QUARTERLY TECHNICAL REPORT
NUCLEAR SAFETY PROGRAM DIVISION
July 1 - September 30, 1969

IDaho Nuclear Corporation
National Reactor Testing Station
Idaho Falls, Idaho

U.S. Atomic Energy Commission
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SUMMARY

This quarterly report is the first in the series of reports to be prepared by the Nuclear Safety Program Division of Idaho Nuclear Corporation under contract to the Atomic Energy Commission. The studies reported are continuations of those previously conducted and reported by the Atomic Energy Division of Phillips Petroleum Company. The work reported was performed by the Nuclear Safety Development Branch and the SPERT Branch of the Nuclear Safety Program Division.

Choked flow models and pressure data from the Semiscale Blowdown and ECC Program were used to determine the quality of the steam-water mixture produced during decompression following system rupture. The quality of the steam-water mixture was used in obtaining calculations of the critical mass velocity. The amount of water expelled from the system was then calculated by integrating the critical mass velocity throughout the saturated portion of decompression and by multiplying by the cross-sectional area of the flow path. The accuracy of the critical mass velocity calculations was determined from comparisons of the calculated and measured values of water expelled during the Semiscale Blowdown and ECC tests. The tests included in this study were initiated by ruptures having 2 to 100% of the fully open pipe area; the location of the blowdown nozzle was near the top of the vessel for most tests.

For larger breaks (100%) and in earlier portions of blowdown caused by intermediate breaks (30 to 60%) critical flow occurred at the exit of the nozzle. For smaller breaks (2 to 10%) and in later portions of blowdown caused by intermediate breaks, critical flow occurred at the orifice which controlled the break area. Fauske's or Moody's choked flow models were used in the critical flow calculations. Close agreement was achieved between the calculated and measured values of mass expelled from the system during blowdown.

An extensive survey and compilation of rod bundle critical heat flux (CHF) data and comparisons of selected correlations with the data have been conducted. The intent of the study was to determine which correlations are accurate over wide ranges of pressure, mass flux, and core inlet conditions such that they can be used for loss-of-coolant accident analyses concerned with the transient behavior of nuclear reactor cores. The effect of attaining CHF is one portion of the accident which is of importance.

Data and accurate correlations for important ranges of pressure, mass flux, and core inlet conditions occurring during a loss-of-coolant accident have not appeared in the open literature. Since prediction of CHF requires experimental data or an empirical correlation, the lack of data and correlations makes accurate predictive analyses difficult. The approach, therefore, is to determine which correlations extrapolate beyond the ranges of parameters over which they are based with reasonable accuracy. The Barnett correlation was found to extrapolate, and when compared with the available data for pressures less than 1000 psia overprediction of CHF occurred. Analyses conducted with empirical correlations outside the ranges of parameters over which they are based can only be considered estimates of the actual results.
Review of the literature indicated that rod bundle CHF data for important ranges of parameters have not appeared in the open literature. These parameters are nonuniform axial heat flux distribution, high pressure, and two-phase mixture at the core inlet. More specifically for loss-of-coolant accident analyses, data for rapid pressure-flow transients are required.

Within the limitations of the data, suggestions for the application of existing correlations to loss-of-coolant accident analyses are given.

RELAP3, which is a computer code that describes the behavior of water-cooled nuclear reactors during postulated accidents, was assessed through comparison of the calculated results with experimental results obtained from the Containment Systems Experiment (CSE) vessel at Battelle Memorial Institute, Pacific Northwest Laboratory. The specific CSE tests for which comparisons were performed were Blowdowns B-19, B-19-B, and B-15.

Differences in the initial conditions between some calculated and experimental results were observed. Some of these differences indicate that reported initial test conditions may have differed from those actually existing in the CSE system at the time of blowdown.

RELAP3 was found to predict CSE blowdown results reasonably well. Differences between calculated and experimentally determined pressures occurred because of the presence of a gas pressure, other than that from steam, which acts on the fluid during blowdown and because of differences between the initial temperatures given for the experiment and the initial temperatures indicated by pressure results. For example, a change of 13°F in the initial temperature changed the saturation pressure by about 145 psi.

Calculated blowdown transients were influenced by changes in initial fluid mass and discharge coefficients. For the bottom blowdowns considered during the saturated blowdown period prior to the transition to high quality, better agreement between the analytical and experimental results was obtained through use of a homogeneous-fluid blowdown model than with a separation model. These results suggest that changes in the separation model may improve the calculated data. Since the homogeneous model does not predict liquid remaining, mixture level, nor the time at which the liquid level reaches the discharge nozzle and high quality transition occurs, the separation model is considered essential for RELAP3.

Experimental and analytical evidence indicated that all the liquid may have been expelled during some or all of the blowdown experiments studies. If all the liquid was ejected, the latter portion of the high quality blowdown period would be single-phase gas expansion whereas if liquid remained in the system, the steam ejected would be partially replaced by vaporization of the remaining liquid. The latter condition increases temperatures and pressures and is representative of large power reactor systems.

A series of nuclear reactor containment response tests was performed at the Carolinas Virginia Tube Reactor (CVTR) facility by Idaho Nuclear Corporation and the Carolinas Virginia Nuclear Power Associates (CVNPA),
Inc. The objectives of these tests were (a) to obtain data on the temperature-pressure response of the CVTR containment system to design-basis-accident conditions and to compare these data with response predictions obtained through use of current analytical techniques; (b) to assess the effectiveness of a containment pressure reduction spray system; (c) to determine structural state-of-the-art seismic analysis; and (d) to evaluate procedures, instrumentation, and assumptions commonly utilized in leakage rate testing of power reactor containments. Three major test efforts were conducted to meet these objectives. Structural vibration tests were conducted in which two independent techniques were used to measure the vibrational characteristics of the containment structure. Leakage rate tests were conducted to determine the response of the containment as a function of temperature and pressure. Design basis accident (loss-of-coolant) conditions were simulated in steam injection tests.

The structural vibration tests employed the Ambient Vibration Survey and the forced vibration (shaker) techniques for determining the characteristic vibration frequencies, mode shapes, damping, and effective masses of the containment. Although decisive agreement between the results from these two techniques was not achieved the information gained was valuable in understanding structural behavior and in determining whether additional investigations are needed. Both techniques indicated relatively large amplitude motion of the internal structures relative to the stiff containment walls. The capability of detecting this type of motion emphasizes the possible value of such measurements for detecting components with excessive response in as-built systems. Both techniques indicated that vibrational analysis of the containment system was far more complex than that of previously tested high-rise structures. Some differences in response can be attributed to differences in the excitation levels of the two techniques. The higher excitation level of the shaker tests and consequent greater displacement (about one-thousandth of an inch) may have resulted in a structure constrained near ground level, whereas the Ambient Vibration Survey may have tested a structure constrained at the base only. If a different structure was tested in each case both results may be correct and complementary. The results strongly suggest the need for additional investigations to assess experimental techniques for establishing vibrational features and to develop criteria that will provide an adequate basis for analytical modeling. Additional test experience is needed to develop a better understanding of how to incorporate material properties and design features.

The simulated design-basis-accident (DBA) tests were performed in the CVTR containment to provide experimental information for use in developing and evaluating analytical models for safety analysis of nuclear power plants. The four tests performed were (a) a system check-out test at about 7 psig with the pressure reduction spray system operated briefly; (b) an initial DBA test at about 18 psig followed by pressure decay through natural processes; (c) a second DBA test at 18 psig but with the pressure reduction spray system operating at 290 gpm; and (d) a third DBA test at 18 psig and with the pressure reduction spray system operating at 500 gpm. Several conclusions were reached from preliminary evaluations of the test results. Most significant of these conclusions was the apparent high rate of energy transfer from the steam-air mixture in the containment volume to the heat absorbing structures of the containment. This high energy transfer rate resulted in lower peak pressures
than were predicted by computer code calculations using currently accepted heat transfer coefficients and may also account for the pressures being higher than predicted during the decay period following steam injection. Large temperature stratification was another significant finding of these tests. Because of the interaction of temperature and heat transfer, a multinode-type analytical model appears to be needed for more accurate representation and prediction of containment response. The pressure-reduction spray system effectively reduced the containment pressure following termination of steam injection. Since the containment leakage rate is a function of pressure, use of pressure-reduction sprays can substantially reduce the potential for fission product release from containments during accidents involving a nuclear reactor.

The CVTR leakage rate tests were performed to determine containment leakage at both ambient and elevated temperatures for several static pressure conditions. Leakage rate measurements were made by both the absolute and reference vessel methods, and leakage rates were calculated by using equations recommended by the American Nuclear Society. Although containment leakage during ambient temperature tests was best extrapolated by the molecular flow equation, the prevailing type of leakage was probably not molecular, but rather a combination of laminar and turbulent types of flow. CVTR results indicate that although an extrapolation equation may predict the containment leakage rate, the leakage may not be of the type equation which applies. Furthermore, to determine which equation applies may be difficult. Even if a series of tests is performed to obtain an applicable equation the equation will be valid only as long as the leakage paths remain constant. A definite decrease in containment leakage was observed during hot air tests as the temperature increased. Also, physical effects on the containment were pronounced as the temperature increased. Cracks appeared on the outside concrete surfaces. Paint inside the containment darkened, grease and oil dripped from motors and valves, and shielding material melted and flowed from neutron chambers and the refueling machine. The containment liner developed small inward dents presumably from expanding against aggregate protruding from the inside surface of the concrete wall. The containment integrity, however, was not violated even though high temperature conditions were maintained for more than ten days. Following the DBA tests, a final leakage rate test was performed to determine the overall effects of DBA conditions on containment leakage and to determine whether DBA conditions had violated containment integrity. This test showed a slight decrease in leakage, approximately 0.23%/day as compared to 0.31%/day. Thus the containment leakage rate did not increase because of DBA conditions and possibly decreased slightly.

The Capsule Driver Core (CDC) installed in SPERT IV is being used to obtain experimental data on the behavior of reactor fuels under transient power conditions. Test fuel rods are positioned in the central flux trap in the CDC in an environment of coolant, hardware, fuel rod grouping, flow constraints, pressure, temperature, and other environmental parameters which might exist in a power reactor at the onset of a reactivity accident. The preliminary results of finite phases of the test program are published in interim technical reports and final reports are published subsequently in topical reports or professional journals. Summaries of the interim reports published during the period June-September 1969 are presented in this report. Additional sources of information also are indicated.
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I. THERMAL-HYDRAULIC ANALYSIS -- INVESTIGATION OF CRITICAL FLOW CONDITION DURING A LOSS-OF-COOLANT ACCIDENT

H. D. Curet and M. Shiba[a]

During the loss-of-coolant accident in a light water power reactor, the critical flow condition at the primary system leak can affect the overall blowdown process and quantities such as fluid decompression rate, core heat transfer rate, and containment pressurization rate. Thus, to appropriately evaluate the consequences of a loss-of-coolant accident of a water reactor adequate critical, or choked, flow models are required.

Previous investigations have developed methods of predicting the critical mass velocity for steam-water mixtures from known static pressure, enthalpy, and quality conditions; but, these methods are not easily applied to vessel blowdown calculations because the quality of the steam-water mixture at the choked location cannot be measured.

This section presents an approach which uses choked flow models and pressure data from the Semiscale Blowdown and ECC Program [1] to determine the quality of the steam-water mixture. With the quality information the critical mass velocity was calculated and integrated through the entire process of the saturated blowdown and multiplied by the cross-sectional area of the flow path to determine the amount of water expelled from the system. The accuracy of the critical mass velocity calculations was determined from the comparisons of the calculated and measured values of expelled water during blowdown tests.

This section also presents data obtained from the Semiscale Blowdown and ECC Program that were used to evaluate the adequacy of existing choked flow models for determining critical flow behavior during blowdown.

1. EXPERIMENTAL APPARATUS

Detailed configurations of test apparatus used in the 500, 600, and 700 series tests of the Semiscale Blowdown and ECC Program are shown in Figures 1 through 3. For these tests, the vessel and piping were mounted in support structures that prevented undesirable oscillations from affecting the decompression process.

Initial fluid conditions for the tests simulated actual PWR fluid conditions of 2300 psig and 540°F. These fluid conditions were provided by a small make-up system consisting of a pressurizer and in-line heaters. This system was isolated from the vessel before blowdown was initiated.

Blowdown tests were initiated by a rupture disc device. This device consisted of two rupture discs placed in series such that a break could be

[a] M. Shiba of the Japan Atomic Energy Research Institute is on a training assignment to Idaho Nuclear Corporation.
produced either by pressurizing or depressurizing the volume between them. A rupture time less than 1 msec was obtained. The rupture discs were located near the exit plane of the blowdown nozzle which was a 4-1/16-inch-ID pipe for all tests discussed in this section.

The test flask used in the 500 series tests was 12 inches in diameter and about 120 inches in axial length. The blowdown nozzle was attached near the top of the vessel as shown in Figure 1. A transducer for measuring the static pressure was located in the bottom nozzle flange.

In the 600 series, the test flask was modified to include a removable top head and instrumentation ports in the vessel shell. This flask is shown in
Fig. 2 Test flask for 600 series tests.

...
Figure 2. An internal structure simulated the reactor core but did not provide heat to the fluid.

The vessel used in the 700 series had a large diameter (14.5 inches) and a shorter length (64 inches) compared to the vessels used in the earlier tests. Locations of the pressure transducers used for measuring the vessel pressure and the static pressure at the nozzle are shown in Figure 3.
2. THE THRUST AND CRITICAL FLOW RELATIONSHIP

Several investigations[2-5] have resulted in flow models for predicting the critical mass velocity as a function of the static pressure and enthalpy for the flow of saturated steam-water mixtures from nozzles. The general forms of the equations used in the models are:

\[ G_c = G_c(p_s, h_s) \]  
\[ h_s = h_f(p_s) + x \cdot h_{fg}(p_s) \]

where

\[ G_c = \text{critical mass velocity (lbm/ft}^2\text{-sec)} \]
\[ p_s = \text{vessel pressure (psia)} \]
\[ h_s = \text{enthalpy of the steam-water mixture (Btu/lbm)} \]
\[ h_f = \text{enthalpy of saturated water (Btu/lbm)} \]
\[ x = \text{steam quality} \]
\[ h_{fg} = \text{latent heat of vaporization (Btu/lbm)}. \]

A choked flow model, which uses the source pressure and enthalpy for steam-water mixtures, was proposed by Moody[2] to give the critical mass velocity. The equations in this model are:

\[ G_c^2 = \frac{2g_c J \left[ h_s - h_f - h_{fg} (S_s - S_f)/S_{fg} \right]}{k(S_s - S_f) v_f + (S_s - S_f) v_{fg}} \]
\[ \frac{S_s - S_f}{S_{fg}} \left[ \frac{S_s - S_f}{S_{fg}} + \frac{S_f - S_s}{k^2 S_{fg}} \right]^2 \]

and

\[ k = (v_g/v_f)^{1/3} \]

where

\[ g_c = \text{gravitational conversion factor (32,174 lbm/lbf) (ft-sec}^{-2} \]
\[ J = \text{mechanical equivalent of heat (778 ft-lb/ftu)} \]
\[ S_s = \text{entropy of steam-water mixture (Btu/lbm}^{{\circ}{\text{F}}}) \]
\[ S_f = \text{entropy of saturated water (Btu/lbm}^{{\circ}{\text{F}}}) \]
\[ S_{fg} = S_f - S_g (\text{Btu/lbm} - \theta) \]

\[ S_g = \text{entropy of saturated steam (Btu/lbm-\theta)} \]

\[ v_f = \text{specific volume of saturated liquid (ft}^3/\text{lbm}) \]

\[ v_g = \text{specific volume of saturated steam (ft}^3/\text{lbm}) \]

\[ k = \text{slip ratio.} \]

With the exception of \( h_S \) and \( S_S \) which are determined by the source pressure and the quality of the mixture, the property values in these equations are calculated for the pressure, \( p \), which satisfies

\[ \frac{dG_c}{dp} = 0 \]  \hspace{1cm} (5)

Another choked flow model proposed by Fauske[3] also was used in the calculations. These equations are:

\[ g_c^2 = g_c k / \left[ (1 + x - kx) \frac{dv}{dp} + (1 + 2kx - 2x) v_g \right. \]

\[ + \left. \left( \frac{v_f}{h_{fg}} \right)^2 (2kx - 2k + 2k^2 x + k^2) \left( \frac{dh_f}{dp} + \frac{dh_{fg}}{dp} \right) \right] \]  \hspace{1cm} (6)

and

\[ k = \left( \frac{v_g}{v_f} \right)^{1/2} \]  \hspace{1cm} (7)

The property values in these equations are calculated for the nozzle pressure, \( p_n \):

\[ p_n = 0.55 p_v \]  \hspace{1cm} (8)

Equation (8) is valid only when the length-to-diameter ratio of the nozzle is larger than twelve.

These flow models are not easily applied to vessel blowdown calculations because the experimental determination of the quality of the mixtures is impossible.

The Semiscale Blowdown and ECC Program provided data on the static pressure measured at certain points within the vessel and nozzle during blowdown. Thrust force measurements also were performed in some blowdown tests. Experimental pressure and thrust data were used in conjunction with analytical techniques to determine the quality required to predict the critical mass velocity for vessel blowdown.
The choking conditions can occur at the entrance to the nozzle, at the exit of the nozzle, and at the orifice. In an earlier report[6] the thrust force applied to the cross section of flow path and the exit of the nozzle for break sizes less than 100% was shown to be equal to

\[ T = (p_v - p_e) A_B + (A_N - A_B) p_e \] (9a)

where

- \( T \) = thrust force (lb)
- \( A_B \) = cross-sectional area of the break (ft²)
- \( A_N \) = cross-sectional area of the nozzle (ft²)
- \( p_e \) = static pressure at the nozzle wall (psia)
- \( p_v = p_s \) = static pressure in the vessel (psia).

For 100% area breaks

\[ T = p_v A_N \] (9b)

An equation

\[ T = (p_v + p_e) A_B \] (9c)

giving thrust force applied to the cross section at the orifice can be obtained by considering the net force applied to the flow path at the orifice. Previous investigations[7,8] have shown that thrust is a summation of the momentum expulsion rate and the pressure force:

\[ T = (p_t + \frac{C_p^2 \rho_m g_c}{A}) \] (10)

where

- \( p_t \) = throat pressure (psia)
- \( \rho_m \) = density of steam-water mixture (lb_m/ft³).

A can be equal to \( A_N \) or \( A_B \) depending on whether or not choking occurs at the exit of the orifice.

The density of the mixtures was calculated for the throat pressure, \( p_t \), by

\[ \frac{1}{\rho_m} = \left[ x v_g + (1 - x) k v_f \right] \left[ x + (1 - x)/k \right] \] (11)
Equations (1), (2), and (11) and one of Equations (9a), (9b), and (9c), depending on the location of the critical flow, substituted in the appropriate combination into Equation 10 give the critical mass velocity, $G_c$, and the quality, $x$, as functions of the static pressure, $p_s$.

Although the critical mass velocity measurements were not performed, the experimental data of the amount of the water expelled from the system during blowdown are available from the Semiscale Blowdown and ECC Program and these data were used to check the calculational procedure. The amount of water expelled, $W$, from the system during blowdown is obtained by multiplying the integral of the critical mass velocity by the cross-sectional area:

$$W = A \int G_c \, dt$$

(12)

where $A$ can be either $A_N$ or $A_B$ depending on the choking location.

3. PRESENTATION OF RESULTS

Equation (12) was used to calculate the amount of water expelled for various tests. Table I gives a comparison between the measured and calculated values of expelled water. The subcooled portion of blowdown was ignored because its duration was very short; the integration was started when the saturation pressure corresponding to the initial temperature was reached. The final portion of blowdown also was ignored because it gives a very small change in the results of the integration; the integration was discontinued when 60 psia was reached. The size of the break varied from 2 to 100% in these tests and the location of the blowdown nozzle was near the top of the vessel for all the tests except Test 708 which was a bottom break. Internal structures were inserted only during the 600 test series and during Tests 715 and 716.

The tests are classified as large (100%), intermediate (30 to 60%), and small (2 to 10%) break tests.

For larger breaks and in earlier portions of blowdown caused by intermediate breaks, the critical flow occurs at the exit of the nozzle. For small breaks and in later portions of blowdown caused by intermediate breaks, the critical flow occurs at the orifice. The critical flow may also occur at the entrance of the nozzle but no attempt has been made to determine whether it really occurs there or not.

One hundred percent breaks can be treated as large breaks and Equations (6), (7), and (8) of Fauske's model, with Equation (9b) can be successfully applied to the critical flow calculation. During earlier portions of blowdown caused by intermediate breaks, Equations (3), (4), and (5) of Moody's model, with Equation (9a) give very good results. Sixty and 30% breaks can be treated as intermediate breaks. During later portions of blowdown caused by intermediate breaks, in which the pressure at the exit of the nozzle goes to zero but the vessel pressure is still high, the critical flow occurs at the orifice. In this calculation, the static pressure measured upstream of the orifice was used as a source pressure to give $h_S$ and $S_S$ in Equation (3). For small break blowdowns, in which the


**TABLE I**

EXPERIMENTAL CONDITIONS AND THE RESULTS OF THE EXPELLED WATER CALCULATION

<table>
<thead>
<tr>
<th>Test</th>
<th>Initial Pressure (psi)</th>
<th>Initial Temperature (°F)</th>
<th>Thrust Equation Used</th>
<th>Flow Model</th>
<th>Break Size (%)</th>
<th>Break Location</th>
<th>Vessel Configuration</th>
<th>Expelled Water (lbm)</th>
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<tr>
<td>548</td>
<td>2330</td>
<td>520</td>
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<td>316</td>
</tr>
<tr>
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<td>2330</td>
<td>520</td>
<td>9a and -c</td>
<td>Moody-Fauske</td>
<td>60</td>
<td>top</td>
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<td>315</td>
</tr>
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<td>2360</td>
<td>530</td>
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<td>Fauske[a]</td>
<td>100</td>
<td>top</td>
<td>With internals</td>
<td>350</td>
</tr>
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<td>540</td>
<td>9c</td>
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<td>2</td>
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<td>No internals</td>
<td>128</td>
</tr>
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<td>2340</td>
<td>540</td>
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<td>Fauske</td>
<td>10</td>
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<td>No internals</td>
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</tr>
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<td>540</td>
<td>9a and -c</td>
<td>Moody-Fauske</td>
<td>30</td>
<td>top</td>
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<td>9b</td>
<td>Fauske</td>
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<td>No internals</td>
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<td>2240</td>
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<td>top</td>
<td>With internals</td>
<td>220</td>
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<td>520</td>
<td>9b</td>
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<td>top</td>
<td>With internals</td>
<td>295</td>
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</table>

[a] Equation 4 was used in the calculation.
exit pressure is always close to or equal to zero psig, the critical flow occurs at the orifice. Equations (6), (7), and (8) with Equation (9c) give good results for small break blowdowns.

3.1 Results of the 700 Series Tests

Table I shows the experimental conditions of the tests of the 700 series. It also indicates the equations and flow models that were used to calculate the mass velocity and compares the calculated results with the experimental data.

Figure 4 shows the pressure and calculated critical mass velocity transients for Test 706. The subcooled portion and the portion of blowdown after 60 psia was reached are neglected. Figure 5 shows the pressure and critical mass velocity transients for Test 708. As indicated in Table I, Fauske's model was used to calculate the critical mass velocity for these large break tests. Figure 6 shows the pressure and critical mass velocity transients for Test 716. As shown, the mass velocity transient has more inflections than similar transients of other tests. These inflections are considered to be a result of the effect the core internals have on phase separation in the vessel, which in turn affects the state of the fluid at the choking location. The same calculation technique was found to be applicable to both top and bottom blowdowns. The technique can also deal with Test 716 in which internal structures were inserted in the vessel.
Fig. 5 Pressure and critical mass velocity for Test 708.

Test 708
100 % Bottom Break
Initial Pressure - 2330 psig
Initial Temperature - 540 °F

Fig. 6 Pressure and critical mass velocity for Test 716.

Test 716 (with core)
100 % Top Break
Fauske's model in combination with Moody's model was applied to determine the critical mass velocity transient for Tests 704 and 715. The expelled water results calculated from the mass velocity transients show close agreement with the experimental results for both Test 704 without internals and Test 715 with internals.

Attempts using Fauske's and Moody's models were made to determine the location of choked flow during blowdown for Test 704. The results indicated that the choked flow condition shifted from the exit to the orifice during blowdown. Figure 7 shows the pressure transient and mass flow rate for this test.

For smaller breaks, such as the 2 and 10% breaks of Tests 702 and 703, critical flow occurred at the orifice. The static pressures downstream of the orifice were not high enough to cause critical flow at the nozzle exit. Comparison of the pressure and the mass velocity transients for Test 703 in Figure 8 with the transients for Test 706 in Figure 4 shows that an appreciable difference exists in the mass velocity for the same pressure value. This difference is attributed to the back pressure downstream of the orifice.
3.2 Results of the 500 and 600 Series Tests

Critical mass velocity calculations were made for Tests 548, 558, 560, 601, 603, and 604. Internal structures were inserted into the vessel during the tests in the 600 series. The size of the break varied from 2 to 100% in these tests, and the location of the break was near the top of the vessel for all the tests.

Equations (6) and (8), based on Fauske's model, with Equation (9b) were applied to the critical flow calculation for Tests 548 and 604 which were 100% top break blowdowns. Unlike the calculations with Fauske's model for comparable 700 series blowdown tests, good agreement could not be obtained for calculated and measured expelled water results when Equation (7) was used. Replacing Equation (7) with Equation (4) provided agreement within 10% of the measured results. This better agreement obtained by replacing Equation (7) with (4) indicates that the slip ratio for the 500 and 600 series tests is attributed to an increase in phase separation associated with the larger length-to-diameter ratio of the vessel used in the 500 and 600 series tests. The length-to-diameter ratio was about 10 for the 500 and 600 series tests vessel and 4.3 for the 700 series test vessel. For Tests 560 and 601, which are small break blowdowns, Equations (3), (4), (5) with Equation (9c) give very good results.

Figure 9 indicates a relation between measured residual water and the size of the break for tests of the same initial fluid condition in the 500 and 600 series. Some of the data points shown in Figure 9 were calculated by the technique described in the paper and these calculated values agreed within 10% of the measured residual water values from blowdown tests with and without hydraulic restrictions in the vessel. Thus, the choked flow models described
in the preceding section are applicable to the critical flow calculation independent of the insertion of internals in the vessel.

Figure 10 shows a comparison between the calculated and measured mass expelled from the system during blowdown. The figure indicates close agreement between the calculated and measured values.
Fig. 10 Comparison between calculated and measured mass removed.
II. HEAT TRANSFER ANALYSIS -- AN EXAMINATION OF ROD BUNDLE CRITICAL HEAT FLUX DATA AND CORRELATIONS AND THEIR APPLICABILITY TO LOSS-OF-COOLANT ACCIDENT ANALYSES

E. Daniel Hughes

One major parameter in loss-of-coolant accident (LOCA) analyses is the time-to-critical heat flux\(^a\) (CHF), that is, the period of time from initiation of the system rupture, or break, to the time critical heat flux occurs in the core. Immediately following the occurrence of a break, the pressure differential across the core results in forced convection which transfers the sensible energy stored in the fuel rods and the decay energy generated by nuclear fission from the rods to the coolant flowing through the core. Eventually, the critical heat flux condition, which is a function of the rod and core geometry, axial heat flux distribution, and the flow and thermodynamic state of the coolant may be reached. At CHF a film of vapor which exists adjacent to the rod surface results in severe reduction in the amount of energy that can be removed from the core. Following the attainment of the CHF condition, the rod surface temperature begins to increase as the energy remaining in the rods and the generated decay energy redistribute.

Accurate, steady state CHF correlations that may be used in conjunction with transient loss-of-coolant accident analysis computer codes are considered in this section. These correlations and codes can later be used to determine the time-to-CHF for the reactor system and rupture under consideration.

Although predictions of the critical heat flux are necessary for reactor design purposes, the phenomenon maintains a unique status in engineering in that no theoretical analysis has been attempted and consequently experimental data are the only source of information regarding its occurrence. Therefore, to obtain values of the critical heat flux for engineering design purposes, experiments are usually conducted at steady state with electrically heated rods of design diameter, pitch, spacing, and length with fluid conditions at or near the design operating point. If sufficient data are taken during tests in which the core inlet condition and coolant mass flux are varied systematically, a correlation of the data may be attempted, and if acquired, may be used as a design equation.

In contrast to the problem of obtaining the information required for design purposes, the problem of predicting when CHF will be reached during a loss-of-coolant accident requires that data be taken over wide ranges of pressure, mass flux, and inlet conditions, and also that the effects of rapid changes in pressure and mass flux be taken into account. An investigation of all steady state, uniform axial flux profile, rod bundle CHF data available in the open literature and several CHF correlations has been undertaken with the objective of determining the applicability of the data and correlations to analytical models being

\[a\] Critical heat flux is used in this section to describe a large decrease in the local heat transfer coefficient which results in a large increase of the surface temperature.
developed to predict the core thermal behavior during a loss-of-coolant accident. This section summarizes the results of the study. Section II-1 gives the nomenclature used in the succeeding discussions. Section II-2 discusses the available sources of rod bundle critical heat flux data. Section II-3 gives the critical heat flux correlations and their ranges of applicability. Section II-4 presents comparisons of the results obtained from the correlations with experimental data. Section II-5 discusses the applicability of the Barnett correlation to loss-of-coolant accident analyses. Section II-6 presents conclusions and recommendations.

1. NOMENCLATURE

<table>
<thead>
<tr>
<th>Variable</th>
<th>Definition</th>
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<tr>
<td>A</td>
<td>cross-sectional flow area of channel (in.²)</td>
</tr>
<tr>
<td>D</td>
<td>circular conduit diameter (in.)</td>
</tr>
<tr>
<td>D₇E</td>
<td>heated equivalent diameter, ( \frac{4 \cdot \text{flow area}}{\text{heated perimeter}} ) (in.)</td>
</tr>
<tr>
<td>D₇E*</td>
<td>defined by Equation (2f) (in.)</td>
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<tr>
<td>D₇HY</td>
<td>wetted equivalent diameter, ( \frac{4 \cdot \text{flow area}}{\text{wetted perimeter}} ) (in.)</td>
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<td>D₀</td>
<td>defined by Equation (2e) (in.)</td>
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<tr>
<td>Dᵣ</td>
<td>heater rod diameter (in.)</td>
</tr>
<tr>
<td>G</td>
<td>mass flux (lb/ft²/hr)</td>
</tr>
<tr>
<td>h</td>
<td>enthalpy (Btu/lbₗ)</td>
</tr>
<tr>
<td>Δhᵢn</td>
<td>inlet subcooling, ( h_f - h_{in} ) (Btu/lbₗ)</td>
</tr>
<tr>
<td>P</td>
<td>pressure (lb/in²)</td>
</tr>
<tr>
<td>L</td>
<td>heated length (in.)</td>
</tr>
<tr>
<td>q</td>
<td>heat flux (Btu/hr-ft²)</td>
</tr>
<tr>
<td>x</td>
<td>thermodynamic steam quality</td>
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</tbody>
</table>

Subscripts

- c calculated value

17
CHF critical heat flux
e experimental value
f saturated liquid
g saturated vapor
in inlet.

2. AVAILABLE ROD BUNDLE CRITICAL HEAT FLUX DATA

Although the critical heat flux data for circular conduits [9-22] and to a lesser extent annuli [23-25] have been obtained systematically and very accurate correlations have been derived, the present status of rod bundle data and correlations is somewhat confusing [9,26]. Conflicting interpretations of the same data have been made, and among data sets many directly opposing results seem to be indicated.

Several correlations have been generated but these have usually been confined to the limited amount of data obtained by one worker or laboratory. In addition, these correlations were considered accurate if the data on which they were based could be predicted within ±40%. Loss-of-coolant accident analyses require much more accurate and extensive correlations because of the wide ranges of pressure, mass flux, and inlet conditions encountered during a loss-of-coolant accident.

In the past few years attempts to correlate all existing rod bundle data in the open literature have been made [18,24,27,28]. However, except for the Becker correlation [27], the application of the resulting correlations has necessarily been restricted to a pressure of approximately 1000 psia. The restriction of the correlations to 1000 psia is due to the very limited, and in many cases nonexistent, data at other pressures rather than the inability of the method of correlation.

The most complete and comprehensive compilations of rod bundle CHF data are contained in the reports of Macbeth [27] and Barnett [18,24]. Additional data have been found in the open literature for use in the present study, and Table II summarizes all data that have been determined to be self-consistent and consistent with other rod bundle data. The experiments represented by Table II cover the following ranges of parameters:

<table>
<thead>
<tr>
<th>Parameter</th>
<th>Range</th>
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<tr>
<td>Pressure</td>
<td>156 to 1400 psia</td>
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<tr>
<td>Mass flux</td>
<td>$0.02 \times 10^6$ to $4.0 \times 10^6$ lb$_m$/hr-ft$^2$</td>
</tr>
<tr>
<td>Fluid inlet condition</td>
<td>0 to 373 Btu/lb$_m$ subcooling</td>
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[a] Reference 26 shows that circular conduit and annulus CHF data cannot be applied directly to the rod bundle geometry.
<table>
<thead>
<tr>
<th>Bundle Number</th>
<th>Data Reference</th>
<th>Number of Rods</th>
<th>Method Used to Support Rods</th>
<th>Heated Length (in.)</th>
<th>Rod Diameter (in.)</th>
<th>Inlet Pressure (psia)</th>
<th>Radial Flux Distribution</th>
<th>Flow Area (in.²)</th>
<th>Heated Perimeter (in.)</th>
<th>Wetted Perimeter (in.)</th>
<th>d_min (in.)</th>
<th>d_max (in.)</th>
<th>Minimum Gap (in.)</th>
<th>Rod to Rod</th>
<th>Rod to Shroud</th>
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<td>0.494</td>
<td>0.360</td>
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<td>Uniform</td>
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<td>0.969</td>
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<tr>
<td>18</td>
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<td>Spacers</td>
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<td>70.68</td>
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<td>Rod Diameter (in.)</td>
<td>Inlet Pressure (psia)</td>
<td>Radial Flux Distribution</td>
<td>Flow Area (in.²)</td>
<td>Heated Perimeter (in.)</td>
<td>Wetted Perimeter (in.)</td>
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<td>D₁₅₀ (in.)</td>
<td>Minimum Gap (in.)</td>
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<td>1000</td>
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<td>1000</td>
<td>Uniform</td>
<td>1.047</td>
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<td>2.707</td>
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<td>19.57</td>
<td>0.485</td>
<td>0.187</td>
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<tr>
<td>35</td>
<td>29</td>
<td>9</td>
<td>Spacing pins</td>
<td>60.0</td>
<td>0.4375</td>
<td>600</td>
<td>8 rods at 0.930</td>
<td>2.373</td>
<td>19.57</td>
<td>19.57</td>
<td>0.485</td>
<td>0.187</td>
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<td>36</td>
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<td>Spacing pins</td>
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<td>1400</td>
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<td>2.373</td>
<td>19.57</td>
<td>19.57</td>
<td>0.485</td>
<td>0.187</td>
<td>0.135</td>
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</tbody>
</table>

(a) Pressure variation was not systematic.
(b) The rods in the bundle are arranged in rings with radial flux variation being from the outer ring to the center rod.
(c) The rods in the bundle are arranged in a square array within a square shroud with rounded corners. The hot rod is one of the corner rods.
Rod diameter 0.250 to 0.625 in.
Rod length 30.0 to 178.0 in.
Spacing between rods 0.022 to 0.307 in.
Axial heat flux distribution Uniform.

The experiments have been conducted by many different investigators for various reasons; however, the only systematic investigation of CHF has been for 1000 psia. At all other pressures the data are scattered with respect to length, inlet condition, mass flux, rod diameter, spacing, pitch, and spacers. The random nature of the data leads to confusion in interpreting the results and precludes determining the effect of pressure on critical heat flux.

An examination of the data shows that the following information required for LOCA analyses is not available in the open literature:

1. Rod bundle data from tests in which the axial distribution of the heat flux was not uniform.
2. Data for full length bundles typical of current PWR and BWR internal construction.
3. Data at very low mass fluxes and two-phase inlet condition.
4. Data for high pressure rod bundles.

Or, in general, a systematic investigation of full-length rod bundle CHF with respect to pressure, mass flux, and inlet condition has not been presented in the open literature. The absence of these data makes LOCA analyses difficult because calculations of CHF require data in the range of pressure, mass flux, and inlet condition of interest or a correlation based on data within the range of interest.

A typical example of the experimental investigation of critical heat flux is given by the results of Bundle 12 of Table II. Figure 11 shows $q_{\text{CHF}}$ as a function of the inlet subcooling, $\Delta h_{\text{in}}$, with mass flux as a parameter for Bundle 12. As first noted by Macbeth[27], the linear dependence of rod bundle CHF on inlet subcooling is clearly shown. This dependency is also true for circular conduits[10] and annuli[24]. Other information available from Figure 11 is the fact that $q_{\text{CHF}}$ is proportional to the square root of the mass flux at small values of $\Delta h_{\text{in}}$ (less than 20 Btu/lbm) and that as the mass flux decreases, $q_{\text{CHF}}$ becomes less dependent on the inlet subcooling[27]. Similar graphs for bundles with only a change in length show that as the length increases the critical heat flux decreases and $q_{\text{CHF}}$ is less dependent on $\Delta h_{\text{in}}$ for all values of the mass flux, $G$.

All data represented by Table II have been checked for accuracy and consistency by use of plots such as given in Figure 11. The linear dependency of $q_{\text{CHF}}$ and $\Delta h_{\text{in}}$ allows the data to be checked for accuracy, and the fact that $q_{\text{CHF}}$ is proportional to $G^{1/2}$ allows the data to be checked for consistency with other data.
Before a comparison of existing correlations with the data is given the following bundle data found in the open literature, but not given in Table II, are discussed:

(1) Data from Battelle Northwest Laboratories[30] obtained in a test section with special mixers on the bundle shroud wall.

(2) Data from General Electric Company[29] and Westinghouse Electric Corporation[31], for which necessary geometrical information cannot be determined.
(3) Swedish data[25] for which the bundle shroud was heated and the CHF was reported to occur on the heated shroud. These data are considered to be nontypical of rod CHF as CHF occurred on the concave surface of the shroud as opposed to the convex surface of a rod.

(4) Data[27] for which the bundle was horizontal.

(5) Data from General Electric Company[32] for which the bundle was made up of rods only 18 inches long and the experimental results were obtained with steam-water inlet conditions. The experimental error is larger for these shorter rods, and the effect of the two-phase mixture at the inlet on q_{CHF} is not typical of the effect for longer rods. In the case of the longer rods the CHF measurements would be less dependent on the method used to introduce the mixture into the bundle because the entrance effects would be dissipated as the mixture travels through the bundle.

(6) Data[27] for which the bundles consisted of wire-wrapped rods. Since wrapped rods are not in use in current water cooled reactor designs, these data are omitted from the comparison.

About 750 data points are represented by these sources. Exclusion of these data leaves 1096 distinct critical heat flux points for vertical upflow in rod bundles which have been checked for accuracy and consistency. The data have been coded onto digital computer cards for purposes of calculation and storage. Geometrical data of the bundle, such as rod diameter, length, and spacing between rods, and operating data, such as pressure, mass flux, and critical heat flux, are coded for each point. As additional data become available, the CHF library is to be expanded to include all useful information.

Section II-3, which follows, discusses several critical heat flux correlations and their ranges of validity.

3. CRITICAL HEAT FLUX CORRELATIONS
AND THEIR RANGES OF VALIDITY

The five CHF correlations considered in this section are probably the most widely known and used at the present time. They have been given by Macbeth[27], Barnett[24], Becker[28], General Electric Company[33], and Westinghouse Electric Corporation (Shippingport)[33]. Details of the correlations are given in the following discussion.

3.1 Correlation Given By Macbeth[27]

Macbeth[27] compiled and analyzed 335 rod bundle critical heat flux data points at about 1000 psia pressure. Results obtained from wrapped bundles and
steam-water mixture inlet conditions were omitted such that 172 data points remained from which to derive the correlation

\[ q_{\text{CHF}} \times 10^{-6} = \frac{A + 0.25 \, D_{\text{HE}} \, (G \times 10^{-6}) \Delta h_{\text{in}}}{B + L} \]  

(1)

where

\[ A = 67.6 \, D_{\text{HE}}^{0.83} (G \times 10^{-6})^{0.57} \]

\[ B = 47.3 \, D_{\text{HE}}^{0.57} (G \times 10^{-6})^{0.27} \]

The experimental ranges of the system parameters used in deriving the correlation are as follows:

- \( P = 1000 \) psia
- \( 36 \leq L \leq 72 \) in.
- \( 0.18 \times 10^6 \leq G \leq 4.0 \times 10^6 \) lb m/hr-ft²
- \( 0 \leq \Delta h_{\text{in}} \leq 283 \) Btu/lb m
- \( 0.250 \leq D_r \leq 0.550 \) in.
- Uniform axial flux.

3.2 Correlation Given By Barnett[24]

Barnett[24] derived a rod bundle CHF correlation by defining an equivalent annulus for the bundle and then evaluating the annulus correlation[24] for the same \( P, L, G, \) and \( \Delta h_{\text{in}} \) as the rod bundle. The critical heat flux is given by

\[ q_{\text{CHF}} \times 10^{-6} = \frac{C + E \Delta h_{\text{in}}}{F + L} \]  

(2a)

where

\[ C = 67.45 \, D_{\text{HE}}^{0.68} (G \times 10^{-6})^{0.192} \left[ 1.0 - 0.744 \exp(-6.512 \, D_{\text{HY}} \, G \times 10^{-6}) \right] \]

\[ E = 0.2587 \, D_{\text{HE}}^{1.261} (G \times 10^{-6})^{0.817} \]

\[ F = 185.0 \, D_{\text{HY}}^{1.415} (G \times 10^{-6})^{0.212} \]

The description of the cross section of the equivalent annulus is given by

\[ D_{\text{HY}1} = D^*_0 - D^*_1 \]  

(2b)
\[ D_{HE1} = \frac{(D_r^2 - D_i^2)}{D_i^2} \]  

(2c)

where

\[ D_i^* = D_r, \text{ (heater rod diameter)} \]  

(2d)

\[ D_o^* = [D_r (D_r + D_{HE}^*)]^{1/2} \]  

(2e)

\[ D_{HE}^* = \frac{\frac{1}{4} \cdot \text{Flow area of the bundle}}{\pi D_r S} \]  

(2f)

\[ S = \frac{\sum q_r}{\text{rods} \ q_{\text{max}}} \]

where \( q_r \) is the heat flux on a rod and \( q_{\text{max}} \) is the maximum heat flux in the bundle.

The experimental ranges of the system parameters used in deriving the correlation are as follows:

- \( P = 1000 \) psia
- \( 24 \leq L \leq 108 \) in.
- \( 0.140 \times 10^6 \leq G \leq 6.20 \times 10^6 \) \( \text{lb}_m/\text{hr-ft}^2 \)
- \( 0 \leq \Delta h_{\text{in}} \leq 412 \) Btu/lb \(_m\)
- \( 0.258 \leq D_{HE} \leq 3.792 \) in.
- \( 0.127 \leq D_{HY} \leq 0.875 \) in.

Uniform axial flux.

3.3 Correlation Given By Becker[28]

Prediction of \( q_{\text{CHF}} \) by the Becker correlation[28] involves the use of graphical data and an iteration scheme. A computer program was written in which the curves are represented by short straight line segments and the iterative procedure is carried out. The program has been used to evaluate the correlation for cases for which Becker[25,28] also has given answers, and the results from the computer program have agreed with Becker's results[25,28].

This correlation is based on CHF data from circular conduits in the ranges

- \( 31 \leq P \leq 1400 \) psia
- \( 15.7 \leq L \leq 157.5 \) in.
The circular conduit data do not directly apply to the rod bundle case. Becker used correction factors to relate the thermodynamic quality at CHF, \( x_{CHF} \), for circular conduits to \( x_{CHF} \) for rod bundles.

### 3.4 General Electric Design Curve CHF Correlation[33]

The General Electric Company CHF correlation[33] was derived by constructing lines under all rod bundle data, within the following ranges of parameters, in the \( q_{CHF}, x_{CHF} \), plane. The critical heat flux is given by

\[
q_{CHF} \times 10^{-6} = 0.705 + 0.237 \ (G \times 10^{-6}), \quad x_{CHF} < x_1 \quad \text{(5a)}
\]

\[
q_{CHF} \times 10^{-6} = 1.634 - 0.270 \ (G \times 10^{-6}) - 4.710 \ x_{CHF}, \quad x_1 < x_{CHF} < x_2 \quad \text{(5b)}
\]

\[
q_{CHF} \times 10^{-6} = 0.605 - 0.164 \ (G \times 10^{-6}) - 0.653 \ x_{CHF}, \quad x_2 < x_{CHF} \quad \text{(5c)}
\]

where

\[
x_1 = 0.197 - 0.108 \ (G \times 10^{-6})
\]

\[
x_2 = 0.254 - 0.026 \ (G \times 10^{-6})
\]

and for pressures other than 1000 psia,

\[
q_{CHF} \text{ (at } P) = q_{CHF} \text{ (at 1000) + 440 (1000 - } P).
\]

The experimental ranges of the system parameters used in deriving the correlation are as follows:

- \( 600 \leq P \leq 1450 \) psia
- \( 29 \leq L \leq 108 \) in.
- \( 0.4 \times 10^{6} \leq G \leq 6.0 \times 10^{6} \ \text{lb}_m/\text{hr} \cdot \text{ft}^{2} \)

Limits on \( \Delta h_{in} \) are not given

- \( 0.245 \leq D_{HY} \leq 1.25 \) in.

Equations (5a) through (5c) do not represent the current General Electric Company correlation.
3.5 Westinghouse Electric Corporation Shippingport CHF Correlation[33]

Although the Shippingport correlation is based on data for flow in the thin rectangular channels used in the Shippingport reactor, it is sometimes applied to rod bundles. The critical heat flux is given by

\[ q_{\text{CHF}} \times 10^{-6} = K \left[ \frac{h^1 - h_{\text{CHF}}}{H^1 - H_o} \right]^{1/2} \]  

where

\[ K = 0.84 \left[ 1 + \left( \frac{2000.0 - P}{800.0} \right)^2 \right] \]

\[ H_o = 655.0 - 0.004 \times (2000.0 - P)^{1.63} \]

\[ H^1 = h_g - 0.275 \times h_{fg} - 0.725 \times h_{fg} \left[ \frac{300.0}{h_{fg}} \right]^{10^6/G}. \]

The range of parameters on which the correlation is based is not given[33]. Equation (6) cannot be used for pressures greater than 2000 psia and imaginary values of \( q_{\text{CHF}} \) may be obtained from this correlation.

The correlations given by Equations (1), (2), (5), and (6) and by Becker[28] are compared in Section II-4 with the data represented in Table II.

4. COMPARISONS OF CORRELATION PREDICTIONS AND EXPERIMENTAL DATA

In this section the predictions of the preceding correlations are compared with all 1096 rod bundle experimental CHF data (Table II) to determine their most accurate range of applicability. For purposes of comparison, the percent error between the calculated and experimental values is defined as

\[ \text{ERROR} = \frac{q_{\text{CHF},e} - q_{\text{CHF},c}}{q_{\text{CHF},e}} \times 100 \]  

where

\[ q_{\text{CHF},e} = \text{experimental critical heat flux} \]

\[ q_{\text{CHF},c} = \text{calculated critical heat flux}. \]

The results of the calculations are presented in Figures 12 through 16 as graphs of the frequency that an error is encountered versus the percent error of Equation (7). Although these figures do not reveal the details of the error distribution with respect to pressure, length, mass flux, inlet subcooling, and
bundle internal construction, they do give an overall view of the accuracy of the correlation. A more detailed investigation of the better correlations follows.

Figure 12 gives the error distribution for the Macbeth correlation of Equation (1). The results are good considering the fact that the correlation is based on a very limited amount of data (172 points)[27]. When compared with CHF values determined for ranges of parameters within as well as outside the ranges over which it is based, the correlation predicts 97% of the data within the error bounds -20% to +25%[a].

![Fig. 12 Error distribution for the Macbeth CHF correlation.](image)

[a] The 97% level of correlation is widely used in the open literature. However, the choice of the error bounds within which 97% of the data are predicted is quite arbitrary. For this discussion values were chosen between which the error distribution is symmetrical.
Figure 13 gives the error distribution for the Barnett correlation of Equations (2). Again, the data were obtained from bundles whose geometric and operating characteristics were within and outside the range over which the correlation was based. Indeed, Barnett derived this correlation on annuli data and the DHE hypothesis given by Equations (2b) through (2e). All of the data are predicted within -32.5% and +27.5%, and 97% is predicted within -22.5% and +17.5%. These results and an examination of Figures 2 through 6 indicate that the Barnett correlation is superior to the others for predicting steady state CHF data from rod bundles.
Figure 14 gives the error distribution for the Becker correlation[28]. Although the correlation is based on data over the pressure range 31 ≤ P ≤ 1400 psia, and all of the rod bundle data are within this range, the predictions are not as accurate as those of the Macbeth and Barnett correlations. The calculated results for 97% of the data agree with experimental results within ±25%.

Figure 15 presents the error distribution for the Shippingport correlation[33]. Only 38% of the data were predicted within -160% to +45% error. The remaining 62% of the predictions produced errors larger than -160% and are not tabulated in Figure 15. As defined by Equation (7), negative errors indicate an over-prediction of q_{\text{CHF}}, thus any correlation that yields negative errors is undesirable for both design and LOCA analyses. The results given in Figure 15 clearly indicate the necessity of comparing any correlation with data before using it. Figure 15 also indicates the inaccuracies that are possible when a correlation is applied to a geometry other than the geometry on which it is based.

The General Electric Company design curve correlation of Equations (5a) through (5c) was derived by constructing curves under CHF data considered to cover the operating range of BWR's (that is, a lower envelope was determined). That the correlation is conservative (underestimates the CHF), as intended, is indicated by the results shown in Figure 16. However, as given in Figure 11 and the discussion pertaining to the figure, the success of this method of correlation requires data over the entire range of parameters over which the correlation is to be applied. An examination of the predictions of Equations (5a) through (5c) shows that the nonconservative (negative) errors in Figure 16 were obtained for low mass flux data (G < 10^6 lb_m/hr-ft^2).

The results presented in this section have shown that the method of correlation used by Macbeth[27] and Barnett[18,24] produce very accurate CHF correlations that may be extrapolated outside the range of parameters over which they are based with reasonable certainty.
Fig. 14 Error distribution for the Becker CHF correlation.
Fig. 15 Error distribution for the Shippingport CHF correlation.
Fig. 16 Error distribution for the General Electric Company CHF design curve.
5. APPLICABILITY OF THE BARNETT CORRELATION TO LOCA ANALYSES

Critical heat flux correlations are simple curve fits of experimental data. As such they are susceptible to the same errors that are present in all curve fits; that is, extrapolation with respect to any of the parameters involved beyond the range covered by the experiments may lead to erroneous results, and numerical curve fits can be ill-behaved within the range of the parameters over which they were obtained. Previous sections of this study have shown that the CHF data required for accurate loss-of-coolant accident analysis are not in the open literature and that some correlations are better than others. The method of correlation of Macbeth[27] and Barnett[18,24] produces CHF correlations that are both well behaved and very accurate over wide ranges of system parameters. In this section the accuracy of the Barnett correlation for conditions well outside the range of parameters on which it is based will be investigated.

The only high pressure data for unwrapped rods are given by the results for Bundle 36 of Table II. (New data have been received for pressures to 2000 psia, however, they have not been analyzed for accuracy[34]). Figure 17 presents the data and the predicted values of the Barnett correlation of Equation (2). The agreement is good, with a maximum error as defined by Equation (7) of -11%. However, no definite conclusions can be stated on the basis of such limited data.

The available low pressure data have not been systematically obtained and the best method to evaluate the Barnett correlation under these conditions is to give its error distribution as in Figure 18. The ranges of parameters of these data are as follows:

\[ 156 \leq P \leq 725 \text{ psia} \]
\[ 0.03 \times 10^6 \leq G \leq 1.7 \times 10^6 \text{ lb}_m/\text{hr-ft}^2 \]
\[ 32.9 \leq L \leq 174.8 \text{ in.} \]
\[ 0.394 \leq D_r \leq 0.543 \text{ in.} \]
\[ 6.0 \leq \Delta h_{in} \leq 373.0 \text{ Btu/lb}_m \]

The rod lengths and rod diameters of current PWR's and BWR's are within the ranges covered by these data. Also, the data are given for low mass fluxes and low pressures that will occur during a loss-of-coolant accident. However, under the conditions of a loss-of-coolant accident, a steam-water mixture will be present at the core inlet and the data only include subcooled water inlet conditions. The error distribution indicates good agreement between calculated and experimental results but most of the results of the correlation are slightly larger than the experimental data. An examination of the calculations shows that the largest errors are obtained for very low mass fluxes \((G < 0.05 \times 10^6 \text{ lb}_m/\text{hr-ft}^2)\) and low pressures \((P < 400 \text{ psia})\) and that 97% of the data are predicted within -25% to +15% error. Unfortunately, the largest errors are obtained for combinations of low pressure and mass flux and the source of the
error cannot be isolated. The Becker correlation also has been compared with these data and the results are given in Figure 19. Although the agreement is somewhat better than that of the Barnett correlation (97% of the data predicted within -25% to +20% error), the Becker correlation will not converge for the steam-mixture inlet condition[28] that will be present during the loss-of-coolant accident.

An important parameter arising in loss-of-coolant accident analyses that has received little experimental investigation is the two-phase inlet condition. Some data are available for this condition[35]; however, only one series of runs is reported in which the mixture was homogeneous at the test section inlet. Figure 20 shows the experimental results compared with the predictions of the Barnett correlation. The agreement between predicted and measured values is good but again the limited data preclude definite conclusions.
In summary, the Barnett correlation appears to extrapolate beyond the stated range of validity with reasonable certainty. However, lack of data to cover the extensive ranges of pressure, mass flux, and inlet condition occurring during a loss-of-coolant accident does not allow a definitive analysis to be made.
Fig. 19 Error distribution for the Becker CHF correlation at low pressures.
Fig. 20 Comparison of the Barnett CHF correlation with two-phase inlet data.
6. CONCLUSIONS AND RECOMMENDATIONS

Of the rod bundle critical heat flux correlations appearing in the open literature, those of Macbeth[27], Becker[28], and Barnett[18,24] are the most accurate. The Shippingport[39] correlation cannot be applied to rod bundles without incurring significant errors. The General Electric Company design limit CHF correlation[33] underestimates the CHF values if it is not used outside the range of parameters on which it is based. Any correlation before being used should be checked for trends and magnitudes by simple parametric studies and comparisons with data within the range of parameters of interest. The following general recommendations are given:

(1) Correlations obtained from data of internal flow geometries (circular and rectangular conduits) should not be directly applied to external surfaces (annuli and rod bundles) and correlations obtained from annuli data should not be applied directly to rod bundles.

(2) Correlations obtained from uniform axial flux distribution data should not be applied directly to nonuniform flux conditions.

(3) Extrapolation outside the stated range of validity of any correlation should not be considered until a parametric study has been conducted to check its behavior.

Results of this study indicate that the following ranges of variables are those within which the various correlations can be used for loss-of-coolant accident analyses:

(1) The Barnett correlation of Equation (2) should be used for all ranges of pressure, mass flux, and inlet conditions with the exception of pressures below 800 psia if the inlet coolant is subcooled.

(2) For pressures below 800 psia if the inlet is subcooled the Becker correlation[28] should be used.

If conservative results (underestimates of CHF) are necessary the General Electric Company design limit correlation can be used within the range of parameters over which it is based.

The error introduced by the use of the Barnett correlation at high pressures ($P > 1200$ psia), low pressures ($P < 800$ psia), low mass fluxes ($G < 0.05 \times 10^6$ lbm/hr-ft$^2$) and two-phase inlet conditions cannot be estimated because of lack of data for these conditions. Results obtained from loss-of-coolant accident analyses that use CHF correlations outside the range of parameters over which they are based can be considered only estimates.

The accuracy with which the Barnett correlation predicts CHF data on a bundle average basis for the large number of different internal constructions, radial flux variations, heated-to-wetted-perimeter ratios, spacings and pitch,
different laboratories, workers, and methods of detection considered in this study indicates that a multichannel analysis computer code is not necessary to predict CHF at steady state with uniformly heated bundles.

The results of this study apply only to steady state conditions. To determine the effects of rapid pressure-flow transients and nonuniform axial flux distributions on critical heat flux requires extensive further work.
III. COMPARISON OF RELAP3 CALCULATED RESULTS WITH CSE EXPERIMENTAL BLOWDOWN RESULTS
S. E. Jensen

RELAP3 is a computer program that describes the behavior of water-cooled nuclear reactors during postulated accidents such as loss of coolant, pump failure, or power transients. The behavior of the primary cooling system and the reactor is emphasized. The program calculates flows, mass inventories, energy inventories, pressure, temperatures, and qualities along with variables associated with reactor power, reactor heat transfer, or control systems. The program is sufficiently versatile to describe simple hydraulic systems as well as complex reactor systems.

A study was performed to assess and demonstrate the predictive capabilities of the RELAP3 computer code for calculating the blowdown phenomena and to test the applicability of the code for use on systems larger than the semiscale blowdown experiment. To accomplish these objectives, a series of RELAP3 calculations was made, and the calculated results were compared with measured results of blowdown experiments performed with the Containment Systems Experiment (CSE) vessel at Battelle Memorial Institute, Pacific Northwest Laboratory. The purpose was to compare analytical results with experimental results of blowdowns initiated from conditions in the typical PWR range. The scope of the study was limited because experimental data were available only for the bottom break location and complete data were available only for two break sizes, nominal two- and four-inch breaks. RELAP3 calculations were made to compare with CSE Blowdowns B-19, B-19-B, and B-15.

Differences in the initial conditions between some calculated and experimental results were observed. Some of these differences indicate that given initial test conditions may have differed from those actually existing in the CSE system at the time of blowdown. To study the effects of variations in starting conditions and to examine the sensitivity of calculated blowdown data to various starting conditions and input parameters, an additional series of calculations was performed. These calculations were made for comparison with CSE Blowdown B-15. In each problem, one of the following parameters was changed slightly from the original B-15 blowdown calculation: (a) initial temperature, (b) initial mass of liquid (vessel volume), (c) discharge coefficient, (d) bubble rise model, and (e) break location.

1. EXPERIMENT AND ANALYSIS

Experimental blowdowns were performed with the CSE reactor simulator vessel. A sketch of this vessel is shown in Figure 21 [36]. The initial conditions given for the blowdowns considered are shown in Table III. Blowdowns B-19 and B-19-B were identical except that the concentration of dissolved nitrogen was minimized for the B-19-B blowdown. The reason for minimizing nitrogen concentration is discussed later. Run B-15 is for a larger break size, and was chosen because more experimental data were available for this run.
Fig. 21 CSE reactor simulator vessel.
TABLE III
INITIAL CONDITIONS FOR BLOWDOWN TESTS

<table>
<thead>
<tr>
<th>CSE Run</th>
<th>Temperature (°F)</th>
<th>Pressure (psia)</th>
<th>Liquid Mass (lbm)</th>
<th>Percent of fully open area</th>
</tr>
</thead>
<tbody>
<tr>
<td>B-15[36]</td>
<td>560</td>
<td>2185</td>
<td>6700</td>
<td>4</td>
</tr>
<tr>
<td>B-19[37]</td>
<td>600</td>
<td>2135</td>
<td>6420</td>
<td>2</td>
</tr>
<tr>
<td>B-19-B[37]</td>
<td>603</td>
<td>2115</td>
<td>6450</td>
<td>2</td>
</tr>
</tbody>
</table>

The RELAP3 computer code was used to obtain the analytical comparisons for these blowdowns. The simple three-volume model shown in Figure 22 was input to the RELAP3 code to describe the CSE vessel. Blowdowns B-19 and B-19-B were performed with a simulated core plate in the vessel. This core plate was described in the analytical model by Junction 1 shown in Figure 22. Blowdown B-15 was performed without the core plate, and the calculations for comparison with Blowdown B-15 used the geometrical model of Figure 22 except that the flow area of Junction 1 was increased to the internal vessel area and the friction losses were reduced. The discharge coefficient used for the calculations was obtained from the following empirical expression[38]:

\[ C_{CSEM} = 0.287 R^{-0.165} \]  \hspace{1cm} (1)

where

\[ C_{CSEM} = \text{flow coefficient to be applied to the Moody model of mass velocity based on CSE data} \]
\[ R = \frac{\text{area of break}}{\text{area of vessel}} \]

A bubble rise velocity of 3.0 ft/sec and a quality gradient coefficient of 0.8 were also input to the calculations. The initial conditions for the computer calculations are as given in Table III except for the initial temperature for Blowdown B-15. The 560°F temperature given for Blowdown B-15 is apparently in error because the available fluid temperature data indicate initial fluid temperatures of 610 to 620°F. The initial measured temperature (620°F) at the desired location, was chosen for this calculation. Liquid mass is calculated from input volumes and conditions and is not input as such to the RELAP3 code.
Fig. 22 CSE model for RELAP3 calculations.

Volume 1
106 ft$^3$

Volume 2
46 ft$^3$

Volume 3
1.422 ft$^3$

Junction 1
2.0625 ft$^2$

Junction 2
0.253 ft$^2$

Junction 3
Break Area
2. DISCUSSION OF RESULTS

Comparison plots and discussions of calculated and experimental data for the three blowdown cases considered are given in Sections III-2.4 through -2.5. Section III-2.4 presents a parametric study of blowdown and Section III-2.5 discusses results obtained for the homogeneous and phase separation blowdown models.

2.1 Blowdown B-19 Comparisons

Figures 23 through 25 show experimental versus analytical results for pressure, fluid temperature, and liquid level for Blowdown B-19. Experimental pressures and temperatures were measured at Nozzle H shown in Figure 21 and are compared with average values for Volume 1 of Figure 22 as calculated by RELAP3. The experimental liquid level shown in Figure 25 was obtained with a time domain reflectometer and is compared with a liquid level based on the RELAP3 calculated liquid masses remaining for the prevailing conditions.

The experimentally measured pressure shown in Figure 23 indicates a subcooled blowdown of long duration. That is, for subcooled blowdown the measured pressure exceeds the saturation pressure for the existing temperature conditions for some time during the blowdown. Measured and calculated fluid temperatures are given in Figure 24, and excellent agreement between measured and calculated temperatures is shown. The RELAP3 calculated pressure after an initial rapid decompression corresponds to saturation conditions for the calculated temperatures. Because of the excellent analytical-experimental temperature agreement, the RELAP3 calculated saturation pressure closely approximates the saturation pressure as a function of time in the reactor vessel. Figure 23 shows that the measured pressure does not approach the saturation pressure as described by the RELAP3 calculated pressure curve until about 40 seconds after rupture. Nitrogen solubility is believed to be the cause of this long duration subcooled blowdown period. RELAP3 does not account for dissolved nitrogen and predicts saturated conditions to occur in fractions of a second. Consequently, agreement between calculated and experimental pressure results is poor until experimental results approach saturated conditions. After 30 seconds, agreement exists between calculated and experimental results.

The time at which the mixture level reaches the nozzle and a high quality blowdown begins is indicated by the knee in the pressure curve at about 52 seconds. The calculated time of the transition to high quality blowdown agrees well with the experimental results. Reasonable agreement exists throughout the high quality blowdown except perhaps for times later in the blowdown at which the calculated pressure is higher than the experimental data. A possible reason for the higher calculated pressure during the latter portion of blowdown is discussed later.

The comparison of calculated versus experimental liquid level is shown in Figure 25. Several problems arise in using the liquid level results from the time domain reflectometer. First, the initial measured water height exceeds the internal vessel height. Second, an abrupt liquid level drop occurs after 35 to 36 seconds following initiation of blowdown. This liquid drop was attributed to formation of steam below the dummy core plate while liquid water remained above the core plate. Thus, both the volume above the dummy core plate and the
volume below the core plate would contain separate liquid and vapor regions, and both volumes would have a liquid or mixture level. Initially, the time domain reflectometer would indicate the upper volume mixture level; however, when the upper volume liquid was exhausted, the abrupt drop of liquid level to that in the lower volume would occur. Third, the experimental data show a rise in liquid level during the high quality transient region of blowdown which indicates failure of the experimental measuring device or a frothing phenomenon at the bottom of the vessel. If this interpretation of the experimental data is correct, the calculated results can only be expected to agree with the experimental results during the time span (36 to 52 seconds) when the liquid level is below the dummy core plate and prior to the high quality transition. Figure 25 shows good agreement between experimental and analytical results for this time interval.
Experimental Results

Temperature at Nozzle H

RELAP3 Calculated Temperature

Volume above Core Plate

Fig. 24 CSE Blowdown B-19 temperature comparison.

Fig. 25 CSE Blowdown B-19 liquid remaining comparison.
2.2 Blowdown B-19-B Comparisons

Comparisons of calculated and experimental results for Blowdown B-19-B, which was essentially a duplicate of Blowdown B-19 with nitrogen concentration minimized, are shown in Figures 26 through 28. Figure 26 shows the comparison of pressure data. A long-duration subcooled blowdown period was not observed for the B-19-B test, and saturated conditions were reached within about 0.5 second as shown in Figure 26. During saturated blowdown calculated pressures for the 603°F initial temperature are about 100 psi higher than measured values. When saturated blowdown begins, RELAP3 predicts a pressure corresponding to saturation conditions at the initial temperature (603°F). Experimental results show a lower sustained pressure indicating that a lower saturation temperature existed in the experiment. The existence of a saturation temperature lower than 603°F was confirmed by the experimenters who recommended an initial saturation temperature of 590°F.[39]. The saturation pressure for 603°F is 1578 psia and for 590°F is 1431.5 psia. Thus, a 1.3°F temperature difference can cause a 146.5 psi difference in saturation pressure for these temperatures. An additional calculation was performed with an initial fluid temperature of 590°F. The results of this calculation also are shown in Figures 26 through 28. Pressures calculated for this initial temperature, as shown in Figure 26, are in better agreement with the measured results. RELAP3 calculated results for Blowdowns B-19 and B-19-B differ slightly because of the slight differences in initial conditions. Experimental results differ significantly not only because of dissolved nitrogen but because of the different initial conditions. Saturated blowdown pressures are generally lower for Blowdowns B-19-B than for Blowdown B-19, and the transition to high quality occurs later at about 60 seconds instead of 52 seconds. Agreement between experimental and analytical pressure results for Blowdown B-19-B is not as good as desired; however, the calculations with an initial temperature of 590°F gave better agreement. Calculated pressure results agree with experimental results within the capability of Blowdown B-19-B to reproduce Blowdown B-19 results, as can be seen by comparing the data of Figures 23 and 26.

Figure 27 shows calculated and experimental fluid temperatures for Blowdown B-19-B. As in the case of Blowdown B-19, Blowdown B-19-B calculated (603°F) and experimental temperatures are in excellent agreement until the calculated transition to high quality occurs. Results of the RELAP3 calculation starting from 590°F differ from the measured results because of the different starting conditions but, as shown in Figure 27, better agreement was obtained for the time of high quality transition. Temperature declines during the high quality blowdown regime are similar for calculated and experimental results, except for the time difference at which the high quality transition occurs. This difference in the time at which transition occurs probably results from the higher saturation pressures and lower mass inventory for the calculated cases.

Calculated and measured water remaining data are shown in Figure 28. The experimental curve represents the locus of approximate midpoints of oscillatory measured data. These experimental results are probably not accurate in magnitude because the starting point shows the liquid mass to exceed 7000 lbm whereas the system as described can contain only about 6500 lbm at the prevailing conditions. Also, the minimum water remaining shown experimentally would exceed the level of the outlet nozzle. In view of these inconsistencies, the experimental-analytical comparison for the curves of Figure 28 do not
provide a valid assessment of RELAP3 capability to predict water remaining. However, the general shapes of the two water remaining curves are similar. As shown in Figure 28, better agreement of water remaining results with experimental data resulted from the calculation with the 590°F initial temperature.
Experimental Results

Temperature at Nozzle R

- RELAP3 Calculated Temperature
Volume above Core Plate (603 °F)

- RELAP3 Calculated Temperature
Volume above Core Plate (590 °F)

Fig. 27 CSE Blowdown B-19-B temperature comparison.

Fig. 28 CSE Blowdown B-19-B liquid remaining comparison.
2.3 Blowdown B-15 Comparisons

Calculated and measured data for Blowdown B-15 are shown in Figures 29 through 31. Blowdown B-15 was conducted with a nominal four-inch break (3.438 inches diameter) which corresponds to 25.4% of the fully open pipe area. Nitrogen concentration was not minimized for this run, and the long-duration subcooled blowdown was observed. The measured pressure approached saturated conditions for the measured temperature at about 7.5 seconds. Pressure results are shown in Figure 29, temperature results in Figure 30, and water remaining results in Figure 31. General agreement between calculated and measured results is shown for all three quantities; however, in some cases significant differences between calculated and experimental values exist. The maximum difference between calculated and experimental temperatures is about 20°F, and the maximum pressure difference is about 160 psi. The maximum differences occur at the same time when saturation conditions are governing and pressure and temperature are intimately related. The reasons for the difference between calculated and observed saturation conditions are not clear. The source of the differences may be experimental problems, the capability of the RELAP3 code to predict blowdown, or a combination of both.

Another potential source of difficulty should be noted. The release of dissolved nitrogen gas which apparently raises the pressure above saturation and causes the long duration of subcooled blowdown would also alter the temperature at which boiling occurs. With nitrogen present, the boiling condition requires a higher liquid temperature than saturated equilibrium conditions. In the actual experiment, energy transfer may not have been rapid enough to achieve the thermal equilibrium required for saturated conditions. In which case, a blowdown governed by boiling conditions during which the liquid and gas phases in any volume are not in thermal equilibrium may have taken place. Calculational models such as RELAP3 would not handle this situation.

2.4 Blowdown B-15 Parametric Study

As noted previously, the possibility exists that some of the observed differences between experimental and calculated data may have been caused by slight differences in initial conditions or input parameters. A parametric study was made to examine the effects of slight changes in input parameters on calculated blowdown results for Blowdown B-15. The initial calculation of Blowdown B-15 was taken as a reference case and three additional cases were run. For the first case, the initial temperature was reduced from the 620°F used for the initial calculation to 610°F. For the second case, Volume 1 of the calculational model of Figure 12 was increased from 106 ft³ to about 119 ft³ to give an initial water mass of about 6700 lbm. For the third case, a discharge coefficient of 0.69 was used. This value of discharge coefficient was given for CSE[38] for the nominal four-inch break but differs from the 0.653 value calculated using Equation (1). The results of this study are shown in Figures 32 through 34. The figures show pressure data, temperature data, and mass remaining data, respectively. In all of the figures, the curves numbered 1 and 2 are replots of the experimental and calculated reference case data. Curves 3 through 5 show results from the three parametric study calculations for the conditions noted on the figures.
Experimental Results
Pressure at Nozzle H

RELAP3 Calculated Pressure
Volume above Core Plate

High Quality Transition

Fig. 29 CSE Blowdown B-15 pressure comparison.
Fig. 30 CSE Blowdown B-15 temperature comparison.
Experimental Results

Calculated from RELAP3 Results
(sum of liquid and steam masses in all three volumes)

Fig. 31 CSE Blowdown B-15 liquid remaining comparison.
Fig. 32 CSE Blowdown B-15 parametric study -- pressure results.
Experimental Results

Conditions for RELAP3 Calculations

<table>
<thead>
<tr>
<th>Temperature (°F)</th>
<th>Liquid Mass (lbm)</th>
<th>Discharge Coefficient</th>
</tr>
</thead>
<tbody>
<tr>
<td>620</td>
<td>6198</td>
<td>0.653</td>
</tr>
<tr>
<td>610</td>
<td>6368</td>
<td>0.653</td>
</tr>
<tr>
<td>620</td>
<td>6737</td>
<td>0.653</td>
</tr>
<tr>
<td>620</td>
<td>6198</td>
<td>0.690</td>
</tr>
</tbody>
</table>

Fig. 33 CSE Blowdown B-15 parametric study -- temperature results.
Experimental Results

Conditions for RELAP3 Calculations

<table>
<thead>
<tr>
<th>Temperature (°F)</th>
<th>Liquid Mass (lb)</th>
<th>Discharge Coefficient</th>
</tr>
</thead>
<tbody>
<tr>
<td>620</td>
<td>6198</td>
<td>0.653</td>
</tr>
<tr>
<td>610</td>
<td>6368</td>
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</tr>
<tr>
<td>620</td>
<td>6198</td>
<td>0.690</td>
</tr>
</tbody>
</table>

Fig. 34 CSE Blowdown B-15 parametric study -- liquid remaining results.
These results show that decreasing the initial temperature lowers the saturation pressure, increases the initial mass inventory slightly, and prolongs the time duration of blowdown. Increasing the initial liquid mass also prolongs the calculated duration of blowdown and increases pressure, fluid temperatures, and mass inventory as functions of time. Increasing the discharge coefficient shortens the blowdown period and decreases all three parameters plotted as functions of time. The comparison of the calculated curves with the reference case indicates the sensitivity of the calculated results to the parameters varied.

2.5 Blowdown B-15 Phase Separation Model Study

Additional calculations were made to examine the effects of changing the separation or bubble rise model. An additional RELAP3 calculation of Blowdown B-15 was made with a homogeneous blowdown model. The results of these calculations are shown along with replots of initial calculated results and the experimental results of Blowdown B-15 in Figures 35 through 37. For the saturated blowdown regime prior to the high quality transition, better agreement with experimental temperature and pressure data was obtained with the homogeneous model than with the separation model. After the high quality transition, the homogeneous model is inadequate for describing the blowdown process.

One additional possibility for improving agreement between analytical and experimental results was examined. As was noted earlier, following transition to high quality blowdown, the calculated pressures and temperatures tend to remain higher than the experimental values. Also, the measured liquid mass remaining reaches zero whereas the calculations show that a finite liquid mass remains. Indications are that possibly in the experiment all the liquid remaining in the vessel below the outlet nozzle was entrained and expelled from the vessel by flowing steam. Complete water expulsion could occur because the nozzle is close to the vessel bottom and agitation by boiling on the hot vessel surface would aid entrainment. If water is completely removed from the vessel, the latter portion of the high quality blowdown would be essentially a single phase blowdown of steam remaining in the vessel with no addition of mass to the steam. However, if as calculated by RELAP3, saturated liquid remained in the system below the outlet nozzle level, the process could still be a single phase blowdown of steam; but, the steam loss would be partially replaced by vaporization of the liquid to maintain saturation conditions. Higher pressures and temperatures during high quality blowdown result from maintaining saturation conditions when liquid remains in the system than would occur if all the liquid were expelled. This fact was demonstrated analytically for Blowdown B-15 by comparison of the case in which liquid was calculated to remain in the system to an additional case in which all the liquid was expelled. The latter case study was performed by a RELAP3 calculation for CSE Blowdown B-15 in which the outlet nozzle location was moved to the bottom of the vessel. This vessel configuration forces all the liquid to be expelled from the vessel and increases only slightly the time at which the high quality transition occurs. The calculated results for this model are shown along with calculated results from the original model and experimental results in Figures 38 through 40.
Fig. 35 CSE Blowdown B-15 homogeneous versus separation model comparison -- pressure results.
Experimental Results
Temperature at Nozzle H

RELAP3 Calculated Temperature
Separation Model
Bubble Velocity = 3.0 ft/sec
Quality Gradient Coefficient = 0.8
Discharge Coefficient = 0.653

RELAP3 Calculated Temperature
Homogeneous Model

Fig. 36 CSE Blowdown B-15 homogeneous versus separation model comparison — temperature results.
Fig. 37 CSF Blowdown B-15 homogeneous versus separation model comparison -- liquid remaining results.
Fig. 38 CSE Blowdown B-15 effect of complete water ejection -- pressure results.
Experimental Results

Temperature at Nozzle H

RELAP3 Calculated Temperature
(break 0.875 ft from vessel bottom)

RELAP3 Calculated Temperature
(break at vessel bottom)

Fig. 39 CSE Blowdown B-15 effect of complete water ejection -- temperature results.
Experimental Results

Calculated from RELAP3 Results
(break 0.875 ft from vessel bottom)

Calculated from RELAP3 Results
(break at vessel bottom)

Fig. 40 CSE Blowdown B-15 effect of complete water ejection -- liquid remaining results.

Only the high quality blowdown regime is affected. The shape of the calculated curves for the bottommost break location is in better agreement with experimental results than the case in which saturated liquid remains in the vessel, thus, providing further evidence that all liquid was expelled from the vessel.

Because of the apparent entrainment problem, the capabilities of the RELAP3 code to predict the high quality blowdown in which water remains in the system could not be assessed by comparison with the available CSE
The complete ejection of liquid will probably not apply to large power reactor systems of current design because recirculating line inlet and outlet locations are not near the bottom of the reactor vessel. Thus for the large plants, saturated liquid will remain in the vessel and the RELAP3 models are expected to apply.

3. CONCLUSIONS

Comparisons of RELAP3 calculated results with those available experimental data which appear to be applicable indicate that RELAP3 predicts CSE blowdowns reasonably well. However, because of the limited ranges of break size and location for which sufficient CSE experimental data are available for comparison, drawing general conclusions about the capabilities of the RELAP3 code to predict CSE vessel blowdowns would be premature at this time. Differences between calculated and experimentally determined pressures occur because of the presence of a gas pressure, other than that from steam, which acts on the fluid during blowdown and because of differences between the initial temperatures given for the experiment and the initial temperatures indicated by pressure results. A change of 13°F in initial temperature changed the saturation pressure by about 145 psi.

During saturated blowdown, the pressure and temperature are closely related by saturation conditions; thus, if reasonable agreement exists between calculated and experimental pressures, excellent agreement between calculated and measured fluid temperatures is to be expected. Saturation conditions are determined initially by the existing fluid temperature in the vessel. Because of the sensitivity of saturation pressure with temperature in this temperature range, the initial fluid temperature must be known accurately if agreement between experimental and calculated blowdown pressure transients is to be obtained.

Blowdown transients are also influenced by changes in initial fluid mass and discharge coefficient. For the bottom blowdowns considered during the saturated blowdown period prior to high quality transition, better experimental-analytical agreement was obtained through use of a homogeneous fluid blowdown model than with a separation model using a quality gradient coefficient of 0.8 and a bubble rise velocity of 3 ft/sec. These results suggest that adjustments in the separation model may improve the calculated data. A separation model is essential for predicting liquid remaining, mixture level, and time at which the mixture level reaches the discharge nozzle and high quality transition occurs. The homogeneous model does not consider these parameters.

Experimental and analytical evidence indicated that all the liquid may have been expelled during some or all of the blowdown experiments studied. If all the liquid was ejected, the latter portion of the high quality blowdown period would be a single-phase gas expansion whereas if liquid remained in the system, the steam ejected would be partially replaced by vaporization of the remaining liquid. The latter condition increases temperatures and pressures as a function of time and is representative of expected conditions for large power reactor systems. The RELAP3 calculational models are
designed to handle the system with liquid remaining; however, the capabilities of the code to calculate this portion of blowdown could not be assessed by comparison with experimental data because of the apparent complete ejection of water during the experiment.
IV. CAROLINAS VIRGINIA TUBE REACTOR (CVTR) IN-PLANT TESTS

J. A. Norberg, J. E. Bingham, R. C. Schmitt, D. A. Waddoups

A series of nuclear reactor containment response tests was performed at the decommissioned Carolinas Virginia Tube Reactor (CVTR) facility at Parr, South Carolina, as part of the Atomic Energy Commission's safety program. These tests were performed jointly by Idaho Nuclear Corporation and the Carolinas Virginia Nuclear Power Associates (CVNPA), Inc. Results of this project provided realistic information on containment system behavior that can be used to evaluate and improve analytical and experimental techniques, thus allowing increased confidence in applying such techniques to safety analyses of nuclear power plants.

1. OBJECTIVES

The primary objectives of this project were (a) to obtain data on the pressure-temperature response of the CVTR containment system to simulated design basis accident (DBA) conditions and to compare these data with response predictions obtained through use of current analytical techniques; (b) to assess the effectiveness of a containment pressure reduction spray system; (c) to determine structural dynamic vibrational properties for comparison with predictions acquired from state-of-the-art seismic analysis, and (d) to evaluate procedures, instrumentation, and assumptions commonly utilized in leakage rate testing of power reactor containments.

Secondary objectives of the program were (a) to measure the containment leakage rate as a function of pressure and temperature and (b) to evaluate the effect of DBA conditions on leakage rate, vessel strain, and engineered safety system operation.

The test project consisted of three major test efforts:

(1) Tests in which two independent techniques were used to measure the vibrational characteristics of the containment structure

(2) Leakage rate tests of the containment as a function of pressure and temperature

(3) Steam injection tests of the containment in which design basis accident (loss-of-coolant) conditions were simulated.

The following sections present a description of the CVTR facility and discussions of the test series and results.
2. CVTR FACILITY DESCRIPTION

The CVTR facility, owned by CVNPA, a corporation formed by four Carolina-area private utilities[a], included a power demonstration prototype of a full-scale, heavy-water-moderated and -cooled pressure tube reactor. The reactor was shut down in late 1967 after having successfully operated for about four years. Since the CVTR test project concerned only the containment system the reactor is not discussed in the following facility description. The containment structure and the data acquisition systems, common to all tests, are described in this section; other instrumentation is presented with the appropriate test description. A complete description of the CVTR facility is contained in CVNA-90, Final Hazards Summary Report[40].

2.1 CVTR Containment Structure

The CVTR containment structure was chosen for this test series because (a) it is similar in construction to newer power plant containment structures; (b) the facility was being decommissioned and tests could be conducted that would not be practical in an operating plant; and (c) a source of steam was available for the DBA tests from the adjacent Parr station.

The CVTR containment, shown in Figure 41, is a reinforced concrete right vertical cylindrical structure with a flat base and a hemispherical dome. Overall interior dimensions are a 57-foot 11-1/2-inch ID and a 114-foot 3-inch height from the basement floor to the inside top of the dome. Pressure retention is provided by the 2-foot-thick reinforced concrete walls of the cylinder section and the 1/2-inch-thick steel dome which is covered with 20-1/2 inches of concrete for radiation shielding. A 5-foot 9-inch-thick reinforced concrete foundation mat supports the containment structure. The top of the foundation mat and the vertical cylinder wall are lined with 1/4-inch-thick steel plates.

A 4-foot 6-inch concrete fill covers the liner plate above the foundation mat to form the basement floor. The liner plates are welded to vertical and horizontal support plates embedded in the concrete and to the steel dome support skirt to make the containment volume vapor tight. Entrance to the containment is provided by a 13-foot-diameter circular equipment hatch with a 7-foot-diameter personnel door and by a 2-foot 9-inch-diameter personnel escape hatch.

The containment structure housed the reactor and primary system, the steam generator, and various auxiliary systems and components. The structure is divided into two main volumes by the operating floor. For purposes of this report the lower volume is subdivided into two parts. The resulting three regions (operating, intermediate, and basement) are shown in Figure 41. The total free volume of the containment system is approximately 227,000 ft³. The structure is designed to withstand an internal pressure of 21 psig at a temperature of 215°F.

2.2 Data Acquisition Systems

The principal data acquisition equipment consisted of a digital data acquisition system (slow scan), an analog data multiplexing system (fast scan), an oscillograph system, and a digital voltmeter with a digital printer. Figure 42 shows the data acquisition systems.
2.21 Digital Data Acquisition System (Slow Scan). The digital data acquisition system provided the capabilities for scanning and recording low-level dc signals from strain gage bridges and other transducers. The output signals from the transducers or the strain gage bridges were sequentially scanned, and the analog output was converted to digital form by a digital voltmeter. A visual digital display of the input signal was provided by the voltmeter that also provided an output to the digital recorder (paper-tape printer) or to a paper-tape perforator. A time code generator was connected to the system so that time was recorded digitally once during each scan cycle. Data were recorded on this system at the rate of one point about every two seconds.

2.22 Analog Data Multiplexing System (Fast Scan). The analog data multiplexing system provided the capabilities for scanning, conditioning, and recording up to 100 analog input signals. Each signal was conditioned and recorded about three times per second. The input signals were multiplexed in groups of 10 channels with respect to the output of the multiplexer; that is, each of the 10 multiplexed channels contained 10 input signals. Each of the 10 multiplexer output channels was conditioned by a separate low level amplifier. The 10 amplified outputs were recorded on 10 channels of a 14-channel magnetic tape recorder. Channel 14 of the tape recorder was used for channel identification and time synchronization with the multiplier inputs. One of the remaining three channels was used for recording time trace information.
2.23 Oscillograph System. The oscillograph system provided capabilities for signal conditioning and continuous recording of up to 36 channels of low-level signals. Low-level signals from suitable transducers were connected to the input terminals. These signals were amplified by the low-level dc amplifiers (which also provide galvanometer protection) and fed into the oscillograph. The oscillograph provided a permanent analog record of the input signals. A low-level calibration source was used to calibrate the system prior to each test.

2.24 Digital Voltmeter System. The digital voltmeter with the digital printer provided capability for recording in digital form one channel of data at the rate of five times per second. The system accepted low-level millivolt signals directly and was equipped with an ohms-to-volts converter so that it could be used as a readout for resistive devices such as the resistance thermometers. A manual-switch timer unit permitted up to 15 channels of resistance thermometers to be manually scanned and the data recorded digitally.

3. STRUCTURAL VIBRATION TESTS

An important consideration in the safety evaluation of nuclear plants is the capability of the structure and equipment to withstand loading forces induced by earthquake excitation. Little experimental information for nuclear structures has been available to check the adequacy of design or of analytical models used to predict seismic response. The decommissioned CVTR containment system provided an opportunity to evaluate the dynamic structural properties of a containment and to obtain meaningful comparisons of experimental results with predicted values from state-of-the-art analysis. Characteristic frequencies of vibration, mode shapes, and damping values were determined and compared for two different experimental techniques -- forced vibration (shaker) by the University of California[41], Los Angeles, and Ambient Vibration Survey by Earth Sciences, A Teledyne Company[42].

Determination of the characteristic vibration frequencies, mode shapes, damping, and effective masses of the CVTR containment was a general objective. The specific objectives of the two vibration studies were

(1) Forced Vibration (UCLA): to experimentally determine the vibrational characteristics of the containment-primary system by using the forced excitation (shaker) technique.

(2) Ambient Vibration Survey (Earth Sciences): to demonstrate the ability to determine the vibrational characteristics of a containment-primary system by using the Ambient Vibration Survey technique and to compare the experimental results with values predicted by a state-of-the-art, lumped-parameter, mathematical model.

This section briefly compares the results obtained from the two techniques. A more extensive comparison has been documented[43].
3.1 Test Techniques

The shaker technique is the more common technique for experimental determination of structural vibration characteristics. Although this technique generates greater response amplitudes than the Ambient Vibration Survey technique, the response amplitude ($\leq 10^{-2}$ g) is low compared to strong motion earthquake excitation. The shaker force level was changed and some aspects of damping as a function of increases in force level were examined during the CVTR tests. Also, the shaker frequencies were varied up to about 60 Hz to determine second or higher order modes of vibration.

The Ambient Vibration Survey technique, which employs ultrasensitive ($10^{-8}$ g) seismometers, is used to detect the response of structures to natural excitation such as from microtremors and wind. Assumptions basic to the technique are that the input forces are a stationary random process and that the frequency spectrum is reasonably flat, although possibly only for short durations. Thus a structure subject to this input is expected to be excited in all the natural modes of vibration in their normal relative amplitudes. Time series analysis is employed to reduce the recorded response data to characteristic frequencies, mode shapes, damping, and effective masses which could provide the observed response.

3.2 Discussion of Results

Decisive agreement between the results from the two separate investigations was not achieved. Nevertheless, a comparison of the results and an evaluation of the differences is valuable both to understanding structural behavior and to determining whether additional investigations are needed. Actually, the minimal number of points of similarity between the results from the two test efforts is possibly one of the most important products of this work. This lack of similarity is a measure of the uncertainties in this subject and of the need for both additional experimental and analytical investigations.

Both experimental investigators indicated relatively large amplitudes of motion for the internal structure relative to the stiff containment walls. This motion results from the reduced lateral restraint for the top of the internal structures. The ability to detect this type of design feature emphasizes the possible value of such measurements for detecting components with excessive response in as-built systems. The addition of antiseismic devices, such as snubbers, to pipes or other components could be valuable corrective action resulting from such testing.

Both groups also noted that the vibrational analysis of the containment system was much more complex than that of previously tested high-rise structures. Both the interpretation of the measured response and analytical modeling of CVTR were more complicated than expected or previously encountered.

The two investigations indicated different damping values: 5 to 10% of critical for the shaker tests and 0.5% of critical for the Ambient Vibration Survey.
Some differences in the results from the two methods can be attributed to differences in excitation levels. More than one mode is normally excited by any particular type or level of practical test excitation and changes in the apparent natural response (a superposition of many modes) would be expected to be different for different types of excitation. The higher excitation level used by UCLA and consequent greater displacement, about one thousandth of an inch, as compared to essentially no displacement for the Ambient Vibration Survey, may have resulted in a test of a structure constrained near ground level whereas the Ambient Vibration Survey may have tested a structure constrained at the base only. If indeed an effectively different system was tested in each case, both results may be correct and complementary.

3.3 Conclusions of the Vibration Tests

Several conclusions can be drawn from the CVTR dynamic investigations:

(1) Quantitative agreement between results from the two techniques was not achieved.

(2) The different levels of excitation may have resulted in different levels of soil-containment interaction. As a result, an effectively different system may have been tested in each case such that both results may be correct.

(3) Since the test results were not conclusive, both techniques, as well as others, should be investigated further to provide a verified in-plant proof testing technique.

(4) The CVTR structure, which is quite representative of current nuclear plant structures, was far more complex to analyze than expected.

(5) The results of the CVTR tests and analysis indicate that the seismic analyses of other plants may require further evaluation.

(6) For complex structures, the results strongly suggest the need for additional investigation to assess experimental techniques for establishing characteristic vibrational features and to develop criteria that will guarantee an adequate basis for analytical modeling. Additional test experience is needed to develop a better understanding of how to incorporate material properties and design features.

4. SIMULATED DESIGN BASIS (DBA) TESTS

Simulated DBA tests were performed in the CVTR containment to provide experimental information for use in developing and evaluating analytical methods for safety analyses of nuclear power plants. The specific objectives of the simulated DBA tests were:
(1) To obtain experimental data on pressure and temperature for evaluating the capability of typical computer codes to predict the response of a containment atmosphere subjected to DBA conditions

(2) To determine the effectiveness of a pressure-reduction containment spray system

(3) To determine the gross effects of DBA conditions, exclusive of radiation, on the CVTR containment integrity.

The temperature-pressure history of the CVTR containment atmosphere was determined experimentally during simulated DBA conditions both with and without pressure reduction sprays. Also, during the tests, time-dependent heat transfer data (including convective behavior) were obtained at various locations throughout the containment structure.

The containment atmosphere response observed during these DBA tests was compared with the response calculated by computer codes for the test conditions. Comparisons also were made between the experimentally determined and predicted heat transfer characteristics.

Pretest and posttest visual observations and photographic records were made both inside and outside the containment structure to help determine gross effects on the containment integrity caused by the DBA tests. Quantitative evaluation of the overall effects of the DBA testing on the containment leakage rate was determined by performing leakage rate measurements of the containment system both before and after the DBA tests.

The following sections briefly describe the test equipment and instrumentation, test results, and conclusions reached from these results. A more detailed description of the instrumentation and a preliminary evaluation of the results have been documented[44].

4.1 CVTR Steam Injection and Pressure Reduction Systems for DBA Tests

Steam for DBA tests was obtained from the Parr Station Steam Plant, a South Carolina Electric and Gas Company coal-fired electric generating station, located about 500 feet from the CVTR. An existing 10-inch steam line, which originally supplied steam from the nuclear plant through an oil-fired superheater to the Parr Station header, was modified to permit reverse flow. The Parr header provided approximately 325,000 lb/hr of superheated steam at 725°F and 400 psig. The steam injection system included a desuperheater to adjust the injection steam to near saturated conditions.

To perform a DBA test, two of the Parr Station boilers were brought to full capacity by venting desuperheated steam to the atmosphere through the cylinder-operated vent valve located adjacent to the CVTR containment. When the boilers reached full capacity and steam flow to the atmosphere stabilized, the cylinder-operated charge valve was opened and the vent valve closed to direct total steam flow into the containment. Steam injection was terminated by reversing the valve operations. This procedure allowed close
control of the steam conditions and pressure rise in the containment and permitted controlled startup and shutdown of the steam plant boilers.

A containment pressure reduction water spray system was installed in the CVTR for the DBA tests. The geometrical arrangement of the spray header and nozzles was patterned after the Connecticut Yankee system. Spray nozzles were selected to produce a droplet size range of 400 to 1400 microns based on calculated pressure differentials and recommendations of the manufacturer.

Spray flow rates of 290 and 500 gpm were used. These flow rates were achieved by changing the total number of nozzles in the spray header. Containment pressure reduction spray flow was initiated about 30 seconds following termination of steam injection and was stopped after about 12 minutes of operation. No provisions were made for spray water recirculation and all water added to the containment remained there until completion of a test.

4.2 DBA Test Instrumentation and Measurements

A primary objective of the simulated DBA tests was to measure the containment response to rapid steam injection and to relate this experimental response to that calculated by current analytical techniques. To accomplish this objective, the following measurements were made:

1. Containment pressure
2. Containment atmospheric temperature
3. Containment liner surface temperature
4. Condensate formation -- rate and gross
5. Heat flow at the containment liner surface
6. Temperature profile through the containment wall
7. Containment liner stress
8. Air movement within the containment
9. Injected steam quality and quantity
10. Spray system flow rate and water temperature.

4.2.1 Containment Pressure. The time-dependent pressure response of the containment atmosphere was measured with seven 0 to 25 psig fast response pressure transducers. Four of the transducers were located at various elevations within the containment, one transducer was positioned in a pipe which was external to the containment, but which was open to the containment atmosphere, and the remaining two transducers were located such that the pressure in the reactor header cavity and the refueling canal was measured. Figure 43 shows the locations of these transducers.
Containment pressure indication was also obtained from a 0 to 30 psig Heise gage which was located in the reactor control room. The Heise gage sampled the vapor containment atmosphere by means of 1/2-inch copper tubing. As the steam was injected (also during pressure decay), gage pressure was recorded as a function of time and compared with that obtained from the pressure transducers. The Heise gage containment pressure indication also served as the basis for manual control of steam injection into the containment.

4.22 Containment Atmosphere and Surface Temperatures. The temperature of the containment atmosphere was measured by thermocouples and resistance bulb thermometers; however, since the time response of a resistance bulb
thermometer is much slower than that of a thermocouple, resistance bulb thermometers were used mainly for spot checking the air temperatures.

Thirty-six Chromel-Alumel thermocouples and 15 platinum resistance thermometers were located throughout the vapor containment atmosphere to provide temperature information from each of the three major containment regions. Ten of the thermocouples were positioned to obtain temperature profile data adjacent to the containment liner surface.

Twenty-two Chromel-Alumel thermocouples were positioned at selected locations on the containment liner surface and on the surfaces of interior concrete structural sections.

4.23 Condensation Heat Transfer Measuring System. Data were taken at several locations to determine the condensation rate (and heat transfer) on the containment steel liner. During the DBA tests, time-dependent measurements were made of (a) the condensate collected from various containment wall areas, (b) the temperature profile through the containment wall, and (c) heat flow at the containment liner surface.

Selected areas on the vapor containment liner were defined by outlining them with a bead of Dow Corning Compound III, and the condensate from each area was collected in a catch bottle. Determination of total condensate formation was obtained by measuring the amount of water that had been collected in the bottles after each test. The rate of condensate formation was obtained by suspending five of the catch bottles from specially designed gage load cells from which the weight of the water collected was recorded as a function of time.

Two heat assemblies were installed in the containment wall. These assemblies consisted of 10-inch-diameter, 1/4-inch-thick carbon steel plate with special thermocouples embedded across the thickness of the plate. A 10-inch-diameter section of the liner was removed and a one-inch-diameter hole was drilled through the concrete containment wall. The 10-inch plate containing the thermocouples was welded in the liner, and a concrete plug with thermocouples along its length was installed in the hole through the containment wall. Figure 44 is a schematic diagram of the temperature profile measuring scheme.

To aid in the determination of heat transfer processes, four special heat flow devices were installed on the containment liner surface in the operating region. The detectors used were bidirectional calorimeter-type devices and provided an output signal of 1 mV per Btu/ft²-hr, which was recorded on the analog data multiplexing system.

4.24 Containment Liner Stress. Strain measurements on the steel liner were made at 83 locations with rosette strain gages having three components. A bonding and water proofing technique developed at the NRTS was used to attach the gages to the liner to facilitate their operation in the steam environment.

4.25 Air Movement Within the Containment. Three ultrasonic anemometers were installed inside the containment. Two of the anemometers were located
in the vicinity of one of the heat transfer assemblies about 2-3/8 inches and 7/8 inch from the liner. The third anemometer was located in the annulus between the operating floor and the containment liner. These instruments measured air velocities during the steam tests. No containment air recirculation equipment was operated during the steam tests.

4.3 Results of DBA Tests

Four simulated DBA tests were performed in the CVTR containment system. These tests included (a) a system check-out test in which the containment was pressurized to about 7 psig and the spray system was operated for about three minutes; (b) an initial DBA test in which the containment was pressurized to about 18 psig and the resulting pressure allowed to decay by natural processes; (c) a second DBA test, similar to the initial DBA test, in which a 290-gpm pressure reduction spray was initiated shortly after the pressure
attained the maximum value and was operated about 12 minutes; and (d) a third DBA test identical to the second test except the pressure reduction spray flow rate was increased to 500 gpm.

In the check-out test, steam was injected for a short duration to evaluate the steam control-injection system and to compare the measured containment response with pretest predictions. The test results were used to determine whether a safety margin existed in pretest analysis and also to determine the injection time for the full-scale 18-psig DBA tests.

Preliminary results of the 18-psig tests are given along with discussions of analytical models and comparisons of the experimental containment response with the predicted response.

4.31 Pressure History. Pressure histories for the three 18-psig tests are given in Figure 45. The data presented were obtained from a Heise gage which measured pressure at a point in the containment near the operating floor. The effectiveness of the spray system as a pressure reduction mechanism is illustrated by a pressure comparisons for the decay region of the tests.

![Fig. 45 Pressure response of full-scale CVTR steam tests.](image-url)
A quantitative evaluation of the spray effectiveness is obtained by comparing the potential reductions in containment leakage resulting from the spray operation. Since pressure is the driving force for leakage from the containment, a reduction in pressure correspondingly results in a reduction in leakage. The leakage rate tests of the CVTR showed that for certain leak paths the leakage rate pressure dependence was represented by the molecular flow equation[45]:

\[ L_r = K_m (1 - 1/\bar{P}_a) \]

where

- \( L_r \) = leakage rate per time increment (time\(^{-1})\)
- \( K_m \) = proportionality constant for molecular leakage
- \( \bar{P}_a \) = containment pressure (atmospheres absolute).

The fact that the leakage rate pressure dependence follows the molecular flow equation does not necessarily mean that the paths were molecular type paths; they may have been a combination of laminar and turbulent flow paths. If this equation is valid for a DBA-pressure reduction spray environment and if temperature effects on containment leakage are neglected, the percent reduction in leakage for the CVTR containment resulting from spray operation can be estimated. The percent reduction in leakage expressed in terms of change in mass of the contained volume for a specified period is

\[ \Delta L = (100) \frac{M_1 - M_2}{M_1} \]

where

- \( \Delta L \) = leakage reduction (percent)
- \( M_1 \) = mass loss at greater leak rate, \( L_1 \)
- \( M_2 \) = mass loss at smaller leak rate, \( L_2 \).

By applying the molecular flow leak rate equation the following mass losses are obtained:

\[ M_1 = t \int_{t_1}^{t_2} L_1 \, dt = K_m t \int_{t_1}^{t_2} (1 - \frac{1}{\bar{P}_1}) \, dt \]

\[ M_2 = t \int_{t_1}^{t_2} L_2 \, dt = K_m t \int_{t_1}^{t_2} (1 - \frac{1}{\bar{P}_2}) \, dt \]

Thus, for a time period \( t_2 - t_1 = t \), the percent reduction in leakage can be expressed as
In the derivation of this expression the assumption was made that the coefficient \( K_m \) does not vary with pressure.

Table IV compares the areas under the curves of Figure 45 corresponding to the time period between 5 and 15 minutes, and the associated percent reduction in leakage calculated from the preceding expression for this 10-minute period of spray operation. Had the sprays been operated for an extended time period, as is usual for power reactor spray systems, the containment pressure would have rapidly approached ambient conditions thereby essentially eliminating the driving force for leakage. The percent reduction of leakage with spray operation versus that without sprays (natural decay) would then have been considerably larger for a longer pressure decay than is indicated by the relatively very short 10-minute time period represented in Table IV.

<table>
<thead>
<tr>
<th>Test</th>
<th>Area Under Curve (psig-min)</th>
<th>Normalized to Natural Decay Test</th>
<th>Area Reduction (%)</th>
<th>Molecular Flow Leakage Reduction, ( \Delta L ) (%)</th>
</tr>
</thead>
<tbody>
<tr>
<td>Natural Decay</td>
<td>134</td>
<td>1.0</td>
<td>0</td>
<td>0</td>
</tr>
<tr>
<td>290 gpm spray</td>
<td>111</td>
<td>0.82</td>
<td>18</td>
<td>13</td>
</tr>
<tr>
<td>500 gpm spray</td>
<td>92</td>
<td>0.68</td>
<td>32</td>
<td>25</td>
</tr>
</tbody>
</table>

4.32 Temperature History. The CVTR containment is divided by an operating floor into two distinct volumes with interconnecting flow paths. The upper and lower regions have about 142,000 and 85,000 ft\(^3\) free volume, respectively, and the total flow path area is about 330 ft\(^2\). The upper volume is relatively clear of obstructions whereas the lower volume which can be subdivided into a region (intermediate region shown in Figure 41) below the operating floor and a basement region contains tanks, pipe runs, support structures, and equipment.

Temperature data for the 18-psig natural decay test is given in Figure 46. Three of the temperature curves represent atmospheric temperature histories in the three separate regions of the containment, that is, the upper region (dome) above the operating floor, the intermediate region below the operating floor, and the basement region. The large temperature differences

\[
\Delta L = (100) \left[ 1 - \frac{\int_0^t \frac{1}{P_2} \, dt - \int_0^t \frac{1}{P_1} \, dt}{t - \int_0^t \frac{1}{P_1} \, dt} \right] \tag{a}
\]

[a] \( \frac{1}{P_1} \) and \( \frac{1}{P_2} \) are dimensionless because in the derivation of the molecular flow equation, the numeral 1 in the numerator represents one atmosphere pressure.
are indicative of the effect on the temperature distribution of containment subdivision into compartments. Additional considerations related to this aspect of the CVTR temperature behavior are provided in Section 4.4, Analytical Predictions and Comparisons with Experimental Results.

Steel liner temperature data, also shown in Figure 46, represent one sample point located near the containment bend line in the upper region. The magnitude of the liner temperature is nearly that of the bulk containment atmosphere temperature in this region and is one indication of the high rate of heat transfer from the steam-air mixture to containment structures occurring in the upper region during the steam injection period.

Temperature data for the two tests with pressure reduction sprays are shown in Figures 47 and 48. As for the natural decay test, a large spread in temperatures is noted prior to spray initiation. However, the spray operation effectively increases the rate of temperature decay and also, apparently, contributes to atmosphere mixing throughout the containment volume since the temperature of the basement region is convergent to a higher value earlier. The mixing mechanism is influenced by the transfer of energy to the lower regions by the migration of the spray water and condensate. The larger spray rate produces more rapid mixing.
4.33 Miscellaneous Information. The CVTR emergency coolant injection pump motor and a motor-driven valve were operated during the pressure decay phase of the DBA tests to determine operability under DBA conditions. Each unit operated as intended. Posttest visual inspection of the equipment revealed that some lubricant had leaked out of each unit; however, the equipment apparently was not affected during the short time of operation.

Visual and photographic observations of painted surfaces in the containment were made following the DBA tests. Blistering of the paint on the steel liner surface was observed following DBA testing. The blistering is believed to be temperature dependent because the blisters were found only on the liner in the region above the operating floor. The blisters apparently were formed by steam and water penetrating the paint at a weak spot or pin-hole-type inclusion and running down under the temperature softened enamel coating. Blistering also was noted in the paint covering the concrete floor surfaces. These blisters first appeared after the hot air tests performed prior to the DBA testing and are believed to be the result of temperature related off-gasing of the concrete. The DBA tests caused additional blistering of these surfaces, particularly on the operating floor.
Fig. 48 Temperature history for 18-psig 500-gpm spray test.

Visual and photographic observations of the containment concrete surface were made during DBA testing. Since expansion due to temperature was the principal cause of the cracking of the concrete surface, the cracks (first observed on the dome and in the upper cylindrical regions of the containment during hot air testing) were noticeably larger during the DBA tests. This cracking would be expected because the inside temperature of the upper region of the containment was higher during the DBA tests than during the hot air tests. The concrete cracking is discussed further in Section 5.33.

An ambient temperature, 21-psig leak rate test of the containment was performed following the DBA testing. The purpose of this test was to determine whether DBA test conditions had affected the leak tightness of the containment. A leak rate of about 0.23%/day was measured following the DBA tests compared to about 0.31%/day measured for similar conditions prior to the DBA tests.

4.4 Analytical Predictions and Comparisons with Experimental Results

An important objective of the CVTR DBA tests was to obtain data for comparison of experimental results with analytical predictions. CONTEMP[T46],
a containment response computer code that is representative of the state-of-the-art, was utilized for much of the CVTR analysis. In addition, CONDRU II[a], a developmental containment response code which is an extension of CONDRU I, was used to predict and evaluate temperature differences between CVTR compartments. CONTEMPT is a single-volume, uniform temperature model whereas the CONDRU II model has the capability of calculating pressures and temperatures in a containment made up of two volumes (compartments) separated by a flow path.

Pretest CONTEMPT calculations were used to design the experiments. The pretest calculations were successful in that the overall features of the CVTR containment response were reasonably predicted such that adequate design parameters were selected.

An important parameter to be determined from the CVTR DBA tests is the time-dependent coefficients for heat transfer by steam condensation. A preliminary estimate of condensation heat transfer was determined from measured temperature differences between the steel liner and containment atmosphere through use of the relationship

\[
h A(T_c - T_l) = C \frac{dT_l}{dt} A X
\]

where

- \( h \) = heat transfer coefficient (Btu/ft\(^2\)-hr-°F)
- \( A \) = unit area (ft\(^2\))
- \( T_c \) = temperature of vapor and air in dome (°F)
- \( T_l \) = liner temperature (°F)
- \( C \) = volumetric heat capacity (55 Btu/ft\(^3\)-°F)
- \( X \) = liner thickness (0.02 ft)
- \( \frac{dT_l}{dt} \) = rate of liner temperature rise (°F/sec).

Figure 49 is a plot of three time-dependent heat transfer coefficients. The posttest curve represents the coefficient as tentatively determined by the given relationship from very limited liner surface and bulk steam-air temperatures measured in the containment upper region. The coefficient is applicable only for the specified region. Also given in Figure 49 is the best estimate heat transfer coefficient curve representing a combination of sources of heat

[a] CONDRU I and II were written by H. G. Seipel, a West German National recently on assignment to Phillips Petroleum Company through an AEC training agreement; both are an extension of earlier work performed in Germany by Mr. Seipel and others.
transfer data. The curve is similar to that obtained from the often used Uchida correlations for steel[47] which are usually combined with a constant coefficient for concrete of about 40 Btu/hr-ft²-°F. For comparison, a time-dependent curve representative of the Uchida data for steel is also shown in Figure 49.

![Graph of heat transfer coefficient comparisons for CVTR calculations.](image)

**Fig. 49** Heat transfer coefficient comparisons for CVTR calculations.

CONTEMPT containment response predictions were compared to the measured response. CONTEMPT predictions were based on the experimental steam flow rate and enthalpy, and the best estimate and posttest experimental heat transfer coefficients shown in Figure 49 were applied. The calculated response curves are presented in Figure 50 along with the experimental response for the 18-psig natural decay DBA test. Calculations based on commonly used best estimate heat transfer data significantly overestimated the experimental CVTR pressures. For this test, better agreement is achieved with the posttest experimentally estimated heat transfer coefficient. This better agreement may be fortuitous because the heat transfer was assigned uniformly to all heat slabs, and some slabs, particularly those in the basement (low temperature regions), may not participate in heat transfer to the same extent as those in upper regions. The implication is that the actual heat transfer coefficient for the upper region may be even higher than was estimated from the experimental temperature data. Further analysis of all the test data is necessary to determine the overall heat transfer behavior in the containment.

Pressure-time results for the 290- and 500-gpm spray tests are shown in Figures 51 and 52, respectively. In each case CONTEMPT calculation results using the posttest experimentally estimated heat transfer coefficient
and the experimental steam injection rates and enthalpies are compared to the measured results. The same general comments that apply to Figure 50 also apply to Figures 51 and 52. The lower pressures predicted by CONTEMP for the 500-gpm spray test are believed to be a result of underestimating the steam injection flow rate.

Figure 53 shows a comparison between temperature data from the natural decay (no spray) test and temperature results from a CONTEMP calculation in which the posttest heat transfer coefficient was used. The CONTEMP temperature results are the volume-weighted average temperatures. No indication of temperature differences due to geometry or temperature differences from stratification can be expected from the CONTEMP or similar one-volume analytical models. Data from the same test are compared in Figure 54 to calculated temperatures obtained from the two-volume model of CONDRU II. Excellent agreement for the calculated and experimental temperatures in the upper region is exhibited. A volume-weighted average temperature is determined by CONDRU II for the lower regions. CONDRU II pressure predictions (not shown) are about 10% higher than measured values. The CONDRU calculations utilize the posttest experimental heat transfer coefficient data; however,
the same general comments concerning the use of the heat transfer coefficient apply to this calculation as applied to the previously discussed CONTEMPT calculations.

4.5 Conclusions from DBA Tests

Several general conclusions were reached from the preliminary evaluations of the test results. Most significant of these conclusions is the apparent high rate of energy transfer from the steam-air mixture in the containment volume to the heat absorbing structures of the containment. This high energy transfer rate resulted in lower peak pressures than were predicted by CONTEMPT calculations using currently accepted heat transfer correlations and may also account for the higher than predicted pressure during the decay period following the steam injection.

The large temperature stratification in the containment volume resulting from the steam injection is another significant finding of these tests. Similar phenomena may be expected to a greater or lesser degree if a DBA should
occur in other nuclear plant containment structures, depending on the particular geometry or type of compartmentation and the location of primary system rupture. Since most current analytical models used to calculate containment response to accident conditions are single-node (one-volume) representations, temperature differentials are not obtained from such calculations. Because of the interrelation of temperature and heat transfer, a multinode-type model appears to be needed for more accurate representation and prediction of containment response.

As shown by the experimental results, the pressure reduction spray system effectively reduced the containment pressure following termination of the steam injection. Since containment leakage rate is a function of pressure, use of pressure reduction sprays can substantially reduce the potential for fission product release from containments during accidents involving a nuclear reactor.

Finally, the gross integrity of the containment was not compromised by performance of the DBA tests. Containment leakage rate measurements performed after DBA testing was completed showed that the DBA tests appear to have produced reduced containment leakage.
Fig. 53 CONTEMPT temperature results for natural decay test.

Fig. 54 CONDRU II temperature results for natural decay test.
5. CONTAINMENT LEAKAGE RATE TESTS

The objective of the CVTR integrated leakage rate tests was to perform containment leakage rate tests at both ambient and elevated temperatures and several static pressure conditions in a manner similar to that for tests being conducted for AEC compliance requirements. The specific objectives of these tests were to:

1. Determine the adequacy and sensitivity of generally accepted leakage rate measurement techniques used in containment leak testing.
2. Evaluate the effects of internal temperature and pressure on containment leakage.
3. Provide base-point data for evaluating the containment leakage resulting from a DBA occurrence.
4. Provide information on other areas of interest including the adequacy of generally accepted extrapolation equations, instrumentation requirements, and error analysis.
5. Evaluate and report typical leakage rate testing experience, especially the problems encountered and the solution to those problems.

Attaining these objectives hopefully will aid in establishing uniform leakage testing and reporting. Such standardization will permit independent evaluation and comparison of leakage rate test results.

The following sections briefly describe the test equipment and instrumentation, test results, and conclusions reached from these results. A more detailed description of the instrumentation and a preliminary evaluation of the results have been documented[48].

5.1 Tests Description and Procedures

Ambient temperature, integrated leakage rate tests were conducted in groups of five tests at 6, 13, and 21 psig in ascending and descending order to evaluate the relationship between leakage rate and test pressure. Additional integrated leakage rate tests were performed at 21 psig and elevated temperatures of approximately 150 and 200°F to determine the influence of temperature upon leakage rate. A final ambient temperature, 21-psig integrated leakage rate test was conducted following the DBA test series to evaluate the effects of DBA testing on the leak tightness of the containment vessel. During part of these tests, a known leak was superimposed upon the normal containment leakage to determine the accuracy and relative sensitivity of the leakage rate measurement systems.

At the completion of several of the tests, a makeup air system was used to return the containment pressure to that which existed at the start of the
test. The amount of makeup air required was used to calculate the containment leakage and help substantiate the leakage rates measured by the absolute and reference vessel methods.

The integrated rate tests were performed, as far as possible, identically to those previously performed at CVTR for AEC compliance requirements[49] and to those performed for evaluation of the CVTR continuous leakage rate system[50].

During containment pressurization at the start of a test series, the personnel hatch and ventilation valves, which were generally open between each test series, were soap bubble leak tested and all leaks stopped. Once leakage rate tests were begun, however, and for the duration of a test series (five tests) no repairing or adjustment of leaks was allowed. In addition, as successive tests in a given test series were performed, the containment pressure was simply increased or decreased to the next test pressure. In this way, the true effect of pressure upon leakage rate could be determined.

The elevated temperature leakage rate tests were performed with the containment at a constant pressure of 21 psig. The initial test in the series was run with the containment at ambient temperature. High capacity electric heaters installed in the containment building air recirculation system were then energized to increase the building temperature to the desired test temperatures, 150 and 200°F. During heating, air was continually bled from the containment to hold the pressure constant at approximately 21 psig.

As the containment system approached the desired test temperature, some of the electric heaters were turned off until, at the desired test temperature, the heat input balanced the heat loss. The heaters were then manually set for a constant heat input and remained so for the duration of the test. A slight containment temperature drift was experienced, but it was very gradual and did not affect the test results.

Use of the constant heat input method resulted in containment air temperature gradients of about 20°F during the hot air tests. Once established, however, and regardless of the length of the test, these gradients changed very little even though the containment air recirculation system was in constant operation at a flow rate of about 25,000 ft³/min.

The final integrated leakage rate test was performed following the DBA test series. This test was a 21-psig ambient temperature test performed identically to previous ambient temperature tests.

Before the vapor container was pressurized for a leakage rate test, the reference vessels were pressurized with dry air to a pressure about 1 psig greater than that desired for the test and isolated. The containment was then pressurized to the desired test pressure and the containment and reference vessel equalizing block valves opened. Since the reference vessels were at a higher pressure than the containment, air flow during pressure equalization was always from the reference vessel to the containment, thus ensuring a dry reference vessel atmosphere and eliminating the need for reference vessel vapor pressure measurements.
The length of the leakage rate tests varied from 18 to 48 hours. For some tests, a known leak was superimposed on normal containment leakage for an additional 6 to 24 hours. During all tests, complete sets of data were taken hourly.

5.2 Leakage Rate Test Instrumentation and Measurements

Containment temperature, pressure, and humidity, and the differential pressure between the containment and each reference vessel were the primary measurements made during the integrated leakage rate tests. Secondary measurements included flow from two controlled leak systems, containment liner strain, and vapor container expansion. These measurements were made at various containment pressures and temperatures and provided information on containment leakage rate, stress level, and expansion.

5.21 Reference Vessels. Two reference vessels, a one-inch-diameter copper tube system and a 1/4-inch-diameter copper tube system, existed at CVTR and were used for the integrated leakage rate tests. Two additional parallel reference vessels, Figure 55, were installed for the leakage rate tests. Both vessels were constructed of 3/4-inch-diameter thin-walled, copper-refrigeration tubing and were installed as nearly as possible on a volume-weighted basis; that is, the volume of any given portion of the reference vessel was in direct proportion to the free volume of the containment in which it was located.

The 3/4-inch-reference vessels were fabricated by joining 50-foot sections of refrigeration tubing to form a continuous length of tubing approximately 1400 feet long. This tubing was distributed throughout the containment vessel. A minimum distance of five feet was maintained between the reference vessel and the containment wall. The dimensions of the reference vessels are shown in Table V.

Both existing reference vessels and one of the 3/4-inch vessels were used in the conventional manner; that is, the vessel was sealed and the differential pressure which developed between it and the containment was measured and the data used to calculate leakage rate. The other 3/4-inch vessel was installed to determine the feasibility of a variation in the reference vessel method of determining leakage rate. This method of determining leakage did not work, however, and following the first group of tests, the vessel was sealed for use as an additional reference system for the remainder of the leakage rate tests.

5.22 Absolute and Differential Pressures. Several pressure measuring devices were used, as shown in Table VI, to obtain data for evaluating the applicability of the various devices with respect to leakage rate measurements. A schematic of the absolute and 3/4-inch reference vessel pressure instrumentation, which is typical of all CVTR systems, is shown in Figure 56.

All pressure instrumentation, except the Heise gage, was located in the auxiliary building basement; the Heise gage was located in the reactor control room.

5.23 Temperature. Temperature measurements of the containment atmosphere were made with 29 Chromel-Alumel thermocouples and 15 platinum resistance thermometers, all of which were positioned throughout the containment on a volume-weighted basis. Temperature measurements of the 3/4-inch
Fig. 55 Three-fourths-inch reference vessel systems.
TABLE V
CVTR REFERENCE VESSELS DIMENSIONS

<table>
<thead>
<tr>
<th>System</th>
<th>ID (in.)</th>
<th>OD (in.)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1-inch</td>
<td>0.996</td>
<td>1.125</td>
</tr>
<tr>
<td>3/4-inch</td>
<td>0.680</td>
<td>0.75</td>
</tr>
<tr>
<td>1/4-inch</td>
<td>0.190</td>
<td>0.25</td>
</tr>
</tbody>
</table>

<table>
<thead>
<tr>
<th>Location[a]</th>
<th>Tubing Size (in.)</th>
<th>Tubing Length (ft)</th>
<th>Volume (ft³)</th>
<th>Volume Ratio Outside to Inside</th>
</tr>
</thead>
<tbody>
<tr>
<td>1-inch Vessel</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Inside containment</td>
<td>1</td>
<td>206</td>
<td>1.11</td>
<td>1 to 358</td>
</tr>
<tr>
<td></td>
<td>1/4</td>
<td>31</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Outside containment</td>
<td>1/4</td>
<td>16</td>
<td>0.0031</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>3/4-inch Vessel</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Inside containment</td>
<td>3/4</td>
<td>1389</td>
<td>3.502</td>
<td>1 to 1297</td>
</tr>
<tr>
<td></td>
<td>1/4</td>
<td>12</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Outside containment</td>
<td>1/4</td>
<td>14</td>
<td>0.0027</td>
<td></td>
</tr>
<tr>
<td></td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>1/4-inch Vessel</td>
<td></td>
<td></td>
<td></td>
<td></td>
</tr>
<tr>
<td>Inside containment</td>
<td>1/4</td>
<td>227</td>
<td>0.045</td>
<td>1 to 14.5</td>
</tr>
<tr>
<td></td>
<td>1/4</td>
<td>16</td>
<td>0.0031</td>
<td></td>
</tr>
</tbody>
</table>

[a] The center of the containment wall at the reference vessel penetration was the dividing line between the inside and outside of the containment.

Reference vessel and the containment liner were obtained by four thermocouples attached to the surface of the reference vessel and 14 thermocouples attached to the surface of the containment liner. The location of the temperature sensors by containment regions is shown in Table VII.

The thermocouples were fabricated at the test site from premium grade thermocouple wire. The junctions were made by fusing the two wires together into a bead about 3/16 inch in diameter. To reduce the effects of thermal radiation, all atmosphere thermocouples except one were shielded with a two-inch length of one-inch-diameter hard-drawn polished copper tubing. One thermocouple was left unshielded to establish a basis of comparisons between shielded and unshielded thermocouples.
### TABLE VI

**CVIR PRESSURE INSTRUMENTATION**

<table>
<thead>
<tr>
<th>Instrument</th>
<th>Range</th>
<th>Use</th>
</tr>
</thead>
<tbody>
<tr>
<td>Two Texas Instruments, Inc., precision pressure gages</td>
<td></td>
<td></td>
</tr>
<tr>
<td>(1) Fused quartz Bourdon tube absolute capsule</td>
<td>0 to 100 inches</td>
<td>Containment pressure</td>
</tr>
<tr>
<td>(2) Fused quartz Bourdon tube differential capsule</td>
<td>0 to 5 psid</td>
<td>Reference vessel - containment differential pressure - 3/4-inch system</td>
</tr>
<tr>
<td>Meriam micromanometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Green indicating sp gr = 1.00</td>
<td>0 to 20 inches water</td>
<td>Reference vessel - containment differential pressure - 3/4-inch system</td>
</tr>
<tr>
<td>Meriam &quot;U&quot; type manometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Red oil sp gr = 0.827</td>
<td>0 to 60 inches water</td>
<td>Reference vessel - containment differential pressure - 1-inch system</td>
</tr>
<tr>
<td>Meriam &quot;U&quot; type manometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Red oil sp gr = 0.827</td>
<td>0 to 6 inches red oil</td>
<td>Reference vessel - containment differential pressure - 1/4-inch system</td>
</tr>
<tr>
<td>Zimmerli gage</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Red oil sp gr = 0.827</td>
<td>26 to 32 inches Mercury</td>
<td>Containment pressure</td>
</tr>
<tr>
<td>Meriam well type manometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: No. 3 indicating sp gr = 2.95</td>
<td>26 to 32 inches Mercury</td>
<td>Atmospheric pressure</td>
</tr>
<tr>
<td>Heise gage</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Barometer</td>
<td></td>
<td></td>
</tr>
</tbody>
</table>

**TABLE VI**

**CVIR PRESSURE INSTRUMENTATION**

<table>
<thead>
<tr>
<th>Instrument</th>
<th>Range</th>
<th>Use</th>
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</thead>
<tbody>
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<td></td>
<td></td>
</tr>
<tr>
<td>(1) Fused quartz Bourdon tube absolute capsule</td>
<td>0 to 100 inches</td>
<td>Containment pressure</td>
</tr>
<tr>
<td>(2) Fused quartz Bourdon tube differential capsule</td>
<td>0 to 5 psid</td>
<td>Reference vessel - containment differential pressure - 3/4-inch system</td>
</tr>
<tr>
<td>Meriam micromanometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Green indicating sp gr = 1.00</td>
<td>0 to 20 inches water</td>
<td>Reference vessel - containment differential pressure - 3/4-inch system</td>
</tr>
<tr>
<td>Meriam &quot;U&quot; type manometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Red oil sp gr = 0.827</td>
<td>0 to 60 inches water</td>
<td>Reference vessel - containment differential pressure - 1-inch system</td>
</tr>
<tr>
<td>Meriam &quot;U&quot; type manometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Red oil sp gr = 0.827</td>
<td>0 to 6 inches red oil</td>
<td>Reference vessel - containment differential pressure - 1/4-inch system</td>
</tr>
<tr>
<td>Zimmerli gage</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: Red oil sp gr = 0.827</td>
<td>26 to 32 inches Mercury</td>
<td>Containment pressure</td>
</tr>
<tr>
<td>Meriam well type manometer</td>
<td></td>
<td></td>
</tr>
<tr>
<td>Fluid: No. 3 indicating sp gr = 2.95</td>
<td>26 to 32 inches Mercury</td>
<td>Atmospheric pressure</td>
</tr>
</tbody>
</table>
Fig. 56 CVTR pressure instrumentation.
TABLE VII
TEMPERATURE SENSOR LOCATIONS

<table>
<thead>
<tr>
<th>Temperature Measurement</th>
<th>Number in Region</th>
</tr>
</thead>
<tbody>
<tr>
<td></td>
<td>Basement</td>
</tr>
<tr>
<td>Atmospheric</td>
<td></td>
</tr>
<tr>
<td>Thermocouples</td>
<td>5</td>
</tr>
<tr>
<td>Resistance thermometers</td>
<td>3</td>
</tr>
<tr>
<td>Liner</td>
<td></td>
</tr>
<tr>
<td>Thermocouples</td>
<td>3</td>
</tr>
<tr>
<td>Reference vessels</td>
<td>1</td>
</tr>
<tr>
<td>Thermocouples</td>
<td></td>
</tr>
</tbody>
</table>

[a] Includes two thermocouples located beneath two-inch-thick polyurethane sheets.

The atmospheric temperature sensors were located at least two feet from any equipment that could have acted as a significant heat source or sink, and at least five feet from the containment wall.

5.24 **Humidity.** The CVTR humidity sampling system was fabricated of 1/4-inch-diameter stainless steel tubing and designed so samples could be drawn from four different containment elevations in any desired combination. During operation, a continuous sample of containment air was pumped through the humidity measuring system. A portion of this flow was sampled by a Cambridge Model 992-C1 hygrometer which measured and indicated the dew point of the sample.

5.25 **Containment Liner Stress and Containment Expansion.** Containment liner strain and containment vessel expansion measurements were made during the CVTR test program to determine the influence of pressure, temperature, and DBA conditions upon the concrete containment and its steel liner.

Strain measurements of the steel liner were made with 83 three-component rosette-type strain gages under three different test conditions:

1. Ambient temperature and static pressure
2. Elevated temperature and static pressure (hot air tests)
3. Dynamic temperature and dynamic pressure (DBA tests).

Vertical and circumferential expansion measurements of the containment structure were attempted using six-inch strain gages bonded to the outside of the containment and by optical techniques using a surveyor's transit and scales positioned on the outside of the containment.
5.26 Controlled Leak Systems. Two controlled leak systems, a known leak system and a fixed leak system, were installed at CVTR. The known leak system superimposed a carefully controlled and measured leakage on normal containment leakage by purging air from the containment to the stack. This system was used during some of the leakage rate tests to help determine the sensitivity and accuracy of the CVTR leakage rate measurement systems.

The fixed leak system was installed to determine the influence of pressure, temperature, and DBA conditions on the leakage rate through a fixed leak path. This system consisted of one-inch-diameter pipe 48 inches long, 40 inches of which were filled with crushed concrete. A four-inch-thick stainless steel wool plug was installed at each end of the pipe to hold the concrete in position. The entire assembly was welded into an existing containment penetration and vented to the stack. The flow, about 100 cm³/min, was initially set with the metering valve and then controlled either on or off as determined by the block valve, thus providing a constant leak path. Before being discharged to the stack, the air passed through a Drierite cartridge to remove the moisture and then through a Hastings mass flowmeter so the flow could be measured.

5.27 Hot Air System. A bank of electric duct heaters totaling 855 kW was installed in the air recirculation system to heat the containment for the hot air tests. The existing air recirculation system fan motors were replaced with larger motors so flow rates of approximately 25,000 ft³/min at 21 psig could be obtained. The motors, heaters, and power cables were designed to operate at elevated temperatures and in the air-steam environment of the DBA tests.

5.28 Instrument Accuracies. To permit evaluation and comparisons of leakage rate measurements, both precision laboratory type instruments and general field type instruments were used to obtain leakage rate data. Table VIII is a tabulation of the instrumentation used at CVTR for leakage rate measurements. The accuracies, resolution, and repeatability values shown in the table were determined experimentally where practical or obtained from the equipment specifications. For the manometers and the barometer, the resolution was taken as one-half the minor scale division.

5.3 Results of Leakage Rate Tests

This section contains a discussion of the equations used in calculating the leakage rate of the CVTR containment, the procedures used in performing the calculations, and the results of the ambient air tests, the hot air tests, and the post-DBA test.

5.31 Leakage Rate Equations. The containment leakage rate measurements were made by both the absolute method and the reference vessel method. Leakage rates were calculated using the equations recommended by the American Nuclear Society[51]. These equations are as follows[a]:

[a] The only deviation from the recommendations of the American Nuclear Society was that the leakage rate was not calculated on an hourly basis. Rather, calculations were made from an initial-data set to each successive data set to reduce the effect of instrument error upon the leakage rate results.
<table>
<thead>
<tr>
<th>Sensor</th>
<th>Range [a]</th>
<th>Accuracy [a]</th>
<th>Resolution [a]</th>
<th>Repeatability [a]</th>
</tr>
</thead>
<tbody>
<tr>
<td>Texas Instruments, Inc., absolute gage</td>
<td>0 to 1359.6 inches water</td>
<td>± 0.2 inch water</td>
<td>4.43 x 10⁻³ inch water</td>
<td>6.64 x 10⁻³ inch water</td>
</tr>
<tr>
<td>Texas Instruments, Inc., differential gage</td>
<td>0 to 138.37 inches water</td>
<td>± 0.02 inch water</td>
<td>4.7 x 10⁻⁴ inch water</td>
<td>7.06 x 10⁻⁵ inch water</td>
</tr>
<tr>
<td>Meriam-micromanometer</td>
<td>0 to 20 inches water</td>
<td>± 0.001 inch water</td>
<td>0.5 x 10⁻³ inch water</td>
<td>[b]</td>
</tr>
<tr>
<td>Meriam-&quot;U&quot; type manometer</td>
<td>0 to 60 inches water at 60°F</td>
<td>[c]</td>
<td>0.05 inch water</td>
<td>[b]</td>
</tr>
<tr>
<td>Meriam-&quot;U&quot; type manometer</td>
<td>0 to 6 inches fluid, 0 to 4.9 inches water at 60°F</td>
<td>[c]</td>
<td>0.05 inch fluid, 0.041 inch water at 60°F</td>
<td>[b]</td>
</tr>
<tr>
<td>Zimmerli gage</td>
<td>0 to 9 cm red oil, 2.93 inches water at 60°F</td>
<td>[c]</td>
<td>0.05 cm fluid, 0.0163 water at 60°F</td>
<td>[b]</td>
</tr>
<tr>
<td>Meriam-well type</td>
<td>0 to 100 inches Hg, 0 to 1359.6 inches water at 60°F</td>
<td>[c]</td>
<td>0.05 inch Hg, 0.63 inch water at 60°F</td>
<td>[b]</td>
</tr>
<tr>
<td>Meriam-well type</td>
<td>0 to 120 inches fluid, 0 to 357 inches water at 60°F</td>
<td>[c]</td>
<td>0.05 inch fluid, 0.15 inch water at 60°F</td>
<td>[b]</td>
</tr>
<tr>
<td>Barometer</td>
<td>26 to 32 inches Hg</td>
<td>[c]</td>
<td>0.005 inch Hg, 0.368 inch water at 60°F</td>
<td>[b]</td>
</tr>
<tr>
<td>Thermocouples, Chromel-Alumel</td>
<td>0 to 200°F of calibration</td>
<td>± 0.2°F</td>
<td>± 0.2°F[d]</td>
<td></td>
</tr>
<tr>
<td>Resistance Thermometers</td>
<td>± 0.25°F</td>
<td>± 0.1°F</td>
<td>± 0.1°F[d]</td>
<td></td>
</tr>
<tr>
<td>Cambridge Dew-point Hygrometer</td>
<td>-20°F to +120°F</td>
<td>± 0.5°F</td>
<td>± 0.5°F</td>
<td></td>
</tr>
</tbody>
</table>

[a] For comparison, all values for pressure have been converted to inches of water.
[b] Manometer repeatability cannot be assessed readily because it depends upon the ability of all operators to read the instrument precisely the same.
[c] Accuracy information is not generally available because of the many different fluids that can be used and also because of the corrections that must be made for any precise manometer readings.
[d] Determined for complete system after sensor installation.
(1) For the absolute method

\[
\text{percent leakage/day} = \frac{24H}{T_1(T_2(T_1 - P_{v_1}))} \left[ 1 - \frac{T_1(P_2 - P_{v_2})}{T_2(P_1 - P_{v_1})} \right] 100. \tag{1}
\]

(2) For the reference vessel method

\[
\text{percent leakage/day} = \frac{24H}{T_1(T_2(T_1 - P_{v_1}))} \left[ \frac{T_1((P_2' - P_{v_2'}) - (P_2 - P_{v_2}))}{T_2(P_1 - P_{v_1})} \right]
\]

\[
\left( \frac{P_1' - P_{v_1'}}{P_1 - P_{v_1}} \right) - \left( \frac{P_1 - P_{v_1}}{P_1 - P_{v_1}} \right) 100 \tag{2}
\]

where

\(H\) = length of the test period (hr)

\(T_1\) = mean absolute temperature of the containment at the start of the test (°R)

\(T_2\) = mean absolute temperature of the containment at the end of the test (°R)

\(P_1\) = absolute pressure of the containment at the start of the test (psia)

\(P_2\) = absolute pressure of the containment at the end of the test (psia)

\(P_{v_1}\) = containment water vapor pressure at the start of the test (psia)

\(P_{v_2}\) = containment water vapor pressure at the end of the test (psia)

\(P_{v_1}'\) = reference vessel absolute pressure at the start of the test (psia)

\(P_{v_2}'\) = reference vessel absolute pressure at the end of the test (psia)

\(P_{v_1}'\) = reference vessel vapor pressure at the start of the test (psia)

\(P_{v_2}'\) = reference vessel vapor pressure at the end of the test (psia).
Since the reference vessels were filled with dry air at the beginning of each test series, the reference vessel vapor pressure can be set equal to zero. Also, because the difference between the reference vessel and containment vessel pressure was measured as a differential, Equation (2) becomes

\[
\text{percent leakage/day} = \frac{24}{H} \left[ \frac{T_1 \left( \Delta P_2 + P_{v_2} \right)}{T_2 \left( P_{v_1} - P_{v_2} \right)} - \frac{\left( \Delta P_1 + P_{v_1} \right)}{\left( P_{v_1} - P_{v_2} \right)} \right] 100
\]

where

\[\Delta P_1 = \text{differential pressure between the containment vessel and the reference vessel at the start of the test (psid)}\]

\[\Delta P_2 = \text{differential pressure between the containment vessel and the reference vessel at the end of the test (psid)}\]

5.32 Ambient Air Test Results. Results of the CVTR leakage rate tests are summarized in Table IX.

The preliminary leakage rate calculations were made and plotted using data from only two of the several leakage rate measuring systems installed at CVTR. These two systems were the 0- to 100-inch mercury Texas Instruments, Inc., absolute gage for the absolute method and the 0- to 5-psia Texas Instruments, Inc., differential pressure gage installed on the 3/4-inch reference vessel for the reference vessel method. Two exceptions to the preceding were (a) for Test 1, which was conducted before the Texas Instruments, Inc., gages were available, data from a 100-inch-mercury manometer and a Meriam micro-manometer were used to calculate the leakage rate by the absolute and the reference vessel methods, respectively; and (b) for Tests 3 through 5, in addition to the results from the Texas Instruments, Inc., gages, data from a 60-inch U-tube manometer installed on the one-inch reference vessel were used for calculations.

The leakage rates for Tests 1 through 5 were calculated by using the average of 10 representative thermocouple readings. For all other tests, the calculations were based upon the average of the 15 resistance thermometer readings.

The containment water vapor pressure was obtained as a combined sample by a simultaneous measurement from all four humidity sampling stations.

Throughout the ambient temperature tests some scatter in test data was experienced. This data scatter, which was consistently less for the 3/4-inch reference vessel than either the one-inch reference vessel or the absolute method, resulted from several contributing factors. In the case of the reference vessels, these factors included the percentage of the reference vessel volume located outside the containment vessel -- 0.3 and 0.08% for the one-inch and 3/4-inch vessels, respectively; the physical location of the reference vessel within the vapor container, that is, the distance from walls, floors, and equipment; the arrangement of the vessel within the containment (volume
<table>
<thead>
<tr>
<th>Test</th>
<th>Initial Conditions</th>
<th>Test Time Without Known Leak (hr)</th>
<th>Test Pressure (psia)</th>
<th>Temperature (°F)</th>
<th>Leakage Rate (%/day)</th>
</tr>
</thead>
<tbody>
<tr>
<td>1</td>
<td>1430</td>
<td>31</td>
<td>20.33</td>
<td>71.9</td>
<td>0.19 0.24</td>
</tr>
<tr>
<td>2</td>
<td>1830</td>
<td>48</td>
<td>27.25</td>
<td>70.3</td>
<td>0.22 0.28</td>
</tr>
<tr>
<td>3</td>
<td>0203</td>
<td>48</td>
<td>35.54</td>
<td>72.5</td>
<td>0.23 0.25</td>
</tr>
<tr>
<td>4</td>
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<td>48</td>
<td>27.27</td>
<td>69.8</td>
<td>0.18 0.24</td>
</tr>
<tr>
<td>5</td>
<td>0730</td>
<td>48</td>
<td>20.21</td>
<td>68.7</td>
<td>0.11 0.17</td>
</tr>
<tr>
<td>6</td>
<td>1830</td>
<td>39</td>
<td>20.80</td>
<td>62</td>
<td>0.25 [a]</td>
</tr>
<tr>
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<td>18</td>
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<td>65</td>
<td>0.31 0.40</td>
</tr>
<tr>
<td>8</td>
<td>0630</td>
<td>20</td>
<td>35.45</td>
<td>67</td>
<td>0.36 0.39</td>
</tr>
<tr>
<td>9</td>
<td>1630</td>
<td>20</td>
<td>27.28</td>
<td>67</td>
<td>0.31 [a]</td>
</tr>
<tr>
<td>10</td>
<td>1830</td>
<td>23</td>
<td>20.47</td>
<td>66.1</td>
<td>0.23 [a]</td>
</tr>
<tr>
<td>11</td>
<td>2330</td>
<td>25</td>
<td>20.60</td>
<td>64.9</td>
<td>0.23 [a]</td>
</tr>
<tr>
<td>11A</td>
<td>0030</td>
<td>24</td>
<td>20.52</td>
<td>64.4</td>
<td>0.10 [a]</td>
</tr>
<tr>
<td>12</td>
<td>1830</td>
<td>18</td>
<td>27.4</td>
<td>65.7</td>
<td>0.17 [a]</td>
</tr>
<tr>
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<td>0830</td>
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<td>35.41</td>
<td>69.6</td>
<td>0.22 [a]</td>
</tr>
<tr>
<td>16</td>
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<td>24</td>
<td>20.48</td>
<td>76.1</td>
<td>0.09 [a]</td>
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<tr>
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<td>18</td>
<td>35.43</td>
<td>76.8</td>
<td>0.31 [a]</td>
</tr>
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<td>18</td>
<td>1630</td>
<td>40</td>
<td>35.47</td>
<td>145.8</td>
<td>0.12 0.32</td>
</tr>
</tbody>
</table>

[a] indicates no known leak data available.
TABLE IX (Contd.)

CVTR LEAKAGE RATE TESTS SUMMARY

<table>
<thead>
<tr>
<th>Test</th>
<th>Initial Conditions</th>
<th>Test Time Without Known Leak (hr)</th>
<th>Test Pressure (psia)</th>
<th>Temperature (°F)</th>
<th>Leakage Rate (%/day)</th>
</tr>
</thead>
<tbody>
<tr>
<td>19</td>
<td>0230 3-20-69</td>
<td>42</td>
<td>34.28</td>
<td>195</td>
<td>[b] 0.1</td>
</tr>
<tr>
<td>20</td>
<td>2230 5-11-69</td>
<td>38</td>
<td>34.85</td>
<td>92</td>
<td>0.23 0.30</td>
</tr>
</tbody>
</table>

[a] No known leak
[b] No leakage measured

weighted); the accuracy of the readout instrumentation used with each system; and the total volume of the reference vessel (the greater the total volume the less the effects of the other factors).

In the case of the absolute method, the controlling factor is the accuracy of the containment temperature measurement. Even with the air recirculation system operating at approximately 25,000 ft³/min, temperature gradients existed during the leakage rate tests. During ambient air tests, temperature gradients of approximately 4.5°F existed with the basement at the higher temperature. In addition, a uniform containment temperature rise of approximately 1.5°F/24-hour occurred. Since containment temperature measurements have significant effect on the leakage rate calculations, particularly by the absolute method, much of the data scatter is believed to be associated with the measurement of these temperature gradients.

Generally, the data scatter increased as test pressure decreased because the lower the test pressure the less the containment leakage rate and the more difficult the leakage rate measurement becomes. Therefore, if the same accuracy in test results is desired, tests at lower pressures must be run for longer periods of time than for higher pressure tests.

The leakage rate lines which have been drawn through the best data for each test are best estimate lines, and no attempt has been made to mathematically fit the lines to the data. Generally, however, the lines were drawn through the 3/4-inch reference vessel precision pressure gage data, since, as mentioned earlier, these data provided the least scatter.

The physical effects of the ambient air tests upon the containment appeared to be negligible. Originally, repeated cycling of the containment from atmospheric pressure to 21 psig was thought to detrimentally affect the structure. However, no detrimental effects were observed. Containment expansion and liner strain measurements indicate that containment expansion and contraction...
These changes were so slight, however, that no permanent visible or measurable effects occurred.

During the latter portion of several of the leakage rate tests, a known leak was superimposed upon normal containment leakage to aid in determining the sensitivity and accuracy of the containment leakage rate measuring system. The leakage rate system quickly detected the known leak each time the known leak was placed in operation. The Texas Instruments, Inc., gages were especially sensitive to the known leak. The effects of the known leak could be seen almost immediately through observation of these instruments.

The accuracy of the containment leakage rate system is evaluated by comparing the measured increase in containment leakage as determined by the leakage rate system to the calculated increase in containment leakage as determined by the flow through the known leak system. By using the best estimate of containment free volume (227,000 ft³) and the results of Test 7, the calculated effect of the known leak was found to be 0.11%/day, which compares very well with the measured effect of 0.09%/day. Part of the difference between these values can be attributed to errors in the calculated value which depends upon the accuracy of the known leak rotameter, the care with which flow is controlled while the known leak is in operation, and the accuracy to which the containment free volume is known.

The effect of test pressure on the CVTR containment leakage can be seen in Figure 57, which shows the leakage rate curves for Tests 11A, 12, and 13. To evaluate leakage rate extrapolation equations, the experimentally determined leakage rate and that calculated by the most commonly used extrapolation equations [52] shown in Table X are plotted in Figures 58 and 59 in terms of Le/Lt versus Pe/Pt. Figure 58, which is valid for Tests 1 through 5, 11A, 12, and 13, shows that the containment leakage is best extrapolated by the molecular flow equation, which, if true, indicates that the containment leakage occurred through an immense number of very small leaks. Figure 59, however, which compares the results of Tests 6 through 10, shows that the containment leakage during these tests closely followed the turbulent flow, smooth path extrapolation equation. During this test series, in addition to normal containment leakage, leakage occurred through the packing gland on one of the large air recirculation system valves causing a change in the leak paths and as a result a change in the applicable extrapolation equation.

As discussed in other reports [52] molecular flow is not of significant consequence for the size of leaks of interest in containment vessels; that is, in the range of flows of interest (300 to 500 ft³/day) an enormous number of leak paths would have to exist for leakage to occur by molecular means. Therefore, although the molecular flow extrapolation equation predicted the containment leakage for part of the tests, the prevailing type of flow from the containment was probably not molecular, but rather a combination of laminar and turbulent types of flow.

The CVTR experience indicates that although an extrapolation equation may predict the containment leakage rate as a function of pressure, the predominate flow from the containment may not be of the type of the equation...
Fig. 57 Containment leakage rate at various pressures.

TABLE X

LEAKAGE RATE EXTRAPOLATION EQUATIONS

<table>
<thead>
<tr>
<th>Type</th>
<th>Equation</th>
</tr>
</thead>
<tbody>
<tr>
<td>Laminar Flow</td>
<td>( L_e = L_t \left[ \frac{P_e - 1/P_e}{P_t - 1/P_t} \right] )</td>
</tr>
<tr>
<td>Molecular Flow</td>
<td>( L_e = L_t \left[ \frac{1 - 1/P_e}{1 - 1/P_t} \right] )</td>
</tr>
<tr>
<td>Turbulent Flow, Rough Path</td>
<td>( L_e = L_t \left[ \frac{1 - 1/P_e^2}{1 - 1/P_t^2} \right]^{1/2} )</td>
</tr>
<tr>
<td>Turbulent Flow, Smooth Path</td>
<td>( L_e = L_t \left[ \frac{P_e}{P_t} \right]^{1/7} \left[ \frac{1 - 1/P_e^2}{1 - 1/P_t^2} \right]^{4/7} )</td>
</tr>
</tbody>
</table>

where

- \( L_t \) = leakage rate at test pressure, \( P_t \)
- \( L_e \) = leakage rate at extrapolation pressure, \( P_e \)
- \( P_t \) = test pressure (atmospheres absolute)
- \( P_e \) = extrapolation pressure (atmospheres absolute).
which applies. In addition, to determine which equation applies may be difficult. Even if a series of leakage rate tests is performed to obtain an applicable extrapolation equation, the equation will be valid only as long as the leakage paths remain constant.

The data obtained from the fixed leak system during Tests 1 through 5 and Tests 6 through 10 were also compared to that predicted by the extrapolation equations and were found to extrapolate according to the molecular flow equation. However, this flow also is probably a combination of several types of flow.

5.33 Hot Air Test Results. The results of the hot air test series (Tests 16 through 19) are plotted in Figure 60. As can be seen, a definite decrease in containment leakage occurred as test temperature increased. The leakage decreased from 0.31%/day at ambient temperature to 0.15%/day at 146°F to 0.13%/day (including the known leak) at 195°F.
During Test 19, even though the containment had been dried as much as practical, the dew point steadily increased until at 42 hours, despite attempts to continue measurements, the range of the hygrometer was exceeded and the test was terminated. The high containment temperatures of Tests 18 and 19, 146 and 195°F, respectively, melted organics in the containment including refueling machine shielding material and neutron chamber shielding material. These temperatures also decreased the viscosity of oils and greases in pumps, motors, and valves, and caused a large increase in the concentration of these materials in the containment atmosphere. As containment atmosphere samples passed through the hygrometer some of these organic contaminants plated out on the sample mirror causing an erroneous (high) dew point indication by the hygrometer. When this erroneous dew point was used in the leakage rate calculation, an abnormally high leakage rate was obtained between 20 and 28 hours, as shown in Figure 60. Several methods were devised to overcome this problem, including heat tracing the hygrometer sample lines, maintaining a continuous flow through the hygrometer, throttling flow to the hygrometer so that the
sampling pressure was less than 4.5 psig, and balancing the dew-point hygrometer (automatically cleaning the mirror) while holding a vacuum on the hygrometer sensor unit. These methods allowed Test 19 to be continued for an additional 14 hours.

The physical effects of the elevated temperature tests upon the containment were quite pronounced. As the temperature of the concrete containment increased, a large number of cracks appeared on the outside concrete surface. Photographs of a typical external containment crack on the cylindrical section of the containment near ground level are shown in Figure 61. The photographs were taken during and following Test 19 at containment conditions of 195°F and 21 psig, 100°F and atmospheric pressure, and ambient temperature and atmospheric pressure for Figures 61a, b, and c, respectively.

Inside the containment the high temperature caused several noticeable effects: paint darkened; grease and oil dripped from gear boxes, bearings, and motors; shielding material melted and flowed from the neutron chambers and the refueling machine; and the containment liner contained numerous small, inward dents, presumably from expanding against aggregate protruding from the inside surface of the concrete wall. The containment integrity was not violated, however, even though high temperature conditions were maintained for more than 10 days.

5.34 Post-DBA Test Results. Following the DBA tests, the final integrated leakage rate test (Test 20) was performed. This test was performed to determine the overall effects of DBA conditions upon containment leakage and to determine whether DBA testing had violated containment integrity. This test showed a slight decrease in the CVTR containment leakage, approximately 0.23%/day versus 0.31%/day. Because of the data scatter experienced during
Fig. 61 Typical external containment concrete cracks.
this test, the leakage rate line shown in Figure 62 could be drawn with several different slopes, resulting in a leakage rate of 0.15 to 0.26%/day. Thus, the containment leakage rate did not increase because of DBA conditions and possibly decreased slightly.

![Graph showing leakage rate test results](image)

**Fig. 62 Leakage rate Test 20 -- 21 psig.**

The average containment temperature during Test 20 was about 20°F higher than that which existed prior to hot air and DBA testing. Because higher containment temperatures result in decreased leakage rates, as shown by the hot air tests, the decreased leakage rate of Test 20 may be a result of temperature or DBA conditions, or both. Certainly, DBA conditions did not cause an increase in containment leakage and may have caused a slight decrease.

5.4 **Conclusions for Containment Leakage Rate Tests**

The following conclusions were reached from the CVTR tests:

1. For a particular leakage path, the CVTR leakage rates increased and decreased with pressure and as predicted by established extrapolation equations. However, leakage paths and associated leakage rates were found to change during and between test series. Thus, the selection of a particular extrapolation equation may be uncertain.

2. The CVTR containment leakage rate decreased with increasing test temperature. This phenomenon is undoubtedly dependent upon the particular leak paths, and these results may not be applicable to all containments. However, they are expected to be representative of many of the new containments which are similar in construction to CVTR.

3. Subjection of the containment to simulated DBA conditions appears to have had negligible effect on the containment leak tightness.
The Capsule Driver Core (CDC) installed in SPERT IV is being used to obtain experimental data on the behavior of reactor fuels under transient overpower conditions. Test fuel rods are positioned in the central flux trap in the CDC in an environment of coolant, hardware, fuel rod grouping, flow constraints, pressure, temperature, and other environmental parameters which might exist in a power reactor at the onset of a reactivity accident. Data of particular interest include failure modes and thresholds, conversion efficiencies of nuclear-to-mechanical energy, transient pressures, and metal-water extents, all as functions of transient energy deposition for a variety of fuel design parameters, burnup, and environmental conditions. The program is described in detail in Reference 53.

The preliminary results of finite phases of the test program are published in interim technical reports issued at the earliest possible date following completion of the reported tests. Final results are published subsequently in regular professional journals or topical reports. A summary of the two interim technical reports published during the period June-September 1969 follows.


The tests discussed in this report were the third in a series of tests in an industrial participation program designed to reveal consequences of power bursts in fuel rods manufactured by commercial fabrication techniques. The fuel was produced by the General Electric Company using the same fabrication techniques currently used in producing fuel rods for commercial boiling water reactors. The rods contained 7%-enriched powder UO₂ clad with 0.020-inch-thick wall, 0.3125-inch OD, Zircaloy-2 tubing.

The encapsulated fuel rods were inserted individually in the CDC test space in stagnant water at atmospheric pressure and ambient temperature and subjected to single power bursts. The reactor periods were from 8.5 to 3.9 msec, which resulted in energy depositions in the fuel ranging from 151 cal/g of UO₂ to 343 cal/g of UO₂.

Preliminary results indicate that the failure threshold for these rods is in the energy-deposition range between 266 and 292 cal/g of UO₂ and that failure results from melting of the cladding.


The fuel rods tested in the reported series were designed specifically for testing in the CDC to obtain basic data describing the performance of water reactor fuels subjected to transient overpower conditions. The 10.5%
enriched UO₂ fuel rods had an outside diameter of 1/4 inch with an active length of five inches and were clad with 0.014-inch thick, cold-worked Zircaloy-2. The rods were encapsulated in stagnant water at ambient temperature at atmospheric pressure. Each of the 14 rods tested was subjected to a single burst at reactor periods from 8 to 3 msec, resulting in energy depositions ranging from 170 to 655 cal/g of UO₂.

Preliminary results indicate an initial cladding failure threshold between 240 and 257 cal/g of UO₂, at which brittle fracture and nearly complete loss of cladding strength were observed. Failure from melting of the cladding initially occurred at 267 cal/g UO₂. A prompt-failure threshold (cladding surface temperatures less than melting) was first observed at an energy deposition of 360 cal/g of UO₂. The prompt-failure threshold also marked the onset of capsule pressure generation and mechanical energy production.

The measured cladding surface temperatures at the time of cladding failure decreased as a function of total energy deposition. For the 655 cal/g of UO₂ tests, the cladding surface temperature rise was only about 300°C at the time of the cladding failure.

Metal-water reaction of the cladding increased as a function of energy deposition; essentially all of the cladding reacted for energies greater than 600 cal/g of UO₂.

Transient capsule pressures and mechanical energy generation increased rapidly for energies greater than 600 cal/g of UO₂. At 655 cal/g, the maximum measured pressure was about 1750 psig, and the nuclear-to-mechanical energy conversion was about 2.8%. At 490 cal/g, the maximum pressure was only about 180 psig with a nuclear to mechanical energy conversion of only 0.2%.
VI. REFERENCES


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49. W. E. Barrow, CVTR Vapor Container Leak Rate Test, September 1966, CVNA-266 (November 1966).


