E2	G1
071 E4	P34
073 D1	C102,L1
074 M1	D1,L2
074IM23	I7,M1
075 P1	M23,C1
0751NEG9	P1
076 M2	P1,D1
078 M3	S19,M2
079 P26	M3,C17
0791NEG12	P26
080 IR5	NEG12,ZERO
085 D5	I4,C18
086 S2	D5,IR5
087 M4	S2,IR3
088 S20	I5,C112
089 M12	S20,IM9
090 P19	M12,C19
091 D7	C109,I6
092 P2	D7,C2
093 M10	P2,IR3
094 M11	M10,E4
095 S21	M11,P19
096 IR6	M4,S21,S12
097 M13	L2,E2
098 IR7	C110,I6,S14
0981M22	IR7,C22
0982P21	I7,C19
099 S26	P21,NEG9,M13,M22
108 D6	IR6,S26
109 M17	D6,E1
110 P25	M17,C25
111 I5	D6
112 I4	P25

123	CG1	I4			
124	CG2	I4			
125	IR10	C116,CG1,S11			
126	IR11	IR10,CG2,S12			
127	NEG11	IR3			
128	M18	IR11,NEG11			
	HDR	TIME,PRESS,WEIGHT,DW/DT,TEMP,ENGY			
	HDR	.DP/DIT,TPRESS,VOLA,REG1,REG2			
	HDR	.F1,F2,F3,F4,F5			
	RD	TR,ITI,I4,I5,IR3,I5,M18			
	RO	TR,ZERO,D25,S25,S22,S11,S12			
	RO	TR,ZERO,D5,IR5,M22,M13,NEG9			
	FIN	IT,STOP			
	END				
6,8500E-02	3,4300E 00	6,3100E 00	-5,9000E 02	3,2300E 00	2,0600E 00
2,2750E-03	-2,0000E 00	3,0000E 01	2,9000E-01	3,9300E-01	9,5000E-02
1,0330E 00	1,8000E-01	3,7300E-02	5,1400E 02	3,7000E-01	7,8100E-01
6,9100E-01	1,6000E-01	8,8200E-01	1,0000E 00	1,0000E 02	-1,0000E 00
-3,2000E 01	4,6000E 02	-4,5500E 03	0.	1,0000E-05	
9,4000E-01	-1,2000E 03	8,2000E 02	1,4300E-01	8,0000E 01	-2,2300E-02
6,4300E 02	2,4200E 03	5,7200E 02	1,0000E-01	6,0000E 00	4,0000E 01
4,0000E 01	1,2000E 03	5,8700E 02	2,4200E 03	3,6000E 00	
3,0000E 00					
4,6080E 00	8,3200E-01	7,0050E 00	2,5000E 00	7,8230E 00	2,6400E 00
8,0000E 00					
4,0000E 02	4,2400E 02	6,0000E 02	4,7200E 02	8,0000E 02	5,1000E 02
1,0000E 03	5,4250E 02	1,2000E 03	5,7150E 02	1,4000E 03	5,9850E 02
1,6000E 03	6,2400E 02	1,8000E 03	6,4850E 02		
7,0000E 00					
5,0000E 01	9,8000E 02	1,0000E 02	1,0300E 03	2,0000E 02	1,0800E 03
4,0000E 02	1,1300E 03	6,0000E 02	1,1600E 03	8,0000E 02	1,1800E 03
1,0000E 03	1,1900E 03				

C MODIFIED BLOWDOWN ANALYSIS WITH CORE PRESSURE DROP

001 CON	C1,C2,C3,C4,C6,C7
002 CON	C8,C9,C18,C19,C20,C21
003 CON	C23,C24,C25,C26,C27,C31
004 CON	C32,C33,C34,C102,C104,C106
005 CON	C112,C113,C123,ZERO,MININT
0051CON	C37,C38,C39,C127,C42
006 PAR	C22,C103,C107,C108,C109,C17
007 PAR	C110,C115,C116,TR,STUP,C117
0071PAR	C35,C36,C40,C41,C126
008 IC	I1,I4,I5,I6,I7
0081IC	I2,I3,I8
010 M24	C17,C110
0101P32	I6,C17
0102NEG1	C115
0103P33	NEG1,C17
0104S22	C117,P32,P33
011 S1	M24,S22
012 S19	I5,C113
0121L1	S1,C109,ZERO
0122L2	S22,C109,ZERO
0124D8	S19,L1
0125P27	D8,C27
0126M19	P27,I7
0127S25	M19,I4
013 S4	I2,C103
014 SOR1	S4
0141NEG7	I2
015 P3	NEG7,C3
016 M8	P3,C108
0161M9	C108,C107
017 S5	M8,M9
018 NEG3	S5
019 P4	SOR1,C4
0191M7	P4,C108
020 S6	M7,NEG3
021 IR1	S5,M7,S6
024 P6	S25,C6
025 P7	S25,C7
026 S8	P6,C123
027 NEG5	S22
028 S11	NEG5,C109
0281M16	S8,C108
029 IR2	IR1,M16,S11
030 S9	P7,C104
031 S10	I6,I7
032 D2	I6,S10
033 NEG4	D2
0331M15	S9,C108

034	M5	M15,NEG4
035	NEG6	I4
036	P8	NEG6,C8
0361	M14	P8,C109
037	S12	M14,I6
038	IR3	IR2,M5,S12
039	I6	IR3
040	S13	D2,C106
041	M6	S13,S9
042	IR9	ZERO,M6,S12
043	I7	IR9
0440	CG4	I2
0441	CG5	I2
0442	P35	CG5,C35
0443	M20	CG4,I8
0444	S27	M20,P35
0450	NEG12	I3
0451	S29	NEG12,IR3
0452	I8	S29
0453	M22	I3,I3
0454	M34	M22,C41
0455	NEG14	S25
0456	S33	I2,NEG14
0460	P40	S33,C40
0461	CG1	I4
0462	NEG13	C116
0463	S31	NEG13,CG1
0464	S32	C126,P40,M34
0465	I3	S32
0470	P39	NEG12,C39
0471	D10	P39,I2
0472	M23	D10,S31
0473	S30	S29,M23
0474	D9	S30,S27
0475	NEG16	I4
0476	P42	NEG16,C42
0477	S34	C127,P42
0478	P36	S34,C36
0479	M25	S29,P36
0480	P38	I2,C38
0481	M21	I8,P38
0482	P37	I8,C37
0483	NEG15	C35
0484	S28	P37,M21,NEG15
0485	IR12	M25,D9,S28
0486	I2	IR12
063	LN1	I4
064	P31	LN1,C31
065	G1	LN1
067	P34	LN1,C34
068	E1	P31

-3.200E-01	4.6000E-02	-4.550E-03	0.	1.0000E-05	
1.770E-02	3.9000E-06	3.0000E-01	2.5700E-03	9.0000E-01	
9.500E-01	-1.8000E-03	-9.5000E-02	8.7200E-02	1.5000E-02	-2.4300E-02
5.950E-03	5.9500E-03	6.4800E-02	10.0000E-04	1.0000E-01	1.5000E-02
6.000E-01	1.6670E-02	7.7500E-02	4.6500E-02	-3.2600E-03	
1.500E-02	1.8000E-03	6.2200E-02	5.9500E-03	9.4800E-00	
2.5042E-03	0.	2.4700E-03			
1.400E-01					
6.000E-01	8.5900E-01	1.0000E-02	5.0100E-01	2.0000E-02	2.3500E-01
3.000E-02	1.5000E-01	4.0000E-02	1.1200E-01	5.0000E-02	8.7000E-02
6.000E-02	7.2000E-02	7.0000E-02	6.1500E-02	8.0000E-02	5.3500E-02
1.000E-03	4.0500E-02	1.2000E-03	3.1800E-02	1.4000E-02	2.5400E-02
1.600E-03	2.0400E-02	1.8000E-03	1.6300E-02		
1.400E-01					
6.000E-01	3.5000E-01	1.0000E-02	1.6100E-01	2.0000E-02	5.7500E-02
3.000E-02	3.0000E-02	4.0000E-02	1.8300E-02	5.0000E-02	1.2000E-02
6.000E-02	8.5000E-03	7.0000E-02	6.4000E-03	9.0000E-02	5.0000E-03
1.000E-03	3.4500E-03	1.2000E-03	2.3000E-03	1.4000E-03	1.7500E-03
1.600E-03	1.4000E-03	1.8000E-03	1.1600E-03		
8.000E-00					
4.000E-02	4.2400E-02	6.0000E-02	4.7200E-02	8.0000E-02	5.1000E-02
1.000E-03	5.4250E-02	1.2000E-03	5.7150E-02	1.4000E-03	5.9850E-02
1.600E-03	6.2400E-02	1.8000E-03	6.4850E-02		
3.000E-00					
4.608E-00	8.3200E-01	7.0050E-00	2.5000E-00	7.8250E-00	2.6400E-00
7.000E-00					
5.000E-01	9.8000E-02	1.0000E-02	1.0300E-03	2.0000E-02	1.0800E-03
4.000E-02	1.1300E-03	6.0000E-02	1.1600E-03	8.0000E-02	1.1800E-03
1.000E-03	1.1900E-03				

APPENDIX B

TEMPERATURE GRADIENTS IN VESSEL WALLS

B.1 PROGRAM

The equations solved in the calculation of temperature gradients in the vessel wall are essentially the same as solved for the simple blowdown model except for the term

$$\left(\frac{v_{\ell v}}{h_{\ell v} v_{\ell}} \right) h_B A_S (T_S - T_{st}) ,$$

which is added to the differential dW_w/dt in equations (A.1-2) and (A.1-4) and relates the rate of pressure drop to the rate of water removal from the vessel as shown in Equation (5.1-1)

$$\frac{dp}{dt} = \frac{1}{W_w F(1) + V_1 F(2)} \left[\frac{dW_w}{dt} + \left(\frac{v_{\ell v}}{h_{\ell v} v_{\ell}} \right) h_B A_S (T_S - T_{st}) \right] \quad (5.1-1)$$

The evaluation of T_S , the surface temperature, is accomplished through a series of differential difference equations using equal increments of $Y = \sqrt{X}$ where X is the distance through the wall. Equations (4.4-5) through (4.4-7) are the resulting equations that are solved. As stated in Section A.2, the value of $v_{\ell v}/(h_{\ell v} v_{\ell})$ is approximated by $30/p$.

The area of heat transfer, A_S , is reduced in proportion to W_w/W_w^o in line with the reasoning that this ratio would approximate the relative liquid level, and that heat would be transferred only through that portion of the vessel surface covered by water.

B.2 HEAT TRANSFER COEFFICIENTS

As discussed in Section 4.4.2 the values of the heat transfer coefficients were calculated using empirical relationships developed from semitheoretical equations for nucleate boiling, critical heat flux, minimum flux for film boiling, and the temperature difference at which the minimum flux occurs. These are given as Equations (4.4-13) through (4.4-16). To obtain the empirical equations for nucleate boiling, transition boiling, and film boiling [Equations (4.4-17 through (4.4-20)], the equations were written in a form so as to group each fluid parameter together and indicate the power relationship between the flux and each parameter. For example Equation (4.4-14) may be written as:

$$q/A = [\rho_v]^{1/2} [\rho(\rho_L - \rho_v)]^{1/4} [\rho_\ell / (\rho_\ell + \rho_v)]^{1/2} [h_{fg}]$$

where the use of brackets may be interpreted as "proportional to".

The variation of each bracketed term with saturation pressure was next determined and led to the relative value of q/A as a function of p at saturation. A single value from the literature of q/A at a particular pressure was used to establish the absolute value. No expression was available for the transition region; but knowledge of the critical flux and ΔT , as well as the minimum film boiling flux and the corresponding ΔT , was used to fix two points on an assumed linear relation of q/A versus ΔT on a log log plot for the transition region. The relationship between the critical heat flux and pressure could not be represented by a simple power function, and the values of $F(p)$ in Equation (4.4-18) are as presented in Table B.2-1.

TABLE B.2-1. Values of the Empirical Factor $F(p)$ in Equation (4.4-18)

<u>P</u>	<u>F(p)</u>
1500	7.35×10^6
1000	1.59×10^7
500	5.49×10^7
200	1.06×10^9

The relationship between Equations (4.4-15) and (4.4-16) is such that they predict a heat transfer coefficient of 530 Btu(hr)(ft²)(°F) at the minimum film boiling flux independent of temperature difference and pressure. Hence it was assumed this was the value for film boiling under all conditions. Figure B.1 is a plot of the calculated heat flux relationships which led to Equations (4.4-17) through (4.4-20). Figure B.2 is the block diagram of the program. A complete tabulation of the program is included at the end of this section.

For the cases run to date, all boiling has been nucleate and the calculated temperature difference between wall surface and steam have been below that at the critical heat flux.

Table B.2-2 lists the various input constants and parameters used in the program in addition to those used in the simple blow-down program.

FIGURE B.1. Calculate Boiling Heat Flux As a Function of Pressure and Temperature Difference

FIGURE B.2. Block Diagram of Program to Calculate Temperature Gradients in Vessel Wall During Blowdown

TABLE B.2-2. Input Parameters and Constants—Temperature Gradient Program

Constant Notation	Value	Application
C11	0.56	Empirical constants in Equation (4.4-17)
C12	5.42	Empirical constants in Equation (4.4-17)
C13	0.64	Empirical Constants in Equation (4.4-19)
C14	5.08×10^{-3}	$30 A_s^\circ / 3600$
C15	1.695×10^{-3}	$2\alpha N^2 h / kL$ [Equation (4.4-5)]
C17	1.79	$(\alpha N^4 / 2L^2) [1/n(2n + 1) - 1/n(2n - 1)]$ in Equation (4.4-6)
C20	0.298	
C23	0.1192	
C26	0.0638	
C29	0.0397	$(\alpha N^4 / 2L^2) [1/n(2n - 1)]$ in Equation (4.4-6)
C18	2.384	
C21	0.477	
C24	0.2043	
C27	0.1135	$(\alpha N^4 / 2L^2) [1/n(2n + 1)]$ in Equation (4.4-6)
C30	0.0722	
C19	0.594	
C22	0.179	
C25	0.0851	$(\alpha N^4 / 2L^2) [1/N(2N + 1)]$ in Equation (4.4-7)
C28	0.0497	
C31	0.0325	
C32	0.0271	
C33	0.0271	
7.15		$2\alpha N^4 / L^2$ [Equation (4.4-5)]
62		Empirical constant in Equation (4.4-19)
A	10^7	Arbitrary upper limit
530		Film boiling heat transfer coefficient

TABLE B.2-2. (Cont.)

<u>Initial Condition</u>	<u>Value</u>	<u>Application</u>
I1		p: Primary vessel pressure
I2		W_w : Primary vessel water
I10		T_o : Surface temperature of vessel wall
I11		T_1 : Temperature at the midpoint slab 1; 0.071 in. from surface
I12		T_2 : Temperature at the midpoint slab 2; 0.284 in. from surface
I13		T_3 : Temperature at the midpoint slab 3; 0.639 in. from surface
I14		T_4 : Temperature at the midpoint slab 4; 1.136 in. from surface
I15		T_5 : Temperature at the midpoint slab 5; 1.775 in. from surface
I16		T_6 : Temperature at the midpoint slab 6; 2.55 in. from surface

```

OPTIONS
C ANALYSIS OF THERMAL STRESSES DURING BLOWDOWN
000 CON TR,2.06,SLOPE,.0177,100,1
10 CON C11,C12,C13,C14,C15,C17
20 CON C18,C19,C20,C21,C22,C23
30 CON C24,C25,C26,C27,C28,C29
40 CON C30,C31,C32,C33,ZERO,7.15
50 CON 62,A,530
60 PAR STOP,PSIOP,BREAK,NOWAT,VOL,INVOL
70 IC I1,I2,I10,I11,I12,I13
80 IC I14,I15,I16
90 NEG1 I1
100 CG1 I1
110 CG2 I1
120 CG3 I1
130 P2 NEG1,NOWAT
140 S3 I2,P2
150 M2 CG2,I2
160 P4 CG3,VOL
170 S2 P4,M2
180 P3 I1,SLOPE
190 S1 P3,.0177
200 M3 S1,I2
210 P5 M3,INVOL
220 IR2 I,P5,S3
230 M4 IR2,S2
240 P6 I1,2.06
250 S4 P6,100
260 IR1 CG1,S4,S3
270 P1 IR1,BREAK
280 NEG2 P1
290 I2 NEG2
300 LN1 I1
310 P11 LN1,C11
320 E1 P11
330 P12 E1,C12
340 CG4 I1
350 NEG7 I10
360 S7 CG4,NEG7
370 M8 S7,S7
380 M7 P12,M8
390 P13 LN1,C13
400 E2 P13
410 D3 62,E2
420 S9 D3,1
430 NEG5 S8
440 LN2 NEG4
450 M9 LN2,NEG5

```


460	E3	M9
470	CG12	I1
480	M10	CG12,E3
490	CG11	I1
510	M11	S7,M7
520	S9	CG11,M11
530	IR3	M7,M10,S9
540	L1	IR3,A,530
550	P15	L1,C15
560	M12	P15,CG4
570	S10	7,15,P15
580	M13	S10,NEG7
590	P16	I11,7,15
600	S11	P16,M12,M13
601	I10	S11
602	P17	I10,C17
603	NEG8	I11
604	P18	NEG8,C18
605	P19	I12,C19
606	S12	P17,P18,P19
607	I11	S12
608	P20	I11,C20
609	NEG9	I12
610	P21	NEG9,C21
611	P22	I13,C22
612	S13	P20,P21,P22
613	I12	S13
614	P23	I12,C23
615	NEG10	I13
616	P24	NEG10,C24
617	P25	I14,C25
618	S14	P23,P24,P25
619	I13	S14
620	P26	I13,C26
621	NEG11	I14
622	P27	NEG11,C27
623	P28	I15,C28
624	S15	P26,P27,P28
625	I14	S15
626	P29	I14,C29
627	NEG12	I15
628	P30	NEG12,C30
629	P31	I16,C31
630	S16	P29,P30,P31
631	I15	S16
632	P32	I15,C32
633	NEG13	I16
634	P33	NEG13,C33
635	S17	P32,P33
636	I16	S17
637	NEG4	S7

638	M6	NFG4,L1			
639	P14	M6,C14			
640	D2	T2,I1			
641	M5	D2,P14			
642	S6	NEG2,M5			
643	D1	S6,M4			
644	I1	D1			
645	NEG3	I1			
646	S5	NEG3,PSTOP			
	HDR	TJME,WEIGHT,PRESS,DW/DT,HEAT,TSURF			
	HDR	,T1,T2,T3,T4,T5			
	HDR	,DELTA,T,H,Q/A,REGEME			
	RO	TR,I,T,I2,I1,NEG2,M5,I10			
	RO	TR,ZERO,I11,I12,I13,I14,I15			
	RO	TR,ZERO,S7,L1,M6,S9			
	FIN	S5,STOP			
	END				
5.0000E-01	2.0600E 00	3.9000E-06	1.7700E-02	1.0000E 02	1.0000E 00
5.6000E-01	5.4200E 00	6.4000E-01	5.0800E-03	1.6950E-03	1.7900E 00
2.3840E 00	5.9400E-01	2.9800E-01	4.7700E-01	1.7900E-01	1.1920E-01
2.0430E-01	8.5100E-02	6.3800E-02	1.1350E-01	4.9700E-02	3.9700E-02
7.2200E-02	3.2500E-02	2.7100E-02	2.7100E-02	0.	7.1500E 00
6.2000E 01	1.0000E 07	5.3000E 02			
1.7500E 03	1.8000E 03	2.2460E-05	5.6200E-05	2.4700E-02	4.0500E 01
1.8000E 03	1.0000E 00	6.2200E 02	6.2200E 02	6.2200E 02	6.2200E 02
6.2200E 02	6.2200E 02	6.2200E 02			
9.0000E 00					
2.0000E 02	3.8000E 03	4.0000E 02	5.5000E 03	6.0000E 02	6.3000E 03
8.0000E 02	7.1200E 03	1.0000E 03	7.9000E 03	1.2000E 03	8.5500E 03
1.4000E 03	9.1500E 03	1.6000E 03	9.6800E 03	1.8000E 03	1.0200E 04
1.4000E 01					
6.0000E 01	3.5000E-01	1.0000E 02	1.6100E-01	2.0000E 02	5.7500E-02
3.0000E 02	3.0000E-02	4.0000E 02	1.8300E-02	5.0000E 02	1.2000E-02
6.0000E 02	8.5000E-03	7.0000E 02	6.4000E-03	8.0000E 02	5.0000E-03
1.0000E 03	3.4500E-03	1.2000E 03	2.3000E-03	1.4000E 03	1.7500E-03
1.6000E 03	1.4000E-03	1.8000E 03	1.1600E-03		
1.4000E 01					
6.0000E 01	8.5900E-01	1.0000E 02	5.0100E-01	2.0000E 02	2.3500E-01
3.0000E 02	1.5000E-01	4.0000E 02	1.1200E-01	5.0000E 02	8.7000E-02
6.0000E 02	7.2000E-02	7.0000E 02	6.1500E-02	8.0000E 02	5.3500E-02
1.0000E 03	4.0500E-02	1.2000E 03	3.1800E-02	1.4000E 03	2.5400E-02
1.6000E 03	2.0400E-02	1.8000E 03	1.6300E-02		
1.3000E 01					
5.0000E 01	2.8100E 02	7.5000E 01	3.0760E 02	1.0000E 02	3.2780E 02
1.5000E 02	3.5840E 02	2.0000E 02	3.8180E 02	3.0000E 02	4.1730E 02
4.0000E 02	4.4460E 02	6.0000E 02	4.8620E 02	8.0000E 02	5.1820E 02
1.0000E 03	5.4460E 02	1.2000E 03	5.6720E 02	1.9000E 03	5.9620E 02
2.0000E 03	6.3580E 02				
1.6000E 01					
1.0000E 02	3.0000E 10	1.5000E 02	4.0000E 09	2.0000E 02	1.0600E 09
2.5000E 02	4.0000E 08	3.0000E 02	2.1000E 08	3.5000E 02	1.3000E 08

4.000E 02	9.000E 07	5.000E 02	5.400E 07	6.000E 02	3.600E 07
7.000E 02	2.650E 07	8.000E 02	2.100E 07	9.000E 02	1.800E 07
1.000E 03	1.560E 07	1.200E 03	1.100E 07	1.500E 03	7.300E 06
1.800E 03	5.200E 06				
1.200E 01					
1.000E 02	1.000E 05	1.500E 02	1.150E 05	2.000E 02	1.280E 05
3.000E 02	1.400E 05	4.000E 02	1.550E 05	5.000E 02	1.600E 05
6.000E 02	1.650E 05	8.000E 02	1.680E 05	1.000E 03	1.700E 05
1.200E 03	1.670E 05	1.500E 03	1.530E 05	1.800E 03	1.370E 05

APPENDIX C

DRY CONTAINMENT PROGRAM

C.1 PROGRAM

The dry containment program primarily expresses the pressure and temperature in the containment vessel as a function of energy input and heat dissipation via conduction and cooling. The simple blowdown model currently provides the mass and energy input from the primary vessel. Thus the equations solved by the program are Equations A.1-1 through A.1-4 for pressure-temperature transients in the primary vessel, plus the energy balance equations for the region, Equation 4.1-19, and the appropriate mass balance equation. For blowdown directly into the containment vessel, Equation 4.1-19 can be simplified to

$$\frac{dT}{dt} = \frac{\frac{dW_{w2}}{dt} [F(5)] - \frac{dW_{w1}}{dt} h_B + Sh_s + Q_D - Q_R - Q_C}{\frac{dp}{dT} [W_{w2} F(3) + V_2 F(4)] + W_A [C_P - \frac{R}{JM_A}]}$$

$$\frac{dW_{w2}}{dt} = \frac{dW_{w1}}{dt} + S$$

where the expression for $F(5)$ is given by Equation 4.1-20. These equations assume a constant mass of air within the system but could be easily modified to include the addition or removal of air.

The addition and removal of heat, ie fission product decay heat, recirculation cooling and conduction from the walls are discussed in Section 4.4.1.

Figure C-1 is the block diagram for the dry containment program; a complete tabulation of the program is included at the end of this section. The value of $F(3)$ and $F(4)$ could be

FIGURE C.1. Block Diagram of Program to Calculate Pressure-Temperature Transients in Dry Containment During a Loss of Coolant Accident

calculated from the empirical expressions given in Section A.2, but as the program stands they are obtained from function generators (designated CG-4 and CG-5) along with F(5) (designated CG-6). These values are printed in the data section of the program as indicated.

Table C.1-1 lists the input constants and parameters for the program.

TABLE C.1-1. Input Parameters and Constants - Dry Containment Program

<u>Constants</u>	<u>Value</u>	<u>Significance</u>
VT2	30,000.0	V_2 total free volume of dry containment region
QDIN	1,200.0	Constant initial decay heat (decay heat at 50 sec)
C2	533.0	$W_A(C_p - R/MJ)$ (Assumed constant)
C3	40.0	Enthalpy of spray water
C4	460.0	Conversion of temperature in °F to °R
50	50.0	Time before fp heat term begins to decay
C50	1.0×10^{-4}	Limiting constant
<u>Parameters</u>	<u>Typical Values</u>	
1.8	1.8	$\alpha(N/L)^2$ in Equations 4.4-1 and 4.4-2
2.668	2.668	$2(1 + h_L/kN)$ in Equations 4.4-1 and 4.4-2
0.668	0.668	$2 h_L/kN$ in Equations 4.4-1 and 4.4-2
0.225	0.225	Constant in Equation 4.4-21
A1	1.0	Constant in Equation 4.4-3 if heat is lost from outer surface. If wall is perfectly insulating, the values are 1.0, 1.0, and 0 as shown.
A2	1.0	
A3	0	
C1	-26.67	[Heat removed via recirculation]/[rate of spray addition]
Spray	30.0	Rate of spray addition, lb/sec
HA	1,528.0	$hA/3600$: h = condensation coefficient; A = condensing area
TS	10.0	Time before spray is activated, sec
C12	0.0275	$W_A R/M_A V_2$, psi/°R
C40	2900.0	$0.1135 \times$ hypothetical reactor power, Btu/sec

TABLE C.1-1. (Cont.)

<u>Initial Condition</u>	<u>Significance</u>
I1	p_1 : Primary vessel pressure
I2	W_{w1} : Primary vessel water
I11	T_2 : Temperature in containment vessel
I12	W_{w2} : Water in containment vessel
I13	p_2 : Saturation pressure in containment vessel
I14	$P_{T,2}$: Total pressure in containment vessel
I20	T_0 : Temperature midpoint of Slab 1
I22	T_2 : Temperature midpoint of Slab 2
I23	T_3 : Temperature midpoint of Slab 3
I24	T_4 : Temperature midpoint of Slab 4
I25	T_5 : Temperature midpoint of Slab 5
I26	T_6 : Temperature outer surface of vessel wall

The program, as written, assumes that engineered safeguards such as spray suppression and recirculation cooling are actuated at the same time. Separate actuation times could be included with no difficulty. More complex models for heat removal via a recirculation system could also be easily handled when design of the system on which to base the model has been completed.

C.2 LONG TERM TRANSIENTS

For long duration effects, the MIDAS program is very time consuming. When adapted to an analogue computer the system of equations should be simplified by replacing the expressions for $-dW_w/dt$ and $(-dW_w/dt)h_B$ by equations such as the following:

$$-\frac{dW_{w,1}h_B}{dt} = \frac{W_{w,1}h_B}{\tau_e}$$

$$-\frac{dW_{w,1}}{dt} = \frac{W_{w,1}}{\tau_m}$$

which can be easily set up on the computer to depict events that happen over a very short portion of the total time of interest. These expressions simplify the release of energy and mass from the primary vessel by assuming an exponential or first order rate of release of mass and energy. The values of the time constants τ_e and τ_m would be arbitrarily varied until the initial temperature and pressure peak, which occurs during the blowdown corresponds with the calculated peak obtained from the MIDAS Program.

Other additional heat input terms, such as reactor transient shutdown power and sensible heat of the core, could be added in similar fashion if warranted.

Possibly for the long term studies using the analog computer, the simulation of heat transfer through the walls should be simulated with fewer difference equations. While accuracy of the simulation is increased by using more segments, stability of the operational amplifiers may dictate that fewer segments be used.

OPTION3

C PRESSURE-TEMPERATURE TRANSIENTS DURING LOC ACCIDENT H=50

001	CON	VT2,QDIN,C2,C3,C4,ZERO
003	CON	100,,0177,1,50,SLOPE,C50
004	IC	I2,I14,I15,I16,I20
005	IC	I21,I22,I23,I24,I25,I26
006	IC	I1,I11,I12,I13
007	PAR	1.8,2.668,.668,2.,.225,2.06
008	PAR	A1,A2,A3,C12,C40
010	PAR	TR,STOP,C1,SPRAY,WA,TS
020	PAR	BREAK,NOWAT,VOL,INVOL,ABREAK
030	NEG1	I1
040	CG1	I1
050	CG2	I1
060	CG3	I1
070	P2	NEG1,NOWAT
080	S3	I2,P2
090	M2	CG2,I2
100	P4	CG3,VOL
110	S2	P4,M2
120	P3	I1,SLOPE
130	S1	P3,,0177
140	M3	S1,I2
150	P5	M3,INVOL
155	IR2	1,P5,S3
160	M4	IR2,S2
170	P6	I1,2.06
180	S4	P6,100
190	IR1	CG1,S4,S3
191	IR3	IR1,ZERO,I2
200	P1	IR3,BREAK
210	NEG2	P1
220	I2	NEG2
230	D1	NEG2,M4
240	I1	D1
250	CG9	I13
260	CG4	I13
270	CG5	I13
280	CG6	I13
290	M11	I12,CG4
300	P11	CG5,VT2
310	S13	M11,P11
320	M12	CG9,S13
330	S14	M12,C2
350	NEG15	TS
360	S18	NEG15,IT
370	IR12	SPRAY,ZERO,S18
380	NEG16	50
390	S19	NEG16,IT

400	S30	IT,C50
410	LN1	S30
420	P13	LN1,,225
430	E1	P13
440	P40	E1,C40
450	IR14	P40,QDIN,S19
460	NEG20	I20
470	S27	NEG20,I11
480	P27	S27,HA
485	NEG27	P27
490	P28	NEG20,2,668
500	P29	I11,,668
510	P30	I21,2
520	S20	P28,P29,P30
530	P20	S20,1,8
540	I20	P20
550	NEG21	I21
560	P31	NEG21,2
570	S21	P31,I20,I22
580	P21	S21,1,8
590	I21	P21
600	NEG22	I22
610	P32	NEG22,2
620	S22	P32,I21,I23
630	P22	S22,1,8
640	I22	P22
650	NEG23	I23
660	P33	NEG23,2
670	S23	P33,I22,I24
680	P23	S23,1,8
690	I23	P23
700	NEG24	I24
710	P34	NEG24,2
720	S24	P34,I23,I25
730	P24	S24,1,8
740	I24	P24
750	NEG25	I25
760	P35	NEG25,2
770	S25	P35,I24,I26
780	P25	S25,1,8
790	I25	P25
800	NEG26	I26
801	P36	I25,A1
802	P37	NEG26,A2
810	S26	P36,P37,A3
820	P26	S26,1,8
830	I26	P26
840	P10	IR3,ABREAK
850	S11	P10,IR12
860	I12	S11
870	CG7	I1

880	CG8	I1	CG7,CG8,S3	1,200E-03	5,330E-02	4,000E-01	4,600E-02	0,
890	IR11	CG7,CG8,S3	IR11	1,770E-02	1,000E-00	5,000E-01	3,900E-06	10,000E-05
900	NEG12	IR11	IR11	1,470E-01	1,000E-05	1,200E-03	7,500E-01	7,500E-01
910	S18	CG6,NEG12	CG6,NEG12	7,500E-01	7,500E-01	7,500E-01	7,500E-01	7,500E-01
920	NEG13	S11	S11	7,500E-01	1,900E-01	4,300E-01	2,250E-01	2,060E-00
930	M13	S15,NEG13	S15,NEG13	2,0334E-00	3,340E-02	2,000E-00	2,900E-03	1,000E-01
940	S16	NEG12,C3,C1	NEG12,C3,C1	1,000E-00	0	3,000E-01	7,640E-01	1,000E-01
950	M14	S16,IR12	S16,IR12	5,620E-05	-2,667E-01	4,050E-01	1,370E-01	
960	S17	M13,M14,IR14,NEG27	M13,M14,IR14,NEG27		2,470E-02			
970	D11	S17,S14	S17,S14					
980	I11	D11	D11					
990	M13	D11,CG9	D11,CG9					
991	I13	M15	M15					
992	S31	I11,C4	I11,C4					
993	P12	S31,C12	S31,C12					
994	S12	P12,I13	P12,I13					
995	HDR	TIME,PT,PS,TEMP,TS,OC	TIME,PT,PS,TEMP,TS,OC					
996	HDR	MIN,DH1/DT,MH1,PI,MH2	MIN,DH1/DT,MH1,PI,MH2					
997	RO	TR,I7,S12,I13,I11,I20,P27	TR,I7,S12,I13,I11,I20,P27					
998	RD	TR,ZERO,S17,NEG2,12,I1,I12	TR,ZERO,S17,NEG2,12,I1,I12					
999	FIN	I7,STOP	I7,STOP					
	END							
3,000E-04				1,200E-03	5,330E-02	4,000E-01	4,600E-02	0,
1,000E-02				1,770E-02	1,000E-00	5,000E-01	3,900E-06	10,000E-05
1,000E-00				1,470E-01	1,000E-05	1,200E-03	7,500E-01	7,500E-01
7,500E-01				7,500E-01	7,500E-01	7,500E-01	7,500E-01	7,500E-01
1,800E-03				7,500E-01	1,900E-01	4,300E-01	2,250E-01	2,060E-00
1,800E-00				2,0334E-00	3,340E-02	2,000E-00	2,900E-03	1,000E-01
1,000E-00				1,000E-00	0	3,000E-01	7,640E-01	1,000E-01
5,000E-01				3,000E-01	-2,667E-01	4,050E-01	1,370E-01	
2,246E-05				5,620E-05	2,470E-02			
9,000E-00								
2,000E-02				3,800E-03	4,000E-02	5,500E-03	6,000E-02	6,300E-03
8,000E-02				7,120E-03	1,000E-03	7,900E-03	1,200E-03	8,550E-03
1,400E-03				9,150E-03	1,600E-03	9,680E-03	1,800E-03	1,020E-04
1,400E-01								
6,000E-01				3,500E-01	1,000E-02	1,610E-01	2,000E-02	5,750E-02
3,000E-02				3,000E-02	4,000E-02	1,830E-02	1,000E-02	1,200E-02
6,000E-02				8,500E-03	7,000E-02	6,400E-03	8,000E-02	5,000E-03
1,000E-03				3,450E-03	1,200E-03	2,300E-03	1,400E-03	1,750E-03
1,600E-03				1,400E-03	1,800E-03	1,160E-03		
1,400E-01								
6,000E-01				8,590E-01	1,000E-02	5,010E-01	2,000E-02	2,350E-01
3,000E-02				1,500E-01	4,000E-02	1,120E-01	5,000E-02	8,700E-02
6,000E-02				7,200E-02	7,000E-02	6,150E-02	6,000E-02	5,350E-02
1,000E-03				4,050E-02	1,200E-03	3,180E-02	1,400E-03	2,540E-02
1,600E-03				2,040E-02	1,600E-03	1,630E-02		
1,700E-01								
0,								
3,000E-00				0,	1,000E-00	3,080E-02	2,000E-00	5,350E-02
1,000E-01				2,090E-01	4,000E-00	9,600E-02	5,000E-00	1,170E-01
					1,500E-01	2,900E-01	2,000E-01	3,710E-01

2,500E 01	4,480E-01	3,000E 01	5,220E-01	3,500E 01	5,960E-01
4,000E 01	6,590E-01	4,500E 01	7,200E-01	5,000E 01	7,850E-01
5,500E 01	8,460E-01	6,000E 01	9,130E-01		
2,500E 01					
1,000E-01	2,300E 02	2,000E-01	1,280E 02	3,000E-01	9,100E-03
5,000E-01	5,900E 01	7,000E-01	4,500E 01	1,000E 00	3,300E 01
2,000E 00	1,850E 01	3,000E 00	1,310E 01	4,000E 00	1,020E 01
5,000E 00	9,500E 00	6,000E 00	7,200E 00	7,000E 00	6,300E 00
8,000E 00	5,600E 00	9,000E 00	5,100E 00	1,000E 01	4,700E 00
1,500E 01	3,300E 01	2,000E 01	2,610E 00	2,500E 01	2,100E 00
3,000E 01	1,860E 00	3,500E 01	1,630E 00	4,000E 01	1,460E 00
4,500E 01	1,330E 00	5,000E 01	1,220E 00	5,500E 01	1,120E 00
6,000E 01	1,040E 00				
1,800E 01					
2,000E-01	3,500E 00	5,000E-01	3,050E 00	1,000E 00	2,940E 00
2,000E 00	2,760E 00	3,000E 00	2,650E 00	4,000E 00	2,500E 00
5,000E 00	2,470E 00	6,000E 00	2,400E 00	7,000E 00	2,350E 00
8,000E 00	2,300E 00	9,000E 00	2,260E 00	1,000E 01	2,230E 00
1,500E 01	2,125E 00	2,000E 01	2,065E 00	2,500E 01	2,020E 00
3,000E 01	1,991E 00	4,000E 01	1,960E 00	6,000E 01	1,920E 00
2,800E 01					
2,000E-01	2,200E 01	5,000E-01	4,800E 01	1,000E 00	6,900E 01
2,000E 00	9,390E 01	3,000E 00	1,092E 02	4,000E 00	1,260E 02
5,000E 00	1,290E 02	6,000E 00	1,370E 02	7,000E 00	1,450E 02
8,000E 00	1,505E 02	9,000E 00	1,558E 02	1,000E 01	1,618E 02
1,500E 01	1,801E 02	2,000E 01	1,954E 02	2,500E 01	2,074E 02
3,000E 01	2,177E 02	3,500E 01	2,260E 02	4,000E 01	2,345E 02
4,500E 01	2,417E 02	5,000E 01	2,482E 02	5,500E 01	2,543E 02
6,000E 01	2,590E 02	8,000E 01	2,790E 02	1,000E 02	2,950E 02
1,500E 02	3,252E 02	2,000E 02	3,482E 02	3,000E 02	3,840E 02
4,000E 02	4,110E 02				
8,000E 00					
4,000E 02	4,240E 02	6,000E 02	4,720E 02	8,000E 02	5,100E 02
1,000E 03	5,425E 02	1,200E 03	5,715E 02	1,400E 03	5,985E 02
1,600E 03	6,240E 02	1,800E 03	6,485E 02		
7,000E 00					
5,000E 01	9,800E 02	1,000E 02	1,030E 03	2,000E 02	1,080E 03
4,000E 02	1,130E 03	6,000E 02	1,160E 03	8,000E 02	1,180E 03
1,000E 03	1,190E 03				

APPENDIX D
PRESSURE SUPPRESSION CONTAINMENT

D.1 PROGRAM

The pressure suppression program calculates the pressure and temperature transients in the three regions—primary vessel, dry well, and wet well—during the blowdown event. As in the dry containment program, the simple blowdown model currently provides the mass and energy input from the primary vessel. The equations for these quantities are Equations A.1-1 through A.1-4. The equations from which temperature and pressure are calculated in the dry well are Equations 4.1-19 through 4.1-22 and in the wet well, Equation 4.1-23.

The values of $F(3)$, $F(4)$, and dp/dT were obtained as outlined in Appendix A.2. In this program these functions were represented by the following empirical relationships

$$\frac{dp}{dT} F(3) = 0.734 p^{0.054} \quad D.1-1$$

$$\frac{dp}{dT} F(4) = 0.1 p^{0.729} \quad D.1-2$$

$$\frac{dp}{dT} = 0.0311 p^{0.824}; p < 100 \quad D.1-3$$

$$\frac{dp}{dT} = 0.373 p^{0.781}; p > 100 \quad D.1-4$$

$$v_v = 334/p^{0.94} \quad D.1-5$$

Figure D.1 is the block diagram for the pressure suppression program; a complete tabulation of the program is included at the end of this section. Table D.1-1 lists the input constants and parameters.

TABLE D.1-1. Input Parameters of Constants for Pressure Suppression Model

Constant	Value	Significance
C-201	78.8	$144 \text{ g}_c/\rho_\ell$ (in vents)
C-207	0.017	v_ℓ - specific volume of water
C-208	0.94	Equation D.1-5
C-209	0.054	Equation D.1-1
C-210	0.729	Equation D.1-2
C-211	0.824	Equation D.1-3
C-212	18.33	Equation D.2-1
C-213	68.2	Equation D.2-2
C-214	0.25	C_p (air)
C-215	0.733	Constant in Equation D.1-1
C-216	0.1715	$C_p = R/JM_A$
C-218	0.0311	Constant in Equation D.1-3
C-251	1.5	Add vent length due to water inertia, ft
C-253	-32.2	g : acceleration due to gravity
C-254	-32.0	32 °F: Reference temperature
C-255	460.0	Convert °F to °R
C-259	334.0	Constant in Equation D.1-5
C-261	1102.0	Constant in Equation D.2-1
C-262	-13.0	Constant in Equation D.2-2
C-263	-1.0×10^{15}	Limiting value on calculation of L
Parameter	Typical Value	Significance
202	3.36×10^{-4}	$R/(M_A V_2)$, $\text{lb}_f/\text{in.}^2 \cdot \text{lb}_m \cdot ^\circ\text{R}$
203	0	Carry-Over (Dimensionless)
204	3.14	A_2 : Vent Area, ft^2
205	5.34	$[1.18 - 0.267 \ln 4 fL/D]$ $\times [12\sqrt{p_e g_c}]$ from Equations D.1-8 and 4.5-2
217	109.5	$0.1 V_2(0.1 \text{ from Equation D.1-2, ft}^3)$

TABLE D.1-1 (Cont.)

<u>Parameter</u>	<u>Typical Value</u>	<u>Significance</u>
252	1272.0	$g(L + \Delta L)^{\circ} + (144 g_c p_3^{\circ})/\rho_l,$ (ft/sec) ²
258	3.84	$1.9 \times 0.31 (4fL/D)$ (dimensionless)
260	1100.0	
302	5.52×10^{-4}	$R/(M_A V_3), lb_f/(in.^2 \cdot lb_m \cdot ^{\circ}R$
317	66.7	$0.1 V_3$ (V_3 = Wet well volume) ft ³
351	1.73	0.433 LD, psi

Initial Conditions

I1	p_1 : Primary vessel pressure
I2	$W_{w,1}$: Primary vessel water
201	T_2 : Dry Well Temperature
202	p_2 : Dry Well Saturation Pressure
203	$W_{w,2}$: Dry Well Water

Explanations for constants and parameters numbered C1-C100 are given in Table A.1-2.

OPTION3

C PRESSURE-TEMPERATURE TRANSIENTS DURING LOC ACCIDENT WITH PRESSURE SUPP

C CASE 1 BODEGA TEST 16, CASE 2 HUMBOLT TEST 17, CASE 3 CSE ~~RUNSAFE-HOOD~~

001	CON	ZERO,C3,C6,C51,C52,C53
002	CON	C201,C207,C208,C209,C210,C211
003	CON	C212,C213,C214,C215,C216,C218
004	CON	C251,C253,C254,C255,C259,C261
005	CON	C262,C263
006	PAR	C1,C2,C4,C5,C10,STOP
007	PAR	C202,C203,C204,C205,C217,C252
008	PAR	C258,C260,C302,C317,C351,TR
009	PAR	MININT
010	IC	I1,I2,I201,I202,I203,I204
011	IC	I205,I206,I301,I302,I303,I304

1001	NEG1	I1
1002	CG1	I1
1003	CG2	I1
1004	CG3	I1
1005	P2	NEG1,C2
1006	S3	I2,P2
1007	M2	CG2,I2
1008	P4	CG3,C4
1009	S2	P4,M2
1010	P3	I1,C3
1011	S1	P3,C51
1012	M3	S1,I2
1013	P5	M3,C5
1014	IR2	C53,P5,S3
1015	M4	IR2,S2
1016	P6	I1,C6
1017	S4	P6,C52
1018	IR1	CG1,S4,S3
1019	IR3	IR1,ZERO,I2
1020	P1	IR3,C1
1021	NEG2	P1
1022	I2	NEG2
1023	D1	NEG2,M4
1024	I1	D1
1025	P10	NEG2,C10

C CALCULATION OF WETWELL PRESSURE AND TEMPERATURE

3001	S306	I301,C255
3002	M303	I304,S306
3003	P302	M303,C302
3004	S304	I302,P302
3005	S305	S304,C351

C CALCULATION OF DRYWELL PRESSURE AND TEMPERATURE

2001	S206	I201,C255
2002	M203	I204,S206
2003	P202	M203,C202

2004	S204	P202,I202
C VENT CLEARING TIME		
2005	I206	I205
2005	IR210	I206,ZERO,I206
2006	S201	IR210,C251
2007	M201	I205,I205
2008	NEG201	M201
2009	P201	S204,C201
2010	NEG202	P201
2011	S202	NEG202,C252,NEG201
2012	D201	S202,S201
2013	S203	D201,C253
2013	NEG230	S203
2013	S230	C263,NEG230
2013	IR230	C263,S203,S230
2014	I205	IR230
2015	LN201	I202
2016	P208	LN201,C208
2017	P209	LN201,C209
2018	P210	LN201,C210
2019	P211	LN201,C211
2020	E201	P208
2021	E202	P209
2022	E203	P210
2023	E204	P211
2024	D204	C259,E201
2025	NEG205	I203
2026	M207	NEG205,D204
2027	P206	NEG205,C207
2028	M208	D204,I204
2029	S210	M207,C260
2029	P203	S210,C203
2030	S211	P206,C260
2031	S213	M208,S211,P203
2032	S212	P203,S211
2033	D206	S204,S305
2034	NEG204	C258
2035	S215	NEG204,D206
2036	IR202	C258,D206,S215
2037	CG210	IR202
2038	P205	CG210,C205
2039	M212	S211,D204
2040	P207	P203,C207
2041	S216	P207,M212
2042	M211	S213,S204
2043	D207	M211,S216
2044	SQR202	D207
2045	M213	SQR202,P205
2046	IR203	ZERO,M213,I206
2047	P204	IR203,C204
2048	D205	P204,S213

2049	M209	D205,M208
2050	M210	S212,D205
2051	S214	M210,P10
2052	NEG206	S214
2053	I203	NEG206
2054	NEG211	M209
2055	I204	NEG211
2056	P212	LN201,C212
2057	P213	LN201,C213
2058	S217	C261,P212
2059	S218	C262,P213
2060	M214	S217,S211
2061	M215	S218,P203
2062	P214	M208,C214
2063	S205	I201,C254
2064	M216	S205,P214
2066	S219	M216,M214,M215
2067	M217	S219,D205
2068	NEG210	M217
2069	CG7	I1
2070	CG8	I2
2071	IR204	CG7,CG8,S3
2072	NEG208	P10
2073	M218	IR204,NEG208
2074	CG206	I202
2075	M219	CG206,S214
2080	S220	NEG210,M218,M219
2081	M220	I203,E202
2082	P215	M220,C215
2083	P216	I204,C216
2084	P217	E203,C217
2085	S221	P215,P216,P217
2086	D208	S220,S221
2087	I201	D208
2088	P218	E204,C218
2089	M221	D208,P218
2090	I202	M221
C WETWELL CONDITIONS		
3006	LN301	I302
3007	P309	LN301,C209
3008	P310	LN301,C210
3009	P311	LN301,C211
3010	E302	P309
3011	E303	P310
3012	E304	P311
3013	CG306	I302
3014	NEG301	M210
3015	M319	CG306,NEG301
3016	S320	M217,M319
3017	M320	I303,E302
3018	P315	M320,C215

[illegible]

2,500E 00	3,830E-01	3,718E 00	3,930E-01		
8,000E 00					
4,000E 02	4,240E 02	6,000E 02	4,720E 02	8,000E 02	5,100E 02
1,000E 03	5,425E 02	1,200E 03	5,715E 02	1,400E 03	5,985E 02
1,600E 03	6,240E 02	1,800E 03	6,485E 02		
7,000E 00					
5,000E 01	9,800E 02	1,000E 02	1,030E 03	2,000E 02	1,080E 03
4,000E 02	1,130E 03	6,000E 02	1,160E 03	8,000E 02	1,180E 03
1,000E 03	1,190E 03				
2,800E 01					
2,000E-01	2,200E 01	5,000E-01	4,800E 01	1,000E 00	6,960E 01
2,000E 00	9,390E 01	3,000E 00	1,092E 02	4,000E 00	1,206E 02
5,000E 00	1,299E 02	6,000E 00	1,377E 02	7,000E 00	1,445E 02
8,000E 00	1,505E 02	9,000E 00	1,558E 02	1,000E 01	1,618E 02
1,500E 01	1,801E 02	2,000E 01	1,954E 02	2,500E 01	2,074E 02
3,000E 01	2,177E 02	3,500E 01	2,266E 02	4,000E 01	2,345E 02
4,500E 01	2,417E 02	5,000E 01	2,482E 02	5,500E 01	2,543E 02
6,000E 01	2,599E 02	8,000E 01	2,790E 02	1,000E 02	2,950E 02
1,500E 02	3,252E 02	2,000E 02	3,482E 02		3,840E 02
4,000E 02	4,110E 02				
2,800E 01					
2,000E-01	2,200E 01	5,000E-01	4,800E 01	1,000E 00	6,960E 01
2,000E 00	9,390E 01	3,000E 00	1,092E 02	4,000E 00	1,206E 02
5,000E 00	1,299E 02	6,000E 00	1,377E 02	7,000E 00	1,445E 02
8,000E 00	1,505E 02	9,000E 00	1,558E 02	1,000E 01	1,618E 02
1,500E 01	1,801E 02	2,000E 01	1,954E 02	2,500E 01	2,074E 02
3,000E 01	2,177E 02	3,500E 01	2,266E 02	4,000E 01	2,345E 02
4,500E 01	2,417E 02	5,000E 01	2,482E 02	5,500E 01	2,543E 02
6,000E 01	2,599E 02	8,000E 01	2,790E 02	1,000E 02	2,950E 02
1,500E 02	3,252E 02	2,000E 02	3,482E 02		3,840E 02
4,000E 02	4,110E 02				

D.2 VENT FLOW

The pressure and temperature conditions in the dry well are the result of a dynamic balance between incoming and outgoing streams and consequently is a sensitive function of the adequacy of simulation of both these flow rates. As discussed in Section 4.5.3 the vent flow is based on the flow of an ideal gas as calculated using the thermodynamics of compressible fluids. Figures D.2 and D.3 are plots of Equation 4.5-2 and a combination of Equations 4.4-3 and 4.4-4 reproduced from Reference (22). Values of ψ at various values of p_2/p_3 and $4fL/D$ were obtained from the these graphs and are listed in Table D.2-1.

FIGURE D.2-1. Calculated Values of ψ As a Function of p_2/p_3 and $4fL/D$

p_3/p_2	$4fL/D$							
	1.0		2.0		5.0		10.0	
	M_1	ψ	M_1	ψ	M_1	ψ	M_1	ψ
0.99	0.085	0.090	0.070	0.075	0.052	0.057	0.046	0.045
0.95	0.196	0.191	0.160	0.158	0.115	0.118	0.084	0.089
0.90	0.278	0.266	0.222	0.216	0.158	0.155q	0.120	0.122
0.85	0.330	0.314	0.266	0.256	0.192	0.188	0.142	0.142
0.80	0.380	0.351	0.304	0.289	0.218	0.211	0.160	0.158
0.70	0.448	0.396	0.360	0.336	0.255	0.245	0.192	0.188
0.60	0.490	0.425	0.398	0.362	0.282	0.271	0.212	0.205
Critical	0.520	0.443	0.430	0.384	0.32	0.306	0.245	0.237
$(p_2/p_3)_{\text{Critical}}$	0.41		0.38		0.27		0.20	

Plotting these values as a family of curves of ψ versus p_3/p_2 with $4fL/D$ as a parameter, one concludes that ψ may be expressed as the following empirical function of the two variables,

$$\psi = [F(p_3/p_2)](1.18 - 0.267 \ln 4fL/D) \quad (\text{D.2-1})$$

up to a limiting value of $(p_3/p_2)^{\text{critical}}$, which may also be expressed as a function (empirical) of $4fL/D$

FIGURE D.2. Entrance Mach Number As a Function of Resistance and Pressure Ratio

FIGURE D.3. Mass Flux As a Function of Entrance Mach Number

$$(p_3/p_2)^{\text{critical}} = 1.9 + 0.31 (4fL/D) \quad (\text{D.2-2})$$

The values of $F(p_3/p_2)$ are identical to those at $4fL/D = 2.0$ given in Table D.2-1 above. These relationships allow ψ to be obtained from which G_2 , the vent flow, may be calculated by Equation 4.5-2. In this equation, v_{3m} is the mean specific volume for both liquid and vapor and is given by the equation,

$$v_m = \frac{\beta v_v + \gamma v_\ell}{\alpha + \beta + \gamma} \quad .$$

The relative flow rates of air and steam-water mixture are calculated from the ratio of each to the sum of the two. Thus,

$$\begin{aligned} \frac{dW_{w,2}}{dt} &= G_2 A_2 \left(\frac{W_{w,m,2}}{W_{A,2} + W_{w,m,2}} \right) \quad (\text{D.2-3}) \\ &= \frac{dW_{w,1}}{dt} - \left(\frac{\beta + \gamma}{\alpha + \beta + \gamma} \right) A_2 G_2 \end{aligned}$$

and

$$\begin{aligned} \frac{dW_{A,2}}{dt} &= -G_2 A_2 \left(\frac{W_{A,2}}{W_{A,2} + W_{w,m,2}} \right) \quad (\text{D.2-4}) \\ &= - \frac{\alpha}{\alpha + \beta + \gamma} A_2 G_2 \quad . \end{aligned}$$

The significance of the parameters α , β , and γ and the effect of liquid carry over is discussed in Section 4.1.2

APPENDIX E
NOMENCLATURE

A_B	= Cross section area of break or nozzle
A_g	= Cross section area of vent
A_T	= Cross section area of flow in core tubes
A_V	= Cross section area of primary vessel
A_S	= Surface for heat transfer
C_L	= Heat capacity of liquid water
C_p	= Heat capacity of air
C_{pm}	= Heat capacity of air-steam mixture
C_{sf}	= Empirical constant dependent on surface conditions during boiling
CO	= Carry-over = $(1 - X_m) W_{w,m} / (1 - X) W_w$
D_v	= Diffusivity of steam in noncondensable gas film
E_T	= Total internal energy of the system
f	= Friction factor for flow resistance of the core
G	= Mass velocity
$G^{(1)}$	= Mass velocity of subcooled water through a break
$G^{(2)}$	= Mass velocity superheated water through a break
$G^{(3)}$	= Mass velocity saturated water through a break
$G^{(4)}$	= Mass velocity steam ($X < 1.0$) through a break
G_2	= Mass velocity of flow from the dry well
g	= Acceleration due to gravity
g_c	= Conversion factor $(lb_m/lb_f)(ft/sec^2)$
h_b	= Boiling heat transfer coefficient
	= Specific enthalpy of fluid escaping from break in the primary vessel

h_c	= Coefficient for heat transfer through condensate film
h_{eff}	= Effective heat transfer coefficient due to condensation
h_g	= Coefficient for sensible heat transfer through non-condensable gas film
h_o	= Heat transfer coefficient at outside surface of containment vessel
h_ℓ	= Specific enthalpy of liquid
h_v	= Specific enthalpy of vapor
$h_{\ell v}$	= $h_v - h_\ell$
$-J$	= Conversion constant, ft-lb/Btu
J_D, J_H	= Chelton-Colburn J factors for heat and mass transfer
K	= Slip ratio, two phase flow
k	= Ratio of specific heat, $C_p/C_v = 1.3$ for steam
k_g	= Coefficient for mass transfer through noncondensable gas film
L_T	= Length of tubes in reactor core
L	= Length of heat transfer path through wall = Length of vent
$L+\Delta L$	= Equivalent length of vent including effect of inertia
M_A	= Molecular weight of air
M_m	= Molecular weight of steam-air mixture
M_s	= Molecular weight of steam
M	= Mach number
M_3	= Mach number at the exit
M_e	= Mach number at the entrance

N	= Number of increments used in differential difference equation
n	= nth element in difference equation set
P	= Power level of the reactor
p	= Pressure at saturation
p°	= Initial saturation pressure
p_T	= Total pressure
p_L	= Total pressure below core
p_u	= Total pressure above core
p_{bM}	= Log mean concentration of noncondensable gas in the gas film
$(Pr)_\ell$	= Prandtl number ($C_L u/k$) for liquid film
Q_C	= Heat "added" by heat transfer (condensation) on surface
Q_D	= Fission product decay heat
Q_R	= Heat "added" by recirculation cooler
q/A	= Heat flux due to boiling
R	= Gas constant: $1543(\text{ft})(\text{lb})/\text{lb mole } ^\circ\text{F}$
R_C	= Resistance to flow through the core, $\text{psi}/(\text{lb}/\text{sec})^2$
S	= Rate of spray water flow
T	= Temperature
T_s	= Temperature of steam in vapor region
T_i	= Temperature at gas-liquid film interface
T_o	= Temperature at wall surface

u	= Velocity
V	= System volume
V_a	= Volume of zone "a" in two region blowdown model
v_l	= Specific volume of liquid
v_v	= Specific volume of vapor
v_{lv}	= $v_v - v_l$
v_m	= Specific volume of liquid-vapor mixture
W_w	= Weight of water
W_L	= Weight of water below the core
W_u	= Weight of water above the core
W_a	= Weight of air
X	= Weight fraction of steam in total volume
X_m	= Steam quality in vapor space
α	= Void fraction in two phase flow
	= Thermal diffusivity (k/PC_p)
λ	= Heat of vaporization
ν	= Gas phase viscosity
ρ	= Density
σ	= Surface tension
θ	= Two phase pressure drop multiplier
ψ	= Function of Mach number
τ_m, τ_e	= Arbitrary time constants to approximate flow from the primary vessel

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