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ANNUAL REPORT OF
THE NUCLEAR SAFETY DEVELOPMENT BRANCH

JANUARY 1 - DECEMBER 31, 1970

Aerojet Nuclear Company

NATIONAL REACTOR TESTING STATION

Idaho Falls, Idaho — 83401

MASTER

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ANNUAL REPORT OF
THE NUCLEAR SAFETY DEVELOPMENT BRANCH
JANUARY 1- DECEMBER 31, 1970

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ABSTRACT

Activities of the Nuclear Safety Development Branch of Idaho Nuclear Corporation are reported for the calendar year 1970. These activities, which are part of the Water Reactor Safety Program of the United States Atomic Energy Commission, are currently continuing under direction of Aerojet Nuclear Company. Topics discussed are as follows: (a) LOFT integral experiments planning with emphasis on analysis to support facility design; (b) loss-of-coolant accident analysis development including thermal, hydraulic, structural, and fission product models; (c) technical assistance in reactor safety analyses in support of the Atomic Energy Commission's regulatory functions; (d) separate effects experiments including the Semiscale Blowdown and Emergency Core Cooling (ECC) Project, the Full Length Emergency Cooling Heat Transfer Projects for both boiling water reactors (BWR-FLECHT) and pressurized water reactors (PWR-FLECHT), the Scoping Emergency Cooling Heat Transfer (SECHT) Project, incipient failure detection by acoustic emission, in-plant testing in the Carolinas Virginia Tube Reactor Containment Testing Project, and fault tree analysis of the reliability of reactor safety systems. Publications prepared and distributed during the year are listed.

SUMMARY

Activities of the Nuclear Safety Development Branch of Idaho Nuclear Corporation[a] are reported for the calendar year 1970. These activities are part of the Water Reactor Safety Program of the United States Atomic Energy Commission.

LOFT INTEGRAL EXPERIMENTS

The principal purpose of the tests to be conducted in the Loss-of-Fluid Test (LOFT) experimental assembly is to provide experimental data for evaluating and improving analytical methods currently used to predict the loss-of-coolant accident (LOCA) response of pressurized water reactors and to predict the performance of engineered safety systems, particularly emergency core cooling systems. Loss-of-coolant accident response analyses were performed to determine whether including or omitting specific design features typical of those of pressurized water reactors would significantly affect the calculated accident response. The purpose of the analyses was to determine whether such features should be represented in the LOFT system. Specific analyses reported are: (a) the effect of the pressurizer surge line inject location on LOCA response, (b) the effect of accumulator coolant injection into the broken loop on LOCA response, and (c) the effect of a steam generator of the proposed LOFT design on the thermal-hydraulic response during an LOCA. In the analyses, the proposed LOFT design feature was incorporated into a RELAP3 model of a large PWR and the calculated response was compared to the calculated response of the standard large PWR.

The analyses led to the following conclusions: (a) whether the pressurizer surge line inlet is located on the broken loop or on one of the unbroken loops does not substantially change the calculated core thermal response during an LOCA, (b) assuming accumulator coolant injection into the broken loop during an LOCA does not substantially alter the calculated system decompression or core thermal response from the case for which no injection into the broken loop was assumed, (c) a steam generator of the proposed LOFT design, even though not typical of that of a large PWR design, does not significantly affect the calculated system decompression and core thermal response.

LOSS-OF-COOLANT ACCIDENT ANALYSIS -- THERMAL MODEL DEVELOPMENT

The core thermal model development is concerned with developing analytical methods for predicting blowdown cooling, core heatup, and ECC cooling during a loss-of-coolant accident. The following core thermal events are considered in the model development task: (a) blowdown cooling, (b) core heatup, and (c) ECC cooling during reflooding. For blowdown cooling, coupled thermal-hydraulic models are used to predict heat transfer to and from the cladding. Thermal-hydraulic models are used to produce input conditions for core heatup and the cladding mechanical response analyses. A single rod, single channel code for calculating the thermal-mechanical response of the fuel rod cladding is discussed and results from calculations performed with the code are reported.

[a] Now Aerojet Nuclear Company.

The calculations indicate that the large inlet break produces the maximum cladding temperature following an LOCA for a typical large PWR. Also presented are the results of a study on the use of critical heat flux data and correlations in subchannel codes for core thermal response analysis. A multidimensional, multirod, thermal-hydraulic model (MOXY) has been developed and evaluated for core heatup analysis. This model considers conduction in the fuel rod, radiation between rods and other surfaces, and convection in the coolant. The code is briefly described, and the calculations performed are compared with experimental data. The comparison between the calculations and experimental data indicates that the assumptions employed in development of MOXY concerning symmetry and gray diffuse surfaces are satisfactory for predictive analysis.

For cooldown by emergency core coolant, a coupled thermal-hydraulic model is being developed for predicting the rate of energy removal from the core, the preferential flow of the coolant in the core, and the extent of metal-water reactions. The use of data from PWR-FLECHT to produce a correlation for predicting heat transfer coefficients is described. This work represents the beginning of code development for the ECC cooling region.

LOSS-OF-COOLANT ACCIDENT ANALYSIS -- HYDRAULIC MODEL DEVELOPMENT

The purpose of the hydraulic model development activity is to develop hydraulic models for predicting the fluid conditions associated with subcooled decompression, the decompression region between the subcooled and saturated fluid states, the saturated two-phase fluid decompression, and emergency core coolant reflooding. The WHAM and RELAP3 computer codes are the primary analytical tools for this portion of the LOCA analysis. An evaluation performed with the RELAP3 code is reported. This evaluation shows that the coupling between heat transfer and fluid flow in the core region are sufficiently significant that they should not be neglected in accident analyses of reactor systems.

LOSS-OF-COOLANT ACCIDENT ANALYSIS -- STRUCTURAL MODEL DEVELOPMENT

The purpose of the structural model development activities is to develop models for predicting structural stresses on reactor systems both during normal operation and during accident transients. The computer code STRAP (STRuctural Analysis Package) is being developed for this purpose. STRAP is a three-dimensional structural analysis code that can be used in the static and dynamic analyses of structures having up to 1000 degrees of freedom. STRAP consists of two operational programs, STRAP-S and STRAP-D, for static and dynamic analyses, respectively. A third program, STRAP-MS, for the analysis of the dynamic response of substructures, is in the developmental stage.

STRAP predictions have been compared with data from the Semiscale Blowdown and Emergency Core Cooling (ECC) Project. The frequency and amplitude of the measured and calculated stresses compared favorably until saturation pressure was reached.

Stresses were calculated with STRAP-D for a typical large PWR subjected to seismic and blowdown forces. For the seismic loading, the piping was subjected to the measured ground accelerations of the 1940 El Centro earthquake which had an intensity comparable to that recommended for operating basis earthquakes in seismic Risk Zone 3. For the blowdown loading, the primary loop piping was subjected to the transient pressures produced in the piping by blowdown through a 30% break in the cold leg. The calculated stresses in the piping loop during blowdown were about 15% higher than the calculated operational stresses. The earthquake loading increased the calculated stress level about 10%.

Stresses produced by ground accelerations of the 1940 El Centro earthquake were also calculated by the response-spectra static equivalent (RSSE) method for comparison with the values calculated by STRAP-D. The comparisons showed that the RSSE method may not always be conservative; the time-dependent method used in STRAP-D yielded higher stresses at over half of the locations at which stresses were compared.

LOSS-OF-COOLANT ACCIDENT ANALYSIS -- FISSION PRODUCT MODEL DEVELOPMENT

The purpose of the fission product model development activity is to develop models for predicting the release of fission products in the event of a loss-of-coolant accident and the subsequent fission product behavior as it affects the potential for fission product release to the environment. A brief discussion is presented indicating the current understanding of fission product behavior during the loss-of-coolant accident sequence. A model describing the migration of fission products during reactor operation is described. The model is based on a thermomigration (steady state release) model that has been used to describe noble gas release from fuel pins during normal reactor operation. The basic assumption in the derivation of this model is that the driving force for migration of fission products during reactor operation is a Gibbs-free-energy gradient that is dependent on temperature and concentration gradients.

A method was developed from the steady state release model to calculate maximum noble gas and iodine release to the fuel-cladding gap of light-water-cooled reactors under steady state operating conditions. This method was evaluated by comparing calculated values with noble gas capsule data from a number of fuel pins. The method was found to be a rapid means of determining release for any fuel heat rating.

TECHNICAL ASSISTANCE IN REACTOR SAFETY ANALYSIS

The program of Technical Assistance in Reactor Safety Analysis provides analytical assistance to the AEC and its regulatory agencies. The scope of the program includes: (a) application of analytical models for determining reactor system behavior and evaluating engineered safety system performance under loss-of-coolant accident conditions for commercial nuclear plants of current or advanced design and (b) assessment of these analytical models by comparing calculated results with experimental data. Currently available computer codes and experimental information are employed; code development and experimental work are not included in the scope of this program. Specific

studies completed during this reporting period include: (a) evaluation of analytical capabilities for predicting subcooled blowdown behavior, (b) loss-of-coolant accident analysis for a large pressurized water reactor, (c) development of correlations for predicting the maximum containment pressure following an LOCA, and (d) evaluation of the predictive capabilities of the CONTEMPT-PS code.

In the first study, the WHAM code was evaluated through comparisons of experimental and analytical results of a semiscale blowdown test. The results of this study led to the conclusion that, although WHAM was not designed to handle two-phase situations, it is capable of providing valuable subcooled pressure predictions even though two-phase conditions are moving in the blowdown pipe. The initial, and most important, decompression wave for analysis of subcooled loads was predicted accurately with WHAM.

In the second study, the RELAP3 and THETA1-B computer codes were used to analyze the response of a nuclear power plant subjected to a fully open, double-ended offset shear of one primary coolant inlet pipe. Detailed results are presented as plots for a base case situation and for several RELAP3 and THETA1-B change cases.

A correlation was developed to predict the maximum containment pressure following an LOCA in a large PWR. The correlation can be used to estimate readily the maximum containment pressure from selected primary system and containment parameters and is an independent means of evaluating pressure for design studies, licensing reviews, and for rapidly checking more detailed analysis.

The methods used to model the most significant portions of the Humboldt Bay and Bodega Bay Test assemblies with the CONTEMPT-PS computer code are described. The results of the prediction of several variables were critically compared with available test data. The conclusion reached was that the CONTEMPT-PS computer code is conservative; that is, it overpredicts the peak dry-well pressure with respect to most of the test data when the computer runs are made with the assumptions that no heat transfer is allowed, the air in the dry well is forced out early in the transient, and essentially 100% of the liquid water is removed from the dry well during the transient.

SEPARATE EFFECTS TESTING

The purpose of the separate effects testing program [which includes the Semiscale Blowdown and Emergency Core Cooling (ECC) Project, the Full Length Emergency Cooling Heat Transfer (FLECHT) Tests Projects for pressurized and boiling water reactors (PWR-FLECHT and BWR-FLECHT), and the Scoping Emergency Cooling Heat Transfer (SECHT) Test Project] is to provide experimental data which serve as a basis for developing and assessing the analysis techniques. Existing and planned experiments cover the time during a loss-of-coolant accident from initiation of a break in the primary system until the emergency core cooling systems (ECCS) have reflooded the core. The experiments are designed to provide data for evaluating the analytical techniques applicable to both pressurized and boiling water reactors.

Tests involving core heat and tests involving core heat with emergency core coolant (ECC) injection were performed in the single-loop semiscale system. Representative results are presented and discussed. Although the results are not directly applicable to a PWR core, they are useful for assessing the adequacy of analytical techniques used for predicting loss-of-coolant accident response. Three tests were conducted with an ECC accumulator added to the single-loop system. In the tests, the electrically heated core was not covered by emergency coolant even though sufficient water was injected into the system to cover the core.

A new semiscale system (the 1-1/2 loop semiscale system) including a 66-inch electrically heated core, a complete operating primary loop and a passive second loop, and system geometry, loop volumes, and pressure drops which model the LOFT system is being constructed.

The purpose of the BWR-FLECHT Project was to provide experimental data concerning the thermal and hydraulic phenomena associated with terminating an LOCA core thermal transient by means of ECC in boiling water reactors. Both top spray (top injection) and core flooding (bottom injection) ECC systems were investigated in tests using both stainless steel and zircaloy-clad fuel rod simulators. Three categories of studies were conducted during 1970: (a) analysis of the data for top spray cooling tests performed with stainless steel-clad bundles, (b) theoretical investigation of the feasibility and limitations of testing with zircaloy-clad bundles with molybdenum filaments at high temperatures and the actual performance of the tests and evaluation of the test results, and (c) parametric testing of stainless steel-clad bundles at system pressures up to 300 psi with both top spraying and bottom flooding modes of ECC injection. The top spray cooling tests provided data regarding the magnitudes of the overall heat transfer coefficients as well as the components of the overall coefficient. In the zircaloy-clad bundle tests sudden temperature excursions were experienced. Neither pretest predictions nor posttest analysis using SINDA indicated temperature increases equivalent to those measured. Metallurgical factors caused by the interaction of the aluminum-oxide insulator and the zircaloy cladding, alloying of the zirconium and aluminum to form a liquid phase, and reaction of the alloy with steam, were considered responsible for the temperature excursions. In the parametric tests of the stainless steel-clad bundle both top spray and bottom flooding ECC effectiveness, in general, increased with increasing system pressure.

The purpose of the PWR-FLECHT Project is to provide experimental data from which core reflooding heat transfer coefficients can be obtained and from which heat transfer correlations can be developed to predict the thermal response of PWR cores from the time ECC fills the lower plenum until the core is reflooded. All tests were conducted using 12-foot electrically heated stainless steel- or zircaloy-clad rod bundles of 49 or 100 rods. Although the temperature rises experienced in PWR-FLECHT are not directly applicable to pressurized water reactors, the data do make evident one general and favorable characteristic of the ECC process -- the higher the initial bundle temperature, the lower the temperature rise. This characteristic behavior is a result of increased steam generation and water entrainment as the temperature difference between the rod surface and the coolant increases. Also, in general, the temperature rise varies inversely with flood rate.

During this reporting period, data obtained earlier from SECHT III and IV series tests were evaluated and an attempt was made to relate results of zircaloy-clad bundles to those of stainless steel-clad bundles. Conclusions reached relative to both the separate test series and to the zircaloy and stainless steel comparison are as follows. Bottom flooding is an adequate technique for cooling electrically simulated nuclear fuel rod bundles as long as the bundle is not sufficiently large as to induce significant radial flows. At all initial bundle temperatures investigated, the temperature excursion was arrested or turned at elevations of nine inches or higher, before the liquid level could have reached that elevation had the bundle been cold. This phenomenon was due to liquid entrainment in the steam flow generated by the heated rods. The time to terminate the temperature transient was inversely proportional to initial bundle temperature. For a given cold flooding rate, the higher the initial bundle temperature, the smaller the effect of bundle power density on turnaround time. A constriction over the exit of the vessel aided cooling at the low flooding rate and retarded cooling at the high flooding rate. Stainless steel- and zircaloy-clad heaters exhibited quite similar behavior up to 1800°F. The magnitudes of the axial turnaround and quenching temperatures for stainless steel-clad heaters was equivalent to those achieved with zircaloy-clad heaters. The major difference between the two heaters was associated with turnaround time. The temperature turnaround time for zircaloy-clad heaters decreased in tests performed after an elevated temperature test because of permanent deformation of the cladding. Sequential testing from low to high temperatures is thus required with zircaloy-clad heaters for the results to be comparable to those from stainless steel-clad heaters.

INCIPIENT FAILURE DETECTION BY ACOUSTIC EMISSION

The specific goal of the Incipient Failure Detection Project has been the development of a nondestructive test system that would provide integrity analysis of an assembled reactor pressure vessel and primary pressure system components of large nuclear power plants by detecting, locating, and describing incipient failure conditions. The incipient failure detection system (IFDS) developed for this purpose and the phenomena on which this system is based are described. A test program was conducted consisting of the following tests: (a) feasibility investigations on the PM-2A reactor vessel, (b) laboratory tests on tensile specimens and small pressure vessels, (c) engineering-scale tests on pressure vessels at the National Reactor Testing Station (NRTS), and (d) in-plant tests of complex chemical and nuclear pressure vessels at various manufacturing and operating facilities. The PM-2A feasibility test results confirmed that acoustic signals emanating from a flaw in a complex vessel can be detected and the source located through triangulation techniques. The detection threshold was 0.1 to 30% of normal operating stress and 0.02 to 5% of the nominal material yield stress. Laboratory tests on jugs in small hydrostatic loops indicated differences in the total acoustic energy release from annealed and embrittled vessels and confirmed the potential of acoustic emission data for metal-condition analysis. Testing of tensile specimens in support of LOFT showed that the IFDS could detect one-micron voids forming around material inclusions and could identify the stress levels at which ductile rupture was initiated and terminated. In-plant field tests of the IFDS were conducted in conjunction with engineering-scale acoustic test series to evaluate fully the IFDS. Data accumulated in SM-1A tests during preanneal and postanneal hydrostatic tests included clean, clear signals of both the low-amplitude deformation and the high-amplitude burst emissions. However, system noise inter-

ferred with data acquisition during the annealing phase when the SM-1A primary pumps were operated. Tests on the Elk River nuclear power plant confirmed the presence of leaking cracks that had previously been located by other techniques. In general, satisfactory techniques and equipment were developed to provide: (a) the necessary sensitivity for detecting and localizing the origin of acoustic signals to within 1% of the vessel dimensions, (b) the capability of excluding typical in-plant electrical and mechanical noise interference, (c) the capability of distinguishing between significant and insignificant flaws and between flaws and other sources of emission, such as leaks and material deformation emission, (d) a method of producing continuous records suitable for comparison with those of future inspections, and (e) timely on-line displays and data interpretation.

IN-PLANT TESTING -- THE CAROLINAS VIRGINIA TUBE REACTOR CONTAINMENT TESTING PROJECT

The Carolinas Virginia Tube Reactor Containment Testing Project consisted of vibration tests, leakage rate tests, and steam injection tests to simulate design basis accident (DBA) conditions. These tests, conducted in a representative PWR containment, provided information applicable to the safety evaluation of power reactors. Actual testing in CVTR was completed prior to 1970; however, data analysis of the leakage rate and DBA tests and reporting continued into 1970; therefore, significant findings of those efforts are summarized in this report.

Over the pressure range investigated and for a particular leakage path, the CVTR containment leakage rate increased and decreased in a continuous manner, as predicted by established extrapolation equations. However, selecting established extrapolation equations may be unreliable for extrapolating containment leakage from lower pressures to expected DBA pressures because leakage paths and rates change during and between test series. A continuous leakage rate measurement system appears to be the most meaningful method of monitoring containment leakage because of changing leak paths. The leakage rate decreased with increasing temperature at an average rate of about 0.0028%/day-°F. Simulated DBA conditions appear to have had negligible effect on the containment leak tightness. The absolute method and the reference vessel method of measuring leakage rate were comparable with equivalent instrumentation; both methods quickly detected a known leak and indicated its size.

Maximum values for the condensing steam heat transfer coefficients were determined experimentally from the DBA tests to be a factor of three to five larger than would be predicted by the often-used Tagami correlation. The potential importance of compartmentation in containment response analysis was re-emphasized; significant temperature differences occurred within a region as well as between regions. The pressure reduction spray was demonstrated to lower effectively the pressure and temperature of the containment atmosphere following the simulated DBA steam injection. Although the DBA tests did not compromise containment integrity (leakage did not increase) indications were that the yield stress of the liner was approached. The CVTR-DBA tests and analysis demonstrated that the CONTEMPT containment response analytical model, which is representative of the state-of-the-art in the industry, will predict reasonably accurately the containment atmosphere pressure-temperature response provided correct assignment of condensing heat transfer and other key parameters is made.

RELIABILITY AND FAULT TREE APPLICATIONS

Fault tree methods and reliability engineering techniques are used at the National Reactor Testing Station (NRTS) principally to evaluate reactor protective and safeguard systems. The particular analytical technique "kinetic tree theory" with the associated computer programs is used to obtain quantitative probabilistic characteristics regarding the safety and reliability of these systems. Once the fault tree or block diagram is drawn, the kinetic tree theory technique automatically yields detailed time-dependent information for every component, for every minimal cut set (critical path), and for the system itself. The computer time needed for this detailed analysis is negligible. In actual application, the fault tree and reliability engineering techniques, with the quantitative evaluations of kinetic tree theory, have yielded results that verify that these types of safety and reliability analyses are of practical value. The specific applications in this report are to the SPERT IV protection system and to the PBF evacuation and poison injection systems.

PUBLICATIONS

A bibliography of all topical and technical progress reports issued during 1970 is presented along with a brief summary of the contents of each report.

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ANNUAL REPORT OF
THE NUCLEAR SAFETY DEVELOPMENT BRANCH
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I. LOFT INTEGRAL EXPERIMENTS

(P. R. Davis, R. D. Hentzen, C. E. Slater)

The LOFT Integral Test Program to be performed in the Loss-of-Fluid Test (LOFT) Facility[1] is part of the overall water reactor safety program of the Atomic Energy Commission. The LOFT program consists of a series of loss-of-fluid tests in which the emergency core cooling (ECC) system is operated both at design and degraded conditions. The design requirements of the LOFT experimental assembly have been developed to ensure that the following objectives will be met:

- (1) To provide data required for the evaluation and improvement of analytical methods currently used to predict (a) the loss-of-coolant response of large pressurized water reactors (PWR); (b) the performance of engineered safety features, especially emergency core cooling systems; and (c) the margins of safety inherent in the performance of engineered safety features.
- (2) To determine whether any unexpected events or thresholds occur in the response of either the plant or engineered safety features and to investigate and develop analytical techniques that adequately describe the unexpected events should they occur.
- (3) To provide experience in the application of RDT Standards and other standards and codes generally applicable to the large PWR by their use and application by the LOFT Project.

The LOFT experimental assembly contains many large PWR design features. However, if the inclusion of a given large PWR design feature added substantially to the cost or fabrication time of the LOFT system, or violated the physical limitations of the existing LOFT facility, and if that feature was not required to maintain suitable operating characteristics, analyses were conducted to determine whether the large PWR design feature was required to achieve acceptable system response during experimentation.

Analyses related to LOFT design were performed for a large PWR. These analyses are described and conclusions arising from them are presented. Included are analyses of: (1) the effect of pressurizer surge line inlet location in a large PWR on the loss-of-coolant accident (LOCA) response, (2) the effect of accumulator injection into the broken loop of a large PWR on the LOCA response, (3) the effect on the thermal-hydraulic response of a large PWR to a loss-of-coolant accident of incorporating the proposed LOFT steam

generator design into the PWR system. In all cases, the thermal behavior of the core during the LOCA was the primary effect under consideration.

Each of the three analyses relied upon the analytical results obtained from the RELAP3[2] computer code, using appropriate modifications to the large PWR system model used for the studies. Emphasis is placed on the fact that these studies were performed to assist in choosing a final design for the LOFT facility. Since scaling effects are possible, similar analyses are to be performed on computer models of the final LOFT design.

These analyses led to the following conclusions: (a) whether the pressurizer surge line inlet was located in the broken loop or in one of the unbroken loops did not substantially change the core thermal response during an LOCA; (b) assuming the accumulator injects coolant into the broken loop during an LOCA did not substantially alter the system decompression or core thermal response from the case for which no accumulator injection into the broken loop was assumed; (c) a steam generator of the LOFT design, even though not typical of large PWR design, does not significantly affect system decompression and core thermal response during an LOCA.

Section 1 discusses the use of the RELAP3 code for these studies. Sections 2 through 4 discuss the individual studies and show the results. Section 5 gives the conclusions reached.

1. RELAP3 MODEL UTILIZED FOR THERMAL-HYDRAULIC ANALYSIS

RELAP3 is a computer program developed to describe thermal-hydraulic conditions within the reactor primary system. The code represents the reactor primary system as a set of arbitrarily connected fluid volumes that describe the reactor core, plenums, heat exchangers, and piping. Each connection between volumes may be specified as a normal junction, a leak junction, or a fill water source. A normal junction may include a pump or valve or both.

The RELAP3 version used in these analyses was limited to a maximum of 20 control volumes. The comparative analyses were performed by employing appropriate modifications to a basic 19-volume model of a typical large PWR. This model is shown in Figure 1. In the model, the four-loop reactor primary system has one broken loop with the other three loops lumped together into one unbroken loop to reduce the number of fluid volumes in the RELAP3 model. The code requires an appropriate set of initial flow conditions. The initial conditions used were full power, steady state conditions of a typical large PWR system. Some pertinent input parameters are given in Table I. The break of the piping was initiated by RELAP3 at 0.01 second and occurred between Volumes 13 and 2 in the computer model shown in Figure 1. All breaks analyzed were 100% double-ended breaks of the inlet piping.

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FIG. 1 RELAP 3 MODEL OF TYPICAL FOUR-LOOP LARGE PWR.

TABLE I

INPUT CONDITIONS FOR THE RELAP3 MODEL OF A TYPICAL LARGE PWR

Pressurizer Pressure	2249.7 psia
Hot Leg Temperature	616.7 °F
Cold Leg Temperature	552.5 °F
Core Flow	3.5139×10^4 lb/sec
Bypass Flow	1.722×10^3 lb/sec
Break Size	100% double-ended break of inlet piping
Break Location	Inlet piping 8 ft from reactor vessel
Discharge Coefficient	0.6
Time Delay Before Initiation of Injection Systems (High pressure injection system and low pressure injection system)	25 sec
Bubble Rise Models	Phase separation model used in upper plenum, pressurizer, and accumulators with a slope parameter, C_0 , of 0.8 and bubble escape velocity, v , of 3 ft/sec. Homogeneous models ($C_0 = 0$, $v = 0$) used for all other volumes.

2. EFFECT OF PRESSURIZER SURGE LINE INLET LOCATION ON CORE THERMAL-HYDRAULIC RESPONSE OF A LARGE PWR TO A LOSS-OF-COOLANT ACCIDENT

The pressurizer surge line of a multiple-loop, large PWR is connected to only one of the loops. Thus, for an LOCA the pressurizer surge line could be connected to either the broken loop or one of the unbroken loops. Since the pressurizer volume is typically only 10% of the total system volume, and since the flow area of the pipe into which the surge line discharges is about four times the area of the pressurizer surge line, little difference was expected between the case with the pressurizer discharging into the broken loop and the case with the pressurizer discharging into one of the unbroken loops.

The objective of this analysis was to determine whether LOFT, based on a design that included a pressurizer in the unbroken loop only, could adequately simulate large PWR response to a break in any loop. This analysis was performed for a model of a large PWR; later analyses are planned in which RELAP3 models of the smaller LOFT system will be employed. This study used the basic 19-volume RELAP3 model of the large PWR reactor system and compared the response of the system shown in Figure 1, in which the pressurizer was connected to the unbroken loop (Volume 16), to the response of the otherwise identical system in which the pressurizer was connected to the broken loop (Volume 9).

The results of this study are presented in Figures 2 through 6. The plots give the flow both into the bottom of the core and out the top of the core (Figures 2 and 3), upper plenum pressure (Figure 4), pressurizer pressure (Figure 5) and the average surface temperature of an average fuel rod (Figure 6)[a]. In Figures 2 and 3, two scales were used to permit better comparison of both magnitude and fluctuation of the predicted flow rates.

Examination of Figures 2 through 6 indicates that pressurizer location has insignificant effect on the hydraulic response of the reactor primary system and the thermal response of the core during an LOCA. Differences in either the upper plenum pressure or the pressurizer pressure were not detectable. Although slight differences can be seen in the core flow (Figures 2 and 3), the average surface temperature of the average fuel rod is not significantly affected by the pressurizer location. Other break locations would not be expected to lead to a different conclusion.

FIG. 2 CORE INLET FLOW RATE.

[a] An average fuel rod is one having the core average cladding temperature.

FIG. 3 CORE OUTLET FLOW RATE.

FIG. 4 PRESSURE IN UPPER PLENUM OF REACTOR VESSEL.

FIG. 5 PRESSURIZER PRESSURE.

FIG. 6 SURFACE TEMPERATURE OF AVERAGE FUEL ROD.

3. EFFECT OF ACCUMULATOR COOLANT INJECTION INTO THE BROKEN LOOP ON CORE THERMAL-HYDRAULIC RESPONSE OF A LARGE PWR TO A LOSS-OF-COOLANT ACCIDENT

This study was performed to investigate the need for an accumulator in the broken loop of the LOFT design.

The 19-volume RELAP3 model shown in Figure 1 was again utilized. Comparison was made between results obtained through use of the basic model and results obtained through use of a model identical except for the deletion of the broken loop accumulator (Volume 1).

The results of this study are shown in Figures 7 through 12. Figures 7 and 8 depict the flow into the bottom and the flow out of the top of the core. The average surface temperature of an average fuel rod is shown in Figure 9. Upper plenum pressures are compared in Figure 10, and leak flows out the pump side of the double-ended pipe rupture are presented in Figure 11. In Figures 7 through 11, the comparisons show only slight differences, with Figures 9, 10, and 11 indicating that differences were not detectable for the average surface temperature of the average fuel rod, upper plenum pressure, and leak flow out the pump side of the break. Figures 7, 8, and 12 have two scales for flow rate to permit better comparison of both magnitude and fluctuation.

FIG. 7 FLOW RATE INTO BOTTOM OF CORE.

FIG. 8 FLOW RATE OUT TOP OF CORE.

FIG. 9 AVERAGE SURFACE TEMPERATURE OF AVERAGE FUEL ROD WITH OR WITHOUT ACCUMULATOR IN BROKEN LOOP.

FIG. 10 UPPER PLENUM PRESSURE WITH OR WITHOUT ACCUMULATOR IN BROKEN LOOP.

FIG. 11 LEAK FLOW RATE OUT THE PUMP SIDE OF DOUBLE-ENDED PIPE RUPTURE WITH OR WITHOUT ACCUMULATOR IN BROKEN LOOP.

FIG. 12 LEAK FLOW RATE OUT THE VESSEL SIDE OF DOUBLE-ENDED PIPE RUPTURE WITH OR WITHOUT ACCUMULATOR IN BROKEN LOOP.

Figure 12 presents the leak flow out the vessel side for a double-ended pipe rupture. The comparison shows a considerable difference in leak flow after about 13 seconds. This difference is caused by initiation of flow from the broken loop accumulator at that time. This flow enters the piping and is quickly forced out the vessel side of the double-ended pipe rupture.

Although Figure 12 indicates considerable difference in the leak flow rate from the vessel side of the break, the core flow rates and temperatures show that no significant effect arises when the accumulator is removed from the broken loop.

4. EFFECT ON THE CORE THERMAL-HYDRAULIC RESPONSE OF A LARGE PWR TO A LOSS-OF-COOLANT ACCIDENT OF INCORPORATING THE PROPOSED LOFT STEAM GENERATOR DESIGN INTO THE PWR SYSTEM

Because of constraints imposed on the LOFT facility, the steam generator design proposed for LOFT differs from typical large PWR designs in the following aspects:

- (1) The ratio of tube volume to total volume of the steam generator in the proposed LOFT design is only 0.33 whereas a typical large PWR steam generator design has a ratio of 0.65 to 0.85.

- (2) The bundle height of the proposed LOFT steam generator is about seven feet whereas typical large PWR steam generators have bundle heights of about 38 feet.
- (3) The plenum heights of the proposed LOFT steam generator and typical large PWR steam generators are 2.8 and 5.5 feet, respectively.
- (4) Although the total pressure drop across the proposed LOFT steam generator for a design flow rate of 5.6×10^6 lb/hr is typical of large PWR steam generators, the pressure drops are distributed differently. In large PWR steam generators the major portion of the total pressure drop occurs across the tubes (including entrances and exits). In the proposed LOFT design the major portion of the pressure drop occurs in the plenums.

In order to model a plant with the proposed LOFT steam generator design, a modified version of the 19-volume RELAP3 model of a large PWR (Figure 1) was employed. This RELAP3 model used a 4-volume steam generator for the unbroken loops, replacing the 1-volume steam generator. The accumulator systems (Volumes 1 and 4 of Figure 1) were omitted because RELAP3 was limited to a total of 20 control volumes at the time this analysis was performed. With the omission of the two accumulator volumes and the replacement of the 1-volume steam generator in the unbroken loop by a 4-volume model, the 19-volume model (Figure 1) was transformed to a 20-volume model.

The analysis of the proposed LOFT steam generator design was performed by comparing the thermal-hydraulic response obtained by using a 4-volume model of a typical large PWR steam generator in the 20-volume RELAP3 model to the corresponding response obtained by replacing the steam generator by a 4-volume steam generator differing in the same manner as the proposed LOFT steam generator. The 4-volume model of the typical large PWR steam generator is shown in Figure 13. The steam generator model used to represent the proposed LOFT steam generator design in a large PWR is presented in Figure 14. This figure shows a reduction in height and a redistribution of the volumes and pressure drops with respect to the typical design. For determining effects caused by the atypical (LOFT) steam generator, comparisons made were of mass flow rates into and out of the steam generator, at the midlength of the tubes in the steam generator, and into and out of the core. In addition, the pressures in the steam generator cold plenum were compared for the two cases. These comparisons, in Figures 15 through 20, were made by using a pump coastdown friction factor of $1.42 \times 10^{-4} \text{ lb}_f\text{-sec}^2/\text{lbm-ft}^3\text{-m}^2$. Comparison calculations were repeated for a reduced pump coastdown friction factor of 3.84×10^{-6} . The latter comparisons, shown in Figures 21 through 25, were performed to determine whether the pump coastdown friction factor was masking effects produced by the steam generators because other loss-of-coolant accident analyses have shown that the pump coastdown friction factor significantly influences the early portion of blowdown. The figures comparing flow rates have two scales to permit comparison of both magnitude and fluctuation.

Pressure Drop Distribution (psi)

Hot Leg to Hot Plenum	2.3
Hot Leg to Tube 1	11.0
Tube 1 to Tube 2	5.0
Tube 2 to Cold Plenum	11.0
Cold Plenum to Cold Leg	2.3
Total	31.6 psi

$$\frac{\text{Tube Volume}}{\text{Total Volume}} = 0.68$$

INC-A-17144

FIG. 13 RELAP3 FOUR-VOLUME MODEL OF TYPICAL LARGE PWR STEAM GENERATOR.

Pressure Drop Distribution (psi)

Hot Leg to Hot Plenum	11.05
Hot Plenum to Tube 1	4.00
Tube 1 to Tube 2	1.50
Tube 2 to Cold Plenum	4.00
Cold Plenum to Cold Leg	11.05
Total =	31.60 psi

$$\frac{\text{Tube Volume}}{\text{Total Volume}} = 0.33$$

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FIG. 14 RELAP3 FOUR-VOLUME MODEL OF LARGE PWR STEAM GENERATOR BASED ON LOFT STEAM GENERATOR DESIGN.

FIG. 15 FLOW RATE INTO THE STEAM GENERATOR.

FIG. 16 FLOW RATE IN THE STEAM GENERATOR AT THE MIDLENGTH OF THE TUBES.

FIG. 17 FLOW RATE OUT THE STEAM GENERATOR.

FIG. 18 FLOW RATE INTO THE CORE.

FIG. 19 FLOW RATE OUT THE CORE.

FIG. 20 PRESSURE IN THE COLD PLENUM OF THE SIMULATED LOFT STEAM GENERATOR.

FIG. 21 FLOW RATE INTO THE STEAM GENERATOR USING REDUCED PUMP COASTDOWN FRICTION FACTOR.

FIG. 22 FLOW RATE IN STEAM GENERATOR AT THE MIDLENGTH OF THE TUBES USING REDUCED PUMP COASTDOWN FRICTION FACTOR.

FIG. 23 FLOW RATE OUT THE STEAM GENERATOR USING REDUCED PUMP COASTDOWN FRICTION FACTOR.

FIG. 24 FLOW RATE INTO THE CORE USING REDUCED PUMP COASTDOWN FRICTION FACTOR.

FIG. 25 FLOW RATE OUT THE CORE USING REDUCED PUMP COASTDOWN FRICTION FACTOR.

When the larger pump coastdown friction factor was used, comparison of mass flow rates into the steam generator (Figure 15) shows that the flow rate into the typical steam generator is slightly higher during the first four seconds and exhibits more fluctuation than the flow rate into the large steam generator of the LOFT design. Figure 16 indicates that flow rates at the midlength of the steam generator tubes are nearly identical to nearly 11 seconds. At that time a small peak in the flow rate for the proposed LOFT design occurs. After 13 seconds the flow rates again became nearly identical. Flow rates out the steam generators are essentially identical for the two cases (Figure 17), except for small fluctuations at about 11 seconds in the PWR model based on the LOFT steam generator design.

Figures 18 and 19 present the flow into and out of the core, respectively, for the case in which the larger pump coastdown friction factor was used. The only significant difference between results using the two different steam generators is that the sharp flow pulses occur about 0.2 second later for the case using the proposed LOFT steam generator design. The delay can be attributed to a change in the response time of the total system due to a change in the effective length-to-area ratio for the steam generator. Also, the differences in the pressure drop distribution between the two cases would be expected to affect the system response time somewhat.

In general, the changes in primary system flow produced by the introduction of a steam generator having the proposed LOFT design were small in magnitude. The largest changes were in the flows within the steam generator.

The pressure histories in the cold plenum of the steam generators are presented in Figure 20. During the first five seconds of the saturated portion of blowdown the pressure in the cold plenum of the steam generator having the proposed LOFT design decreases more slowly than the pressure in the cold plenum of the typical large PWR steam generator, but otherwise the pressure histories are quite similar.

The results obtained by using the smaller pump coastdown friction factor are given in Figures 21 through 25. Although the core flow was altered by the smaller pump coastdown friction factor it was not significantly affected by the differences in steam generator design. The pump apparently did not mask any changes introduced by the LOFT steam generator design.

In general, the introduction of the proposed LOFT steam generator design produced some differences in the predicted thermal-hydraulic response of a large PWR to an LOCA. However, the atypical steam generator did not significantly affect the calculated core flow rate and, therefore, no significant effect upon core thermal response is expected.

5. SUMMARY AND CONCLUSIONS

Comparative thermal-hydraulic analyses related to LOFT design have been performed using a RELAP3 model of a typical large PWR system for a 100% inlet pipe rupture. The studies include: (a) the effect of pressurizer surge line inlet location on core thermal-hydraulic response of a PWR to a loss-of-coolant accident, (b) the effect of the presence or absence of the accumulator in the broken loop upon the thermal-hydraulic response of a large PWR to a loss-of-coolant accident, and (c) the effect of a steam generator of the proposed LOFT design on the thermal-hydraulic response of a PWR to a loss-of-coolant accident.

The calculations indicate that (a) the location of the pressurizer surge line inlet, in either the broken loop or one of the unbroken loops, has insignificant effect upon the thermal-hydraulic response of a large PWR to an LOCA; (b) the omission of the accumulator from the broken loop does not significantly affect the core thermal-hydraulic response of a large PWR to an LOCA; and (c) the use of a steam generator of the proposed LOFT design does not significantly change the thermal-hydraulic response of a large PWR to an LOCA from that expected using the typical large steam generator design.

The analytical studies indicate that the LOFT facility can be expected to simulate the thermal-hydraulic response of a large PWR to an LOCA with out an accumulator or pressurizer in the broken loop and with a steam generator of the proposed LOFT design.

II. LOSS-OF-COOLANT ACCIDENT ANALYSIS -- THERMAL MODEL DEVELOPMENT

(D. R. Evans, C. J. Hocevar, E. D. Hughes, R. F. Jimenez, J. R. Larson)

The purpose of the fuel rod thermal response model development is to provide the capability to predict the thermal response of the reactor core and the associated mechanical behavior of the fuel element cladding during an LOCA and during ECC injection. Since the mechanical integrity of the cladding depends strongly on the fuel rod thermal response characteristics, the heat transfer coefficients need to be predicted accurately during blowdown and ECC injection.

The core thermal response task considers the following core thermal events (a) blowdown cooling (pre-CHF cooling and post-CHF cooling), (b) core heatup, and (c) ECC cooling (vessel reflooding).

For core cooling during blowdown, coupled thermal-hydraulic models are used to predict heat transfer to, and from, the cladding. Thermal-hydraulic models are used to produce input conditions for the core heatup and the cladding mechanical response analyses.

Section II-1 describes the development of a single-rod, single-channel code (THETA1-B) for calculating the thermal-mechanical response of the fuel rod cladding. The single-rod, single-channel code is intended for computing the fuel rod temperature distribution, fuel rod internal gas pressure, cladding strain and thermal expansion, and the fluid temperature and behavior for a single fuel rod and coolant channel. Calculations performed with the code indicated that the large inlet break produces the maximum cladding temperature following an LOCA for a typical large PWR.

Section II-2 describes a study on the use of critical heat flux data and correlations in subchannel codes for core thermal response analysis. This study was performed as part of the blowdown cooling code development effort.

Section II-3 briefly describes a multidimensional, multirod, thermal-hydraulic code (MOXY) which has been developed and evaluated for core heatup analysis. The code considers conduction in the fuel rod, thermal radiation between fuel rod surfaces and control rods, barrels and grids, and convection heat transfer to the coolant. Calculations performed with the code are compared with experimental data.

Section II-4 describes a coupled thermal-hydraulic code which is being developed for predicting the rate of energy removal from the core and the extent of metal-water reactions during the ECC cooling period. Data from the PWR-FLECHT project were used to produce a correlation for predicting heat transfer coefficients for ECC reflooding.

1. THETA1-B -- FUEL ROD HOT-SPOT THERMAL RESPONSE

The THETA1-B[3] code was used to calculate the temperature response of the fuel rod hot-spot in a typical four-loop large PWR during a postulated

loss-of-coolant accident. The accident condition assumed for the study was an offset shear rupture of an inlet pipe.

The THETA1-B code solves the energy transfer equations for the fuel rod and the adjacent coolant channel[3]. The fluid pressure, mass flux, and inlet enthalpy during the transient are needed as boundary conditions. These quantities are determined through use of the RELAP3[2] code which considers the dynamic behavior of the fluid in the entire primary system. Since THETA1-B solves only the energy equation and not the coupled set of conservation equations for fluid, several simplifying assumptions are required. The average core pressure as computed by RELAP3 is used as the pressure in the fluid channel. The pressure and mass flux are assumed to be uniform throughout the channel at any instant in time. RELAP3 calculations show that the fluid may be flowing into, or out of, the core at both ends simultaneously during portions of the transient. Simultaneous flow implies that a stagnation point exists at some location in the core. For safety analysis, the flow rate in the core during these periods is assumed to be zero. When the flow is upward through the core (positive) the mass flux and the enthalpy at the junction of the lower plenum and core are input to THETA1-B from RELAP3; and when the flow is downward through the core (negative), the mass flux and enthalpy at the junction of the upper plenum and core are input. The pressure, mass flux, and inlet enthalpy histories used during this analysis are shown in Figure 26.

FIG. 26 INPUT QUANTITIES FOR THETA1-B.

The power generation rate in the fuel rod is also required for THETA1-B calculations. This quantity is obtained from the reactor kinetics model in the RELAP3 code. The power generation rate drops rapidly to about 7% of the normal operating power after a loss-of-coolant accident is initiated and then decreases slightly during the remainder of the transient.

The thermal response of the fuel rod and the fluid channel calculated by THETA1-B for a typical large PWR during the decompression transient is shown in Figure 27. The heat transfer coefficient at the rod-fluid interface is also presented.

FIG. 27 FUEL ROD AND COOLANT THERMAL RESPONSE.

The critical heat flux is exceeded early in the transient (about 30 msec) and a corresponding large decrease in heat transfer coefficient occurs at the rod surface. As shown in Figure 26, the flow rate drops to and remains essentially zero from 0.03 to 0.85 sec. Some flow oscillations do occur during this period but their duration is short. During this period the heat transfer varies between flow film boiling and pool film boiling regimes. The heat transfer coefficient for film boiling during this period is low compared to the pre-CHF nucleate boiling coefficient.

Because of the low thermal inertia of the cladding and the decreased heat transfer coefficient at the cladding surface, the cladding temperature rises rapidly to a level that is approximately equal to that of the fuel pellet surface. The steady state temperature difference across the fuel rod gap at the hot spot is about 280°F. At this time a large temperature gradient still exists in the fuel pellet. During the remainder of the transient the cladding surface temperature response is controlled by the redistribution of sensible energy in the fuel rod, the decay heating in the rod, and the film boiling mechanism at the cladding-fuel interface.

Between 0.85 and 2.5 sec, flow through the core increases and causes an increase in the heat transfer coefficient and a decrease in the cladding temperature. From 2.5 to 3 sec the core flow rate decreases and the cladding temperature again rises. Between 3 and 6.5 sec flow through the core is zero except for small flow pulses, and during this period, the coolant adjacent to the hot spot is superheated. The surface heat flux is very low and the cladding temperature rises as energy is redistributed in the fuel pellet. The flat spot in the temperature profile is caused by a phase change in the zircaloy cladding material. At approximately 6.5 sec, flow through the core begins to fluctuate sufficiently to reduce the rate of temperature rise of the cladding. However, the cladding temperature continues to rise until a maximum temperature of 2200°F is reached between 9.5 and 10 sec.

Shortly after 10 sec, the flow calculated by RELAP3 begins to fluctuate more violently and eventually negative flow is established.

For times longer than about 30 sec, flows and heat transfer coefficients are low. Decay heat produces an increasing fuel rod temperature as can be seen in Figure 27.

2. CRITICAL HEAT FLUX ANALYSIS

Studies were performed to determine the uncertainties associated with using average fluid conditions rather than local subchannel fluid conditions for predicting the onset of CHF in core thermal analysis. The usual procedure in the past was to use bundle average conditions in the CHF correlations. However, localized conditions can now be supplied by computer codes with subchannel analysis capability. Examples of these codes are HAMBO[4,5], COBRA[6], and THINC[7,8]. These codes use the experimental geometric details and operating conditions to obtain mass flux, enthalpy, and void fraction in a subchannel in which CHF is reported to occur. These fluid conditions are then used with heat flux data to derive the CHF correlations. The correlations in turn are used in computer codes for predicting CHF in rod arrays.

In a subchannel code for core thermal analysis, the rod bundle is divided into subchannels. Figure 28 shows a typical division into subchannels for a rod array within a shroud. In addition, the heated length is divided into a number of axial levels. The code solves the conservation equations for the fluid for each subchannel at each axial level to obtain local values for mass flux and fluid state as the coolant travels through the bundle. Thus the temperature, enthalpy, pressure, and void distributions both radially and axially are determined. Empirical correlations are used to predict coolant cross-flow from subchannel-to-subchannel, void fraction, liquid-vapor slip ratios, pressure drops, local values of the heat transfer coefficient, spacer effects, and the critical heat flux.

The data of Israel^[9] has been used to study the uncertainties associated with the use of average bundle fluid conditions rather than detailed subchannel conditions in the CHF correlations. The experiments reported by Israel were conducted with test channels, rod sizes, and clearances corresponding to those of BWR fuel assemblies. Tests were conducted to determine the effects on CHF of subchannel power factors, spacers (type and location), and location of the rods within the shroud. Figure 29 shows the experimental configuration and power distribution of two test sections (Test Section 1 and Test Section 2) used by Israel. The rod power factor of Test Section 1 is indicated on each rod; that of Test Section 2 is included in parenthesis on those rods for which it differed from that of Test Section 1. The maximum-to-average heat flux ratios are equal for the two test sections. However, a difference exists in the power factors for Subchannel 2 and for Subchannel 3 with the power factor being about 4% higher for Test Section 2.

FIG. 28 ROD BUNDLE WITH SHROUD AND POSSIBLE SUBCHANNELS.

FIG. 29 CONFIGURATION OF TWO TEST SECTIONS USED BY ISRAEL TO SHOW EFFECT OF SUBCHANNEL CONDITIONS ON CHF.

The effect of the differences in subchannel power factors on CHF is shown in Figure 30 in which the critical heat flux, q_{CHF} , is plotted as a function of the bundle average exit quality x_{CHF} for a mass flux of $G=1.0 \times 10^6 \text{ lb}_m/\text{hr-ft}^2$. Data for the two test sections taken for the same inlet subcooling are connected with the dashed lines. Figure 30 shows that the higher peaking factor produces lower critical heat flux with other factors constant. Other results, discussed in subsequent paragraphs, indicate that the effect is larger at higher mass fluxes. As the bundle average exit quality decreases, the difference in CHF decreases with some indication that the difference approaches zero at qualities less than about 10%.

Figure 31 shows the critical heat flux for the two test sections as a function of the inlet subcooling with mass flux as a parameter. Exit quality, which is the independent variable for Figure 30, is a function of these two quantities. For the low value of the mass flux, $G = 0.5 \times 10^6 \text{ lb}_m/\text{hr-ft}^2$, practically no difference occurs in q_{CHF} values for the two test sections. For higher values of G , increased differences in q_{CHF} for the two test sections are observed.

Also shown in Figure 31 with the data are linear least-squares curve fits to all data for each mass flux. The maximum deviation from these fits occurs at $G = 1 \times 10^6 \text{ lb}_m/\text{hr-ft}^2$ and an inlet subcooling of 25 Btu/lb_m. At these conditions the data for Test Section 1 (the cooler hot subchannel) are 5% above the curve and the data for Test Section 2 (the hotter hot subchannel)

FIG. 30 EFFECT OF SUBCHANNEL POWER FACTORS ON q_{CHF} AS A FUNCTION OF BUNDLE AVERAGE EXIT QUALITY AT $G=1.0 \times 10^6 \text{ LB}_m/\text{HR-FT}^2$.

FIG. 31 EFFECT OF SUBCHANNEL POWER FACTOR ON q_{CHF} AS A FUNCTION OF INLET SUBCOOLING.

are 5% below the curve. The differences in q_{CHF} are observed to decrease as the inlet subcooling increases at all values of the mass flux. Thus, for a subchannel analysis code to predict the small differences in critical heat flux for the two test sections, an accurate understanding of all thermal-hydraulic processes occurring within the subchannels of the rod array is necessary.

Figure 32 shows a comparison between the test data of Israel, for Test Section 1, and CHF predictions with the Barnett[10,11] correlation employing bundle average fluid conditions. The comparison shows differences between the data and the predicted dependence of critical heat flux on the inlet subcooling. Most of the test results are overpredicted, but the errors are small and are within the accuracy required for LOCA analysis.

Israel et al[9] also conducted experiments to determine the effect of rods placed eccentrically within the shroud. For these experiments the array of Figure 29 was skewed to the upper left hand corner of the shroud by reducing the spacing between the rods and the shroud from 0.126 to 0.090 inch along the two walls. In addition, the top 12 inches of the highest powered corner

FIG. 32 COMPARISON OF BARNETT CORRELATION PREDICTIONS WITH EXPERIMENTAL DATA FROM TEST SECTION 1.

rod of Figure 29 was bowed so that the rod-to-shroud clearance was reduced to 0.060 inch. Figure 33 shows a comparison of the critical heat flux for the skewed and nonskewed Test Section 1. The experimental results indicate that the critical heat flux for the skewed bundle is lower than that for the nonskewed bundle (Figure 32). Since the Barnett correlation overpredicts the results for the nonskewed case, it will also overpredict the results of the skewed bundle tests.

At present, many CHF correlations exist in the literature for prediction of critical heat flux in single channels. Examples are correlations based on circular and rectangular conduits and annuli, and correlations based on rod bundles with uniform and nonuniform axial and radial heat flux distributions. Some of the correlations were derived from data obtained in several different geometries with various flux distributions. The common practice for subchannel analysis codes is to program several correlations into the code.

Caution should be exercised in selecting a CHF correlation for subchannel analysis. For example, Lahey^[12], in his review of the use of empirical correlations in subchannel codes, concluded that the best approach for subchannel analysis is to use correlations based on subchannel data. In this

FIG. 33 COMPARISON OF CRITICAL HEAT FLUX FROM SKEWED AND NONSKEWED TEST BUNDLES.

connection, Bowring^[13], has found that single-channel correlations used in conjunction with the HAMBO code do not have $\pm 5\%$ predictive capabilities. Israel et al^[9] attempted to predict the rod bundle data by employing the Macbeth correlation for circular conduits^[14] in the COBRA code. The local mass flux calculated with COBRA (for the hot subchannel of Figure 29) was 20 to 30% less than the bundle average value and was used in the CHF correlation. Predicted values were higher than the observed data. These observations emphasize that care should be exercised in selecting a critical heat flux correlation for use in a subchannel code for core thermal analysis.

3. COMPARISONS OF CLADDING TEMPERATURES PREDICTED BY MOXY WITH THOSE OBTAINED FROM FHUST

A multidimensional, multirod thermal-hydraulic code, MOXY^[15], has been developed for core heatup analysis. The code considers conduction in the fuel rod, thermal radiation between fuel rod surfaces and control rods, barrels, and grids, and convection heat transfer to the coolant. A two-dimensional version of MOXY, which represents an array of pins in x-y coordinates, has been used for calculating values for comparison with data obtained from the Fuel Simulation Test (FHUST) Program^[16]. Steady state and transient cladding temperatures from FHUST are compared with computer-calculated cladding temperatures. The comparisons indicate that the assumptions in MOXY of gray diffuse surfaces and symmetry are realistic.

3.1 FHUST Experimental Arrangement

A cross section of the FHUST experimental assembly is shown in Figure 34. The FHUST test section consists of an 8 x 8 array of rods surrounded by a stainless steel radiation barrier. This assembly is located concentrically within the FHUST vessel, which is a jacketed stainless steel pipe. Water or steam in the jacket maintains the outside vessel wall at a constant temperature. The length of the FHUST test section is 36 inches. A FHUST simulated fuel rod is shown in Figure 35. The rods had uniform axial heat generation. Differences between FHUST rods and a nuclear fuel rod, which can cause differences in the transient responses of the rods, are as follows:

- (1) The heat source in the nuclear fuel rod is distributed throughout the fuel pellets, whereas in the FHUST rod it is concentrated in the central tungsten heater.
- (2) One gap (the fuel-cladding gap) occurs in a nuclear fuel rod, whereas two occur in the FHUST rod (the tungsten heater-insulator gap and the insulator-cladding gap).

The rod surface temperatures were measured with Chromel-Alumel thermocouples constructed of 5-mil wire welded to flame-sprayed niobium pads on the exterior of the rods. The heater elements were energized by direct current from storage batteries.

FIG. 34 CROSS SECTION OF FHUST EXPERIMENTAL ARRAY.

FIG. 35 CROSS SECTION OF FHUST SIMULATED FUEL ROD.

Two types of FHUST tests were conducted as follows:

- (1) Steady state tests in which power to the rods was adjusted so that the temperature of the four central rods was maintained at a predetermined level.
- (2) Transient tests in which power was applied initially to cool rods until the rod temperature had increased to a predetermined value.

3.2 MOXY Code Description

The MOXY code used for calculating values for comparison with the FHUST data is based on the assumption of one-eighth symmetry^[17]; it employs models which describe heat transfer due to thermal radiation, conduction, and convection by single-phase steam. Assumptions implicit in these heat transfer models are:

- (1) All surfaces are in thermal equilibrium during each time step
- (2) All surfaces are gray, diffuse, and opaque
- (3) All surfaces emit radiation uniformly and are irradiated uniformly
- (4) The medium between surfaces does not absorb, scatter, or emit radiation
- (5) Axial radiation interchange is negligible

- (6) Rod temperature is independent of axial and angular position
- (7) Heat generation in the heated regions is independent of position and temperature (any and all regions in the rod can be heated regions)
- (8) Extent of metal-water reaction is small.

3.3 Steady State Comparisons

Previously reported work including parametric analyses[18] was based on a version of MOXY that used a different conduction model than is presently used. Values for steady state temperature calculated with the improved conduction model in MOXY did not materially change from values calculated with the earlier version. Evaluation completed since the earlier steady state comparisons shows that the highly temperature-dependent tungsten heater resistivity, combined with relatively cold rod ends, results in an axial midplane power peaking factor of approximately 1.2. Use of this peaking factor in the calculations results in the good agreement at steady state conditions between calculated and experimental temperatures as shown by Figure 36 for the

FIG. 36 EXPERIMENTAL AND CALCULATED ROD SURFACE TEMPERATURE PROFILES FOR STEADY STATE CONDITIONS AND FHUST CENTRAL ROD TEMPERATURE OF 1500°F.

central rod at 1500°F. Calculated surface temperatures for a peaking factor of 1.0 are also shown for comparison. Each of the data points shown represents results obtained in both vacuum and argon environments. Since the results from the two environments were essentially the same at the same temperature and for the same energy input, natural convection heat transfer in argon appears to be negligible.

3.4 Transient Comparisons

Comparisons between calculated and experimental transient temperatures are presented for two cases. The first case, shown in Figure 37, was a transient test (Test 004) in which the rods were initially at room temperature. The electrical power was increased to a high value and maintained until the rod temperature reached approximately 1500°F, at which time the power was turned off. Stagnant argon was the atmosphere in the vessel. As can be seen, predicted temperatures are somewhat higher than the experimental temperatures at 800 sec and are quite close to experimental temperatures at 1600 and 2000 sec.

Figure 38 shows comparisons for the second case, a higher temperature test. In this test, the rod power was initially just sufficient to maintain the rod temperatures above the saturation temperature of the steam in the vessel. Condensing steam maintained the vessel jacket at a constant temperature. Power was increased to initiate the transient. A final rod temperature of about 2175°F was attained. The comparison between calculated and experimental temperature can be seen to be quite good, except at 300 sec at which time the calculated temperatures are somewhat high. The general agreement between rod temperatures calculated with MOXY and the rod temperatures observed in FHUST tests indicates that the assumptions employed in the model, particularly the assumptions of gray diffuse surfaces and symmetry, are realistic.

4. ECC HEAT TRANSFER CORRELATION BASED ON PWR-FLECHT TESTS

An empirical correlation for calculating heat transfer coefficients during ECC reflooding was developed on the basis of a limited number of experimental data from the Pressurized Water Reactor - Full Length Emergency Cooling Heat Transfer (PWR-FLECHT) Project^[19,20]. This correlation is a function of PWR-FLECHT test variables (time after flooding initiation, flooding rate, and axial position of node in question) and was developed by using a hypothesized equation with variable coefficients and exponents. The coefficients and exponents in the equation were varied until the equation gave a reasonable fit to the experimental data.

The final form of the empirical correlation derived from the PWR-FLECHT data for ECC reflooding heat transfer coefficients was the following:

$$h = 2.0 + \frac{11 t}{(Z+1)^{5/2}} F^{0.7(2/3 + Z/12)} + (0.05Z + 0.3)t F^{0.8} e^{-\left[\frac{t}{3Z+13}\right] F^{0.3}} \quad (1)$$

FIG. 37 EXPERIMENTAL AND CALCULATED ROD SURFACE TEMPERATURE PROFILES FOR FHUST TEST 004.

FIG. 38 EXPERIMENTAL AND CALCULATED ROD SURFACE TEMPERATURE PROFILES FOR METAL-WATER REACTION TEST.

where

h = film heat transfer coefficient (Btu/hr-ft²-°F)

t = time after initiation of flooding (sec)

Z = axial distance from bottom of core (ft), $\bar{Z} < 6$

F = flooding rate (in./sec).

Equation (1) was developed from data from selected PWR-FLECHT Group I[19] and Group II[20] experiments. These experiments utilized electrically heated rods with stainless steel cladding. Runs which exhibited lower values for heat transfer coefficients were chosen for development of Equation (1) in order to ensure the correlation would predict conservative, or low, values for coefficients. No coolant fallback occurred in any of the runs selected. (When coolant fallback occurs, the heat transfer coefficients are slightly increased.) Three tests with flooding rates of 6, 2, and 1 in./sec were used to derive the correlations. Conditions for these tests are indicated in Table II.

Equation (1) is specifically a function of time, flooding rate, and axial position and may not accurately account for the effect of other parameters on the film heat transfer coefficient. The correlation is based on and should be used with the difference between the localized temperature of the cladding and the saturation temperature of water at the pressure under consideration.

Figures 39, 40, and 41 show comparisons between heat transfer coefficients calculated from the PWR-FLECHT experimental data using the DATAR code[19] and the heat transfer coefficients predicted by Equation (1) at elevations of 2, 4, and 6 feet above the bottom of PWR-FLECHT bundle for flooding rates of 6, 2, and 1 in./sec. For a flooding rate of 2 in./sec (Figure 40) the calculated values are lower than the observed values for coefficients below 15 Btu/hr-ft²-°F. The calculated coefficients appear to be adequate for values above 15 Btu/hr-ft²-°F. For a flooding rate of 1 in./sec (Figure 41) the calculated values are slightly higher (> 15%) than the experimental values for the 4- and 6-foot elevations but are conservative (low) at the 2-foot elevation. The correlation does not track the upswing in the heat transfer coefficient that occurs shortly before quenching.

TABLE II

PWR-FLECHT RUNS WHICH FORM THE BASIS FOR EQUATION (1)

<u>Run</u>	<u>Thermo-</u> <u>couples</u>	<u>Initial</u> <u>Maximum</u> <u>Power</u> <u>(kW/ft)</u>	<u>Flooding</u> <u>Rate</u> <u>(in./sec)</u>	<u>Inlet</u> <u>Subcooling</u> <u>(°F)</u>	<u>Pressure</u> <u>(psia)</u>	<u>Initial</u> <u>Maximum</u> <u>Cladding</u> <u>Temperature</u> <u>(°F)</u>	<u>Temperature</u> <u>Rise</u> <u>(°F)</u>	<u>Turn-</u> <u>around</u> <u>Time</u> <u>(sec)</u>	<u>Bundle</u> <u>Size</u>
0812	3D3, 3D4 3D5	1.24	5.9	136	15	1807	55	7	7 x 7
7158	3D3, 3D4 3D5	1.24	2.0	149	54	2156	88	9	7 x 7
6553	3D3, 3D4 3D5	1.24	1.0	148	59	2012	278	62	7 x 7

FIG. 39 COMPARISON BETWEEN EXPERIMENTALLY DETERMINED HEAT TRANSFER COEFFICIENT AND HEAT TRANSFER COEFFICIENT PREDICTED BY EQUATION (1) FOR FLOODING RATE OF 6 IN./SEC.

FIG. 40 COMPARISON BETWEEN EXPERIMENTALLY DETERMINED HEAT TRANSFER COEFFICIENT AND HEAT TRANSFER COEFFICIENT PREDICTED BY EQUATION (1) FOR A FLOODING RATE OF 2 IN./SEC.

FIG. 41 COMPARISON BETWEEN EXPERIMENTALLY DETERMINED HEAT TRANSFER COEFFICIENT AND HEAT TRANSFER COEFFICIENT PREDICTED BY EQUATION (1) FOR A FLOODING RATE OF 1 IN./SEC.

III. LOSS-OF-COOLANT ACCIDENT ANALYSIS -- HYDRAULIC MODEL DEVELOPMENT

(H. D. Curet)

In the hydraulic model development effort digital computer codes are being developed for use in predicting the thermal-hydraulic response of light-water reactors to accidents such as the hypothetical major loss-of-coolant accident. The codes are intended to predict the fluid conditions associated with subcooled decompression, the decompression region between the subcooled and saturated fluid states, saturated two-phase fluid decompression, and emergency core coolant reflooding. These fluid conditions are then used as input to heat transfer codes for hot pin and hot channel analyses, to structural codes for calculations of loads on reactor internals and primary piping, and to fission product transport codes for determination of the amounts of fission products released during a nuclear accident.

Evaluations performed with the computer code RELAP3[2] are discussed in this section. The evaluations concern the coupling effects between the heat transfer and fluid flow in a reactor core. The coupling effects were found to be sufficiently significant that they should not be neglected in accident analysis of reactor systems.

1. EFFECT OF CORE HEAT TRANSFER REPRESENTATION ON THERMAL- HYDRAULIC BEHAVIOR DURING AN LOCA IN A LARGE PWR

(P. D. O'Reilly and C. E. Slater)

The effects of heat transferred from the core to the coolant on the blow-down behavior of a large pressurized-water reactor were investigated. The investigation employed the RELAP3[2] code to analyze a 100% inlet break for a large PWR. Two versions of RELAP3 were used in this analysis. The first version, the RELAP3-plate version, which used plate geometry quantities to represent the cylindrical fuel elements of a PWR contained a simple heat transfer model for the core volumes. The plate-type heat transfer model is the same as that used in the RELAP2[21] code. The second version, the RELAP3/MOD 0, included a cylindrical conduction model for the fuel element and a subroutine which determined the heat transfer coefficients for seven modes of heat transfer. This version also included critical heat flux correlations for six different ranges of pressure and mass flux conditions. In this analysis, a single volume was used in RELAP3 to represent the reactor core.

1.1 Analytical Results

A typical, large, four-loop pressurized-water reactor experiencing 100% inlet break conditions was modeled for these studies. The representation of the plant modeled by RELAP3 is presented in Figure 42. The initial conditions used are listed in Table III.

BROKEN LOOP

THREE UNBROKEN LOOPS

FIG. 42 RELAP3 MODEL OF TYPICAL FOUR-LOOP, LARGE PWR.

TABLE III

RELAP3 INPUT PARAMETERS

System Pressure (psia)	2249.7
Core Inlet Temperature ($^{\circ}\text{F}$)	552.5
Core Outlet Temperature ($^{\circ}\text{F}$)	619.6
Core Flow (lb_m/sec)	3.51×10^4
Bypass Flow (lb_m/sec)	1.72×10^3
Power Level (MW)	3411
Discharge Coefficient at Break	0.6

Figures 43 and 44 represent the flow at the core inlet as predicted by the two versions of RELAP3. The RELAP3-plate version predicted a reversal in the core inlet flow almost immediately after rupture, which lasted for about two seconds. A short period during which the flow passed through a stagnation point followed and then the flow returned to its normal direction. A small spike occurred in the flow at two seconds after rupture, and other spikes of greater magnitude appeared later in the blowdown. RELAP3/MOD 0 also predicted a reversal in the core flow very soon after rupture but for less than one-half second, after which the flow passed through a stagnation point and then returned to the normal direction. The large flow spikes predicted by RELAP3-plate version were not predicted by RELAP3/MOD 0.

Flow at the core outlet as predicted by the two RELAP3 versions is presented in Figures 45 and 46. The RELAP3-plate version predicted a large positive peak in the flow (about 55,000 lb_m/sec) followed by a rapid decrease to almost zero flow in the first second after rupture. RELAP3/MOD 0 predicted a smaller positive peak (about 33,000 lb_m/sec) in the flow immediately after rupture. After the flow passed through an oscillatory period lasting about one second, it decreased from about 10,000 lb_m/sec to zero in about 2.7 seconds. Again, the large flow spikes predicted by the plate version of RELAP3 were not predicted by RELAP3/MOD 0. The large flow spikes predicted by the RELAP3-plate version can be attributed to the collapse of the fluid in the core from the superheated to the saturated state. The core fluid qualities predicted by the two versions of the code are presented in Figures 47 and 48. The RELAP3-plate version predicted the core fluid quality to increase rapidly after the first flow spike as fluid was expelled from both the top and bottom of the core. This expulsion continued until the total mass in the core was nearly depleted. A flow reversal then occurred and cold fluid entered the core and reduced the pressure, causing fluid to rush into the core from both the top and the bottom. The aforementioned flow spikes corresponded to the sharp reductions in core fluid quality. In contrast, RELAP3/MOD 0 predicted a smoothly increasing quality up to about 12 seconds after rupture and thereafter

FIG. 43 FLOW INTO THE CORE AS PREDICTED BY RELAP3-PLATE VERSION.

FIG. 44 FLOW INTO THE CORE AS PREDICTED BY RELAP3 MOD 0.

FIG. 45 FLOW OUT OF THE CORE AS PREDICTED BY RELAP3-PLATE VERSION.

FIG. 46 FLOW OUT OF THE CORE AS PREDICTED BY RELAP3 MOD 0.

FIG. 47 QUALITY IN THE CORE AS PREDICTED BY RELAP3-PLATE VERSION.

FIG. 48 QUALITY IN THE CORE AS PREDICTED BY RELAP3 MOD 0.

a smoothly decreasing quality. This decrease in quality after 12 seconds was due to significant flow through the core in the negative direction. Prior to 12 seconds, the flow had been essentially stagnant. The differences in predicted core quality can be traced directly to the differences in the heat transfer models in the two versions of RELAP3.

Figures 49 and 50 present the surface temperatures and the heat transfer coefficients predicted for an average pin by the two versions of the code.

FIG. 49 SURFACE TEMPERATURE OF A TYPICAL PIN AND HEAT TRANSFER COEFFICIENT PREDICTED BY RELAP3-PLATE VERSION.

FIG. 50 SURFACE TEMPERATURE OF A TYPICAL PIN AND HEAT TRANSFER COEFFICIENT PREDICTED BY RELAP3 MOD 0.

(The temperature of the hot spot in the core would be considerably higher than the surface temperature of an average pin.) RELAP3-plate version predicted that the core quickly experienced nucleate boiling and did not depart from nucleate boiling until about one second after rupture. After departure from nucleate boiling (DNB), the heat transfer coefficient dropped to 100 Btu/hr-ft²-°F (a procedure built into the code) and remained at this value for the duration of the run. Since the core fluid was essentially stagnant, the core fluid was vaporized and superheated. The superheating of the steam resulted in a decrease in the core heat transfer and a resulting increase in the surface temperature. When the core fluid collapsed, as discussed previously, the core fluid temperature returned to saturation temperature thereby resulting in a large difference between the surface temperature and the fluid temperature. The increase in the temperature difference caused an increase in the heat flux which resulted in a momentary sharp reduction in surface temperature.

RELAP3/MOD 0 predicted DNB occurring in less than 0.1 second after rupture. The post-DNB heat transfer coefficients were determined from correlations and were low (less than 100 Btu/hr-ft²-°F between 3 and 11 seconds after rupture).

Core power after rupture predicted by the two versions of the code is presented in Figure 51. During the first second of the transient, the power calculated by RELAP3/MOD 0 was significantly higher than that predicted by the RELAP3-plate version because for RELAP3/MOD 0 the average fluid quality in the core was lower for this period.

1.2 Conclusions

The correspondence in time of the flow spikes, quality oscillations, and temperature spikes calculated by the RELAP3-plate version clearly demonstrate the coupling between fluid dynamics and heat transfer. The apparently erratic behavior predicted by this version of the code was not due to numerical instability but was real within the limitations of the analytical techniques employed. The differences between the results predicted by the RELAP3-plate version and by RELAP3/MOD 0 demonstrate that the heat transfer can significantly affect the fluid dynamics. From these observations, the conclusion was reached that the equations of mass, momentum, and energy should not be decoupled and that codes describing the fluid dynamics during an LOCA should have realistic heat transfer models as integral parts.

FIG. 51 NORMALIZED POWER PREDICTED BY THE TWO VERSIONS OF RELAP3.

IV. LOSS-OF-COOLANT ACCIDENT ANALYSIS -- STRUCTURAL MODEL DEVELOPMENT

(J. A. Dearlen)

The purpose of the structural model development effort is to provide structural analysis methods for predicting structural responses both during normal operations and during accident transients. The structural system of a nuclear reactor must be designed to resist the internal pressure of the primary coolant, successfully undergo thermal elongation associated with system heatup, and withstand an operating basis earthquake and continue to function normally. It must also be designed to withstand a design basis earthquake and shut down safely. In the unlikely event of a pipe rupture and subsequent loss-of-coolant accident (LOCA), the system should be designed such that the forces resulting from rapid decompression of the system will not propagate damage throughout the system because of adverse structural response. To ascertain the capabilities of the system, the designer must have at his disposal adequate structural analysis techniques. The STRuctural Analysis Package (STRAP) was developed for use in the analysis of reactor structures subjected to static and dynamic loading conditions^[22].

This section describes the current STRAP code, compares STRAP analytical results with experimental data, and presents the results of a structural analysis on a PWR subjected to seismic and blowdown forces.

1. STRAP DESCRIPTION

STRAP consists of two operational programs, STRAP-S and STRAP-D, for the static and dynamic analyses, respectively, of reactor structures. A third program, STRAP-MS, for dynamic analysis of substructures is in the developmental stage. The finite element technique^[23] is used throughout the code for the static and dynamic analysis of three-dimensional linear-elastic structures. The solution technique is one of generating a mathematical model of the structure to be analyzed, specifying the forces applied to the structure, and solving the resulting system of mathematical relations for the response of the structural system.

The following subsections describe the finite element representation employed in STRAP, the selection of structural nodes, the forces on the structure, and the method for dynamic analysis of substructures.

1.1 Finite Element Representation of Structural Systems

The mathematical modeling of a structural system involves developing a set of mathematical relationships which define the response of the structure to the loads applied to the structure. By modeling a continuous structure as one with a finite number of concentrated mass points connected by massless members, the force response relation can be represented by a set of ordinary differential equations that can be solved by using available digital computer techniques.

The mathematical model of a structural system, as generated by STRAP, is developed using the finite element method. Figure 52 shows one way a simple frame might be represented with finite elements. The nodes of the structure, representing the connection points of the finite elements, have specified directions of motion designated as degrees of freedom. In the structure of Figure 52, each node has three degrees of freedom -- translation in the X and Y directions and a rotation about the Z axis. For a three-dimensional structure, such as the reactor system in Figure 53, each node has six degrees of freedom -- translations in the X, Y, and Z directions and rotations about the X, Y, and Z axes.

The mathematical modeling of a structure begins with definition of the stiffness of the structure. The structural stiffness is represented by a stiffness matrix $[K]$ which defines the relationship between static forces applied to the structure and the static deflections of the structure due to those forces.

For the analysis of a structural system subjected to constant loads such as dead loads, loads caused by temperature gradients through the systems, or externally applied loads which are not functions of time, the equation

$$\{F\} = [K] \{Y\} \quad (2)$$

where

$\{F\}$ = vector of loads applied to the structure

$[K]$ = structural stiffness matrix

$\{Y\}$ = vector of structural deflection

is solved for the structural deflections. The structural deflections are then used to calculate the individual member forces.

When the structure is subjected to loads that vary with time, a dynamic analysis of the system is required. The mass and damping of the structure are included in the calculations[24].

The mass of the structure is assumed to be lumped at the nodes of the representative model. The equation:

$$[M] \{\ddot{Y}\} + [D] \{\dot{Y}\} + [K] \{Y\} = \{F(t)\} \quad (3)$$

where

$[M]$ = mass matrix

$[D]$ = damping matrix (viscous)

$[K]$ = stiffness matrix

$\{\ddot{Y}\}$ = nodal acceleration vector

$\{\dot{Y}\}$ = nodal velocity vector

FIG. 52 A FINITE ELEMENT MODEL OF A RIGID FRAME STRUCTURE.

FIG. 53 TYPICAL LARGE FOUR-LOOP PWR PIPING.

$\{Y\}$ = nodal displacement vector

$\{F(t)\}$ = vector of time-dependent forces applied to structure.

1.2 Selection of Structural Nodes

Setting up the finite element model of a structural system begins with the selection of nodal points throughout the system with the finite elements being defined between the appropriate nodes. The members (finite elements) are defined as constant cross-section straight members or constant radius, constant cross-section, curved members. The members are further defined by specifying the individual member properties and the nodes which they connect. Features of a structure at which a node is generally required are:

- (1) Bends in pipes
- (2) Tees
- (3) Changes in cross-sectional areas
- (4) Support points
- (5) Points at which two or more members intersect
- (6) Points of load application.

Other points in a structure may be selected as nodes if better definition of the system response is desired. For example, a node may be placed at the midpoint of a pipe for determining the center point deflection.

For dynamic analysis the mass of the system must also be considered in the selection of the structural nodes. The structure is analyzed as a lumped mass system with the mass of the structure being concentrated at the designated nodes. Therefore, the accuracy of the model and of the resulting solution depends on the accuracy with which the relatively continuous mass structure is represented by a lumped mass model.

1.3 Determination of Forces on Structure

STRAP has the capability of analyzing the system structural responses resulting from both static and dynamic forces as described in the following two sections. A third capability for the dynamic analysis of substructures is currently in the developmental stage.

1.31 Static Forces. The static analysis section of STRAP, STRAP-S, computes the elastic response of a structural system to steady state conditions. These conditions include:

- (1) Dead loads
- (2) Internal pressurization
- (3) Externally applied static loads
- (4) Settlement of foundations
- (5) Temperature gradients.

The loads, temperatures, and applied displacements are supplied by the user. The resulting output consists of the structural displacements at the nodes and the induced forces in the structural members.

1.32 Dynamic Forces. The dynamic analysis section of STRAP, STRAP-D, computes the time-dependent elastic response of a structural system to loads applied as a function of time. STRAP-D is designed to accept three forms of time-dependent forcing functions:

- (1) Pressure histories
- (2) Force histories
- (3) Seismic acceleration histories.

The first two forcing function inputs, pressure and force histories, are used to describe the forces applied to a reactor structure during decompression or under any operating condition for which the force and the time of occurrence are known for the system. The third input form, acceleration histories, is used to input earthquake ground acceleration data for evaluation of structural response to seismic excitation.

1.33 Dynamic Analysis of Substructures. The capability for determining the dynamic response of substructures is being developed as STRAP-MS. STRAP-MS will have the capability of determining part of the frequencies and mode shapes of structures having several hundred degrees of freedom. The mode shapes and frequencies are computed by modal synthesis^[25], a technique by which the primary structure is partitioned into substructures. By manipulating the frequencies and mode shapes of the substructures, an approximate set of mode shapes and frequencies for the primary structure is calculated.

A version of the STRAP-MS that has the following properties is operational: (a) The primary structure may have up to 250 degrees of freedom. (b) The primary structure may be partitioned into as many as five substructures. (c) Either a lumped mass matrix or a consistent mass matrix model can be used.

Two check problems have been run with STRAP-MS. One problem was run for a model of the system used in the Semiscale Blowdown and Emergency Core Cooling (ECC) Project^[26]. This model allowed 132 degrees of freedom. The other problem was run for a model of one loop of a typical four-loop PWR system having 180 degrees of freedom. The problem for the semiscale blowdown configuration was performed in three phases: STRAP-MS was used with substructures, STRAP-D was used with 60 degrees of freedom condensed, and a closed form (no substructure) computation was made for all 132 frequencies and mode shapes. The frequencies determined by the three techniques were the same, and the mode shapes obtained for two substructures and the closed form solution for no substructures were the same. The four-loop PWR problem was run on STRAP-MS with 180 degrees of freedom, and also on STRAP-D with 77 degrees of freedom considered. The calculated frequencies agreed within 5%.

2. COMPARISON OF STRAP ANALYTICAL RESULTS WITH EXPERIMENTAL DATA

The computational accuracy of STRAP has been checked by comparing calculated values against a number of classical solutions as well as against experimental data^[27]. Several studies have also been made to determine the response of a structure as a function of modeling variations^[22].

The most recent comparison between analytical and experimental results was made using data from the Semiscale Blowdown and Emergency Core Cooling (ECC) Project. The semiscale blowdown facility is operated at conditions of 2300 psig and 585°F. Piping strains are taken as part of the experimental data. STRAP was used to predict analytically the material strain at two points of the piping system. Figure 54 shows the semiscale piping system and the location of Strain Gages 3 and 25 which were used for strain data acquisition. Figure 55 shows the finite element model of the semiscale piping system.

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FIG. 54 SEMISCALE BLOWDOWN SYSTEM.

FIG. 55 SEMISCALE STRUCTURAL MODEL.

Data for comparison with analytical predictions were taken from two tests, one initiated by a 100% break and one by a 30% break.

Subcooled blowdown pressure histories obtained from the WHAM[28] code were used in generating the forcing functions for the analytical solutions. A typical pressure history generated by WHAM is shown in Figure 56.

Figures 57 and 58 show comparisons between the experimental and calculated strains at gage Locations 25 and 3, respectively. The frequency and amplitude compare favorably until saturation pressure is reached. Since WHAM does not handle two-phase mixtures, the comparisons are not expected to be good below the saturation pressure.

FIG. 56 TYPICAL PRESSURE HISTORY AS GENERATED BY WHAM.

FIG. 57 COMPARISON OF EXPERIMENTAL AND THEORETICAL AXIAL STRAIN HISTORIES FOR TEST 808.

FIG. 58. COMPARISON OF EXPERIMENTAL AND THEORETICAL AXIAL STRAIN HISTORIES FOR TEST 809.

3. CALCULATION OF PEAK STRESSES IN A PWR PRIMARY COOLANT PIPING SYSTEM DURING EARTHQUAKE AND LOSS-OF-COOLANT EVENTS

The structural state of the piping is determined by the peak stresses induced in the piping during an earthquake or system blowdown. These stresses, for a seismic Risk Zone 3 OBE and a postulated blowdown, were calculated for a typical large PWR.

The response of PWR plant primary piping to earthquake and blowdown loadings was calculated by STRAP-D. The analysis was confined to the reactor vessel and one loop of the four-loop primary coolant system. The loop was divided into 30 finite elements and given 77 degrees of freedom. The model analyzed is shown in Figure 59. The resistance the other three coolant loops offered to the motion of the reactor vessel was represented by equivalent springs attached to the vessel at the inlet and outlet nozzles of the three loops. The core barrel was assumed to be rigidly attached to the reactor vessel. The bumper-action spring supports of the primary loop were considered to act as linear elastic springs. The auxiliary piping connected to the piping was considered to offer negligible resistance to the motion of the primary piping. For the earthquake analysis, the foundation of the primary coolant system was assumed to move with the input ground motion; that is, the containment building was assumed to move with the soil beneath it.

The primary loop was subjected to two different time-dependent loadings. The first loading consisted of subjecting the piping to the measured ground accelerations of the 1940 El Centro earthquake. This earthquake had an intensity comparable to that recommended for operating basis earthquakes in seismic Risk Zone 3 [29]. The second loading consisted of subjecting the primary loop piping to the transient pressures produced in the piping by blowdown through a 30% break in the cold leg of the piping. The WHAM code was used to predict the transient pressures in the piping.

The peak stresses induced in the piping loop by the two types of loadings are shown in Table IV. For comparison purposes, the peak stresses in the piping loop during normal operation are also shown. The stress locations mentioned in Table IV are indicated on Figure 59.

The stresses in the piping loop during blowdown were about 15% higher than the calculated operational stresses. The earthquake loading increased the calculated operation stress level about 10%.

Table IV shows peak stresses, even during normal operation, that are higher than allowable. The higher-than-allowable stress level in the piping loop during normal reactor operation is partially a result of a simplifying assumption made in the modeling of the inlet and outlet nozzles of the reactor vessel steam generator and coolant pump. These nozzles were modeled as nonflexible members to avoid a three-dimensional stress-strain analysis, whereas in reality, both the nozzles and the outer shell of the equipment to which they are attached have some degree of flexibility.

A common method used by reactor vendors for the seismic analysis of reactor piping systems is the response-spectra static equivalent (RSSE) method. In this method, the contribution of each mode of oscillation to the response of the structure is determined from a response-spectra curve. Static equivalent loads are then determined by combining in root-mean-square manner the response contributed by each of the considered modes of oscillation [30].

To compare the results produced by two different methods of seismic analysis, the stresses in the PWR primary loop piping resulting from an earthquake of the intensity of the 1960 El Centro earthquake were calculated by STRAP-D and the RSSE method. In the RSSE method, the response of each mode of oscillation of the primary loop was picked from Housner's average response acceleration spectrum [31] for an earthquake of the intensity of the 1940 El Centro earthquake. The stresses calculated by the two different methods are compared in Table V. The stress locations mentioned in Table V are indicated in Figure 59.

FIG. 59 SINGLE LOOP MODEL OF TYPICAL FOUR-LOOP PRESSURIZED WATER REACTOR.

TABLE IV

PEAK STRESSES IN PWR PRIMARY COOLANT LOOP

Type of Loading	Peak Stress Calculated by Criteria of Nuclear Power Piping Code	Location of Peak Stress	Peak Stress by Determination of Principal Stresses	Location of Peak Stress
	(% of Allowable)		(% of Yield)	
Dead weight and normal internal pressure (Loading during normal reactor operation)	115	Point 3 of Figure 59	122	Point 3 of Figure 59
Dead weight and transient internal pressures caused by 30% cold leg break	Not applicable		141	Point 6 of Figure 59
Dead weight and static internal pressure and ground accelerations of 1940 El Centro earthquake	124	Point 6 of Figure 59	126	Point 3 of Figure 59

TABLE V

PEAK STRESSES PRODUCED IN PIPING LOOP BY 1940 EL CENTRO
EARTHQUAKE -- COMPARISON OF TIME DEPENDENT (TD) AND RESPONSE
SPECTRA STATIC EQUIVALENT (RSSE) METHODS

Location Indication on Figure 59	Peak Static Stress (% of Allowable)	Peak Static Plus Dynamic Stress, TD Method (% Increase Over Static)	Peak Static Plus Dynamic Stress, RSSE Method (% Increase Over Static)
Point 1	73.9	7.4	1.8
Point 2	68.4	3.6	5.0
Point 3	115.4	7.6	4.0
Point 4	89.6	1.9	1.7
Point 5	101.1	1.5	2.1
Point 6	113.0	9.9	9.0
Point 7	76.94	2.2	1.8
Point 8	92.7	3.9	5.9

The comparisons of Table V show that at over half of the locations at which stresses were compared, the stresses calculated by the time-dependent method are higher than those calculated by the RSSE method. Thus, the RSSE may not be conservative. A possible reason for the lower values calculated by the RSSE method is that an averaged response spectra curve is normally used in this method. This approach smooths out the features in specific earthquakes that could lead to added excitation of the structure analyzed. To assure that a conservative prediction of structure response be obtained, the absolute values of model contributions should be combined. Conservatism (overprediction of stresses) is more likely if a maximum response spectra curve such as that mentioned in Reference 31 is used.

V. LOSS-OF-COOLANT ACCIDENT ANALYSIS -- FISSION PRODUCT MODEL DEVELOPMENT

The objective of the fission product model development program is to develop models for predicting (a) the release of fission products to the fuel-cladding gap during reactor operation, (b) the release of fission products from the fuel-cladding gap in the event of fuel cladding rupture and the transport of fission products through the reactor system to the containment, (c) the behavior of fission product iodine in the containment building, and (d) the behavior of fission products in a compartmented containment.

Section V-1 presents a summary discussion of fission product studies and indicates the current understanding of fission product behavior during the hypothesized loss-of-coolant accident sequence.

The migration of fission products during reactor operation (steady state release) is presented in Section V-2 in terms of a thermomigration model. This model has been used to describe noble gas release from fuel pins during normal reactor operation. The basic assumption in the derivation of this model is that the driving force for migration of fission products during reactor operation is a Gibbs-free-energy gradient that is dependent on temperature and concentration gradients.

Section V-3 describes a method developed from the steady state release model for estimating maximum noble gas and iodine release to the fuel-cladding gap of light-water-cooled reactors under steady state operating conditions. This method was evaluated by comparing calculated release values for noble gases with data from a number of irradiated fuel pins. The comparison showed that the method is satisfactory for determining maximum noble gas and iodine releases for various fuel heat ratings.

1. FISSION PRODUCT RELEASE AND BEHAVIOR FOLLOWING A LOSS-OF-COOLANT ACCIDENT

(D. H. Walker)

Experimental and analytical studies of fission product behavior following a loss-of-coolant accident can be divided into two periods. The early investigations were concerned with the magnitude of the release and the subsequent behavior of the fission products following a loss-of-coolant accident in which emergency core cooling systems were not used. More recent studies have been concerned with accidents in which the core thermal transient would be terminated by emergency core cooling systems sufficiently early that core damage would essentially be limited to cladding rupture. These more recent studies have also included investigations to determine the effectiveness of engineered safety systems for removing fission products from the atmosphere of containment vessels.

The early studies included experiments conducted by Idaho Nuclear Corporation[32] and Oak Ridge National Laboratory[33] to determine the release of various fission product species from overheated fuel pins. In these experiments, irradiated, clad fuel specimens were heated in an induction furnace at temperatures up to the UO_2 melting point (5070°F). Release fractions for various fission product species from the overheated specimens were measured; the results are summarized in Table VI.

TABLE VI

FRACTION OF FISSION PRODUCT SPECIES RELEASED FROM OVERHEATED FUEL SPECIMENS
IN IDAHO NUCLEAR CORPORATION AND ORNL TESTS

<u>Fission Product Species</u>	<u>Fraction Released</u>
Noble Gases (xenon and krypton)	0.85 to 0.90
Iodine	0.25 to 0.90
Tellurium	0.02 to 0.2
Cesium	0.03 to 0.2
Molybdenum	0.02 to 0.08
Ruthenium	3×10^{-5} to 0.02
Strontium	10^{-4} to 2×10^{-3}

Studies concerning fission product release following accidents in which the core thermal transient is terminated by ECC systems before cladding melting, but not before cladding rupture, are quite limited. In this situation some fraction of the fission products that have migrated from the UO_2 fuel to the interior gas space, or plenum, of the fuel pin will certainly be vented when the fuel pin cladding bursts.

Some experimental data from postirradiation examination of clad fuel pins or specimens are available regarding the fraction of the noble gas fission product inventory that migrates to the fuel pin plenum during reactor operation. These data are useful in estimating the magnitude of the fission product source available for release in the event that cladding rupture occurs during an accident. Correlations based on the data indicate that the fraction of noble gases migrating from the fuel to the plenum depends principally upon the fuel power rating, fuel temperature, fuel pellet radius, and perhaps upon other factors such as burnup and UO_2 stoichiometry[34]. For reactors of current designs, estimates based on the data indicate that 1 to 10% of the inventory of the core noble gases is likely to be present in the gas space of the fuel pin.

Data concerning the extent of migration of the other fission product species to the fuel pin plenum during an LOCA are limited to indicative information for radioiodine. This information indicates that the rate of iodine release to the fuel pin plenum varies from being equal to that for noble gases to being two orders of magnitude less than that for noble gases[35]. The data which indicate that radioiodine release to the plenum during operation is about equal to that for noble gases were obtained by measuring the release from irradiated specimens held at a constant temperature[36]. However, recent analytical descriptions of the migration process indicate that the severe thermal gradient existing in operating fuel pins has a marked effect on the magnitude and direction of the fission product migration[34]. Thus, the validity of using the isothermal data for estimating iodine migration under operating conditions is doubtful. Further, little information exists regarding the chemical state of that iodine which migrates to the fuel pin plenum and its availability for escape upon fuel pin rupture. The potential for reactions between zircaloy cladding and iodine is important in this regard[37].

Some fraction of the fission products released from the fuel pins following pin rupture will be transported from the core region to the containment. Since essentially no data are available to indicate the fraction of released fission products retained in the reactor system, the highly conservative assumption in U. S. safety evaluations is that all the released fission products reach the containment.

Fission products present in the containment atmosphere constitute the source available for leakage to the environment. Thus, their distribution in containment atmospheres as a function of time after release has been studied extensively. Principal test facilities for these studies were the Contamination-Decontamination Experiment (CDE)[32] of Idaho Nuclear Corporation, the Containment Systems Experiment (CSE) Facility[38] of Battelle Northwest Laboratories, and the Containment Research Installation (CRI) and Nuclear Safety Pilot Plant (NSPP)[39,40] of Oak Ridge National Laboratory. The behavior of both simulated fission product mixtures and of fission products released from overheated fuels has been investigated.

The tests in the containment test facilities have shown that, with the exception of noble gases and certain chemical forms of iodine, fission products can be removed rapidly and effectively from containment atmospheres by spray systems employing alkaline solutions. Filter systems are also effective for removal of all species except noble gases and certain chemical forms of iodine. However, rates of removal for filter systems are lower than for sprays and are limited by the rate at which the containment atmosphere can be circulated through the filter system containing the media for fission product adsorption.

The chemical forms of iodine that are difficult to remove with either spray or filter systems have been identified as organic iodides[41] (principally methyl iodide) and hypoiodous acid[42,43]. Early work showed that up to 10% of the iodine appearing from heated fuel specimens could be present as difficult to remove or persistent species[44]. However, recent examination of the information available from the experiments in the various containment test facilities has shown the fraction of iodine present as persistent species depends upon the mass level of iodine in the containment atmosphere[45].

These data indicate that the fraction of iodine present as persistent iodine is less than 0.1% for iodine mass levels of more than a few hundred micrograms per cubic meter. Persistent fractions of 1% or greater were observed when iodine mass levels were less than a few micrograms per cubic meter. As a point of reference, release of 25 to 50% of the core iodine inventory to the containment atmosphere of current reactor plants produces iodine mass levels in the range 10,000 to 100,000 $\mu\text{g}/\text{m}^3$.

The experimental information summarized indicates that in the event of a loss-of-coolant accident, the principal fission product species which will appear in the containment atmosphere will be the noble gases and iodines. Other fission products such as tellurium and cesium may appear, but are likely to be present at lower concentration levels. Application of containment sprays will rapidly and effectively remove these other fission products as well as the iodine present as either molecular iodine (I_2) or hydrogen iodide (HI). Fission products remaining in the containment atmosphere and available for release to the environment will be the noble gases and the persistent iodine species. The current conservative practice in evaluating the consequences of a hypothesized accident in the U. S. is to assume that 10% of the iodine reaching the containment will remain as persistent species; available experimental data indicate the fraction remaining as persistent species may be 0.1% or less of the iodine reaching the containment.

2. DISTRIBUTION OF SOLID FISSION PRODUCTS IN UO_2

(J. H. McFadden, V. F. Baston, W. A. Yuill)

Considered in this section is a model that describes the migration of fission products and fissile materials, and the segregation of metallic ions in reactor fuel pins during reactor operation. Fission product migration and subsequent fission product release to the fuel-cladding gap are important considerations in safety evaluations for both thermal and fast reactors. Redistribution and segregation of fissile material can change the power generation profile in the fuel and affect the Doppler coefficient of mixed oxide fuels. Experimental data for fission product and plutonium migration have been presented by Bates[46,47]. An analytical description of thermal diffusion of fission products and fissile materials in reactor fuels has been given by Wirtz[48] and Beisswenger et al[49,50]. Christensen[51] also has presented a model for fission product migration.

In this section, a thermomigration model for describing the migration of fission products in reactor fuel during operation is described. The thermomigration model was developed earlier and used to describe noble gas release from fuel pins during operation[34,52]. The basic assumption employed in the derivation of this model is that the driving force for migration of fission products during reactor operation is a Gibbs-free-energy gradient, which is dependent on temperature and concentration gradients. Examples of forces proportional to the thermal gradient that contribute to the Gibbs-free-energy gradient are entropy effects, thermoelectric effects, and thermal stresses.

2.1 Physical Model for Solid Fission Products

The physical model proposed for describing the behavior of solid fission products in operating fuel pins is very similar to that proposed previously for noble gases^[52]. The newly formed fission products, depending on their chemical properties, will either be soluble in the UO_2 fuel lattice or will migrate to cracks or fissures in the fuel pellet where they will precipitate as part of a metallic inclusion or low-melting point oxide. An important feature of the fuel pin, however, is that the precipitation of fission products in the fissures and cracks is at least partially reversible because of radiation effects. Fission fragments and ionizing radiation will in effect cause mixing among the various phases present. In addition, the inclusions containing precipitated fission products will be subject to the forces and stresses in the fuel, and will move into the hot regions of the pin over long periods of time. The important consideration of such a physical model is that all the fission products in the fuel should be regarded as being subjected to the forces which exist in the UO_2 fuel lattice regardless of whether they are actually in the UO_2 lattice or in some phase.

2.2 Analytical Description

The time-dependent diffusion equation for a fission product with concentration, $C(r, t)$, and effective decay constant, λ^* , produced at a rate of $P(r, t)$ is

$$\frac{dC(r, t)}{dt} = P(r, t) - \lambda^* C(r, t) + \nabla \cdot \left[\frac{D(r, t) C(r, t)}{RT(r, t)} \nabla G(r, t) \right] \quad (4)$$

where $T(r, t)$ = the absolute temperature

R = the gas constant

$\nabla G(r, t)$ = the Gibbs-free-energy gradient.

The diffusivity, $D(r, t)$, of the isotope is

$$D(r, t) = D_0 \exp[-Q/RT(r, t)] \quad (5)$$

where D_0 = the diffusion constant

Q = the activation energy for diffusion.

The Gibbs-free-energy gradient, $\nabla G(r, t)$, can be written as

$$\nabla G(r, t) = \left(\frac{\partial G}{\partial T} \right)_C \nabla T + \left(\frac{\partial G}{\partial C} \right)_T \nabla C \quad (6)$$

$$\text{or} \quad = \left(\frac{\partial G}{\partial T} \right)_C \nabla T + \frac{RT}{C} \nabla C \quad (7)$$

where $\left(\frac{\partial G}{\partial T} \right)_C$ includes all terms which are proportional to the thermal gradient.

The assumption that the driving force for atomic or ionic diffusion is the gradient of the free energy was first presented by Einstein^[53]. Diffusion can occur under the condition of zero concentration gradient, if the free-energy gradient is nonzero. Ficks law describes a special type of diffusion, and can be derived from Einstein's assumption by assuming the thermal gradient is zero.

The thermal gradient is an important term to include in a fission product migration model for two reasons. The thermal gradient in the outer region of the fuel is on the order of 4000°K/cm. Therefore, if a species has a partial molar entropy of ten entropy units at 1000°K, the thermal gradient term contributes more to the Gibbs-free-energy gradient than the concentration gradient term, unless the concentration change across that region is greater than a factor of about 10^7 . The second reason for including the thermal gradient term concerns the direction of migration. Entropy affects the direction of migration in that chemical species formed as the result of an endothermic reaction in the lattice will concentrate at the high temperature end of the thermal gradient, whereas those formed in an exothermic reaction will migrate to the lower temperature end of the thermal gradient. A similar movement of ionic fission products occurs because of thermoelectric effects.

If the fuel has been operating for a time that is long compared to the half-life of the isotope being studied, steady state equilibrium will be established, $dC/dt = 0$. Equation (4) becomes

$$\nabla \cdot \left[\frac{D(r)C(r)}{RT(r)} \left[\left(\frac{\partial G}{\partial T} \right)_C \nabla T + \frac{RT}{C} \nabla C \right] \right] - \lambda * C(r) = P(r) \quad (8)$$

where the expression for ∇G from Equation (7) has been substituted for the Gibbs-free-energy gradient. Approximate solutions to Equation (8) have been obtained by assuming a series solution of the form

$$C(r) = \sum_{n=0}^N a_n \cos \frac{n\pi r}{b} \quad (9)$$

where b is the radius of the fuel rod.

To solve Equation (8), the series, Equation (9), is substituted for $C(r)$, and each term is multiplied by

$$\cos \frac{m\pi r}{b} \quad (10)$$

and integrated over the volume element, rdr . Evaluation of these integrals results in a system of simultaneous linear equations, represented in matrix form by

$$Ax = B \quad (11)$$

where x is the column matrix of N coefficients, a_n , B is the column matrix whose terms are of the form

$$b_m = \int_0^b P(r) \cos \frac{m\pi r}{b} r dr \quad (12)$$

and A is the N by N matrix composed of the integrated terms on the left hand side of Equation (8).

Equation (11) is solved for the coefficients, a_n , by means of a Gauss-elimination process, and the concentration profile is obtained by substitution of these coefficients into Equation (9) and evaluation of the summation.

2.3 Migration of Strontium-90 and Cesium-137

The method of solution of Equation (8), discussed in the previous section, was used in obtaining the distribution of strontium-90 and cesium-137 in an operating UO_2 fuel rod of radius 1.7 cm^[47]. The temperature distribution for the fuel rod (GEH-4-49) was calculated from data for preirradiated pins^[54] and is shown in Figure 60. The experimental distributions of strontium-90 and cesium-137 for this pin were taken from the work of Bates^[47] and are given in Figure 61. Since this specimen was irradiated for only 17 days and

FIG. 60 TEMPERATURE DISTRIBUTION IN 1.7-CM RADIUS PIN.

the analytical solution assumes $dC/dt = 0$, the experimental distributions for a specimen (APED-A-597) of 0.6 cm radius^[47], irradiated for a longer time at approximately the same linear power rating, are also given in Figure 61.

The analytical distributions for strontium-90 and cesium-137, based on a 15-term expansion of Equation (9), are shown in Figure 62. These distributions are assumed to be converged solutions, because the addition of more terms (up to $N = 20$), does not change the values of the first 15 coefficients, a_n . The values of the physical parameters used in these calculations are given in Table VII. The values of $(\partial G/\partial T)_C$, $-4\text{ cal}/^\circ\text{K}$ for strontium-90 and $5\text{ cal}/^\circ\text{K}$ for cesium-137, used to calculate the distributions in Figure 62, are reasonable entropy values for the formation of oxides of strontium and cesium at 1000°K . The effect of the sign difference of the values of $(\partial G/\partial T)_C$ is evident, with cesium moving toward the cooler regions of the fuel and strontium migrating toward the hotter regions.

In comparing the experimental and calculated distributions, notice should be taken that the shape of the experimental and calculated curves are the same, and that the direction of migration (that is, up or down the thermal gradient) was correctly represented by the analytical method. However, the magnitudes of the experimental and calculated distributions are not the same. The reason for this discrepancy is that the production rate was based on a normalized radial power shape, which is proportional to the actual production rate for a particular isotope.

FIG. 61 EXPERIMENTAL DISTRIBUTIONS OF STRONTIUM-90 AND CESIUM-137 FROM BATES.

FIG. 62 ANALYTICAL DISTRIBUTION OF STRONTIUM-90 AND CESIUM-137 AFTER LONG IRRADIATION ($b=0.6$).

TABLE VII

PARAMETERS FOR STRONTIUM-90 AND CESIUM-137 CALCULATIONS

Isotope	$\lambda^*(\text{sec}^{-1})$	$D_0^{[a]}(\text{cm}^2/\text{sec})$	$Q^{[a]}(\text{kcal/mol})$	$(\partial G/\partial T)(\text{cal}/^\circ\text{K})$
Sr-90	7.84×10^{-9}	4.5×10^{-7}	24.4	-4
Cs-137	7.32×10^{-10}	8.53×10^{-9}	6.1	5

[a] Values from Oi and Takagi^[55].

The cesium-137 curves in Figure 63, obtained by varying the value of $(\partial G/\partial T)_C$ as indicated in the figure, demonstrate the importance of including the thermal gradient term when concentrations are predicted as a function of radius. Because of the complex chemical reactions occurring in a fuel pin during irradiation, physical properties such as stoichiometry and entropy are not constant in either space or time, and this lack of constancy should be considered whenever possible.

FIG. 63 CESIUM-137 DISTRIBUTION WITH $(\partial G/\partial T)$ AS A PARAMETER ($b = 0.6$ CM).

2.4 Conclusions

The calculated fission product distributions presented in this section demonstrate that the thermomigration model for fission product distribution and release can be used to describe the distribution of fission products if the diffusivity and thermodynamic data for the isotope are known. The model also correctly accounts for the migration of strontium and cesium in opposite directions because of the difference in sign of $(\partial G/\partial T)_C$ for the two isotopes.

3. SAFETY ANALYSIS FOR STEADY STATE FISSION GAS RELEASE

(V. F. Baston, J. H. McFadden, W. A. Yuill)

Methods to predict analytically the release of fission gases from reactor fuels have been actively sought since the late 1950's. Information regarding the pressure that could be exerted on the fuel cladding by fission gases released from the fuel to the fuel rod plenum and the quantity of fission products that would be available for release in the event of cladding failure was needed for fuel pin design and radiological safety analyses. Although concentrated efforts using considerable experimental data from capsule fuel were expended to describe the fission gas release phenomena, the analyses remained incapable of clearly correlating in-pile data. For safety analysis high release assumptions have been adopted for use in radiological safety analyses for reactor setting[56].

This section describes a method developed from the steady state release model[52] for calculating maximum noble gas and iodine releases to the fuel-cladding gap for light-water-cooled reactors under steady state operating conditions. The steady state release model has been shown to provide a good correlation for in-pile fission gas release data and to be capable of describing the fission product distribution in UO_2 . The analytical method described in this section is believed to be appropriate for prediction of internal fuel pin pressures during operation and for radiological safety analysis.

To define release from the fuel accurately, several parameters such as the fuel surface temperature, T_b , and the Gibbs-free-energy terms, dG/dT , must be known but are difficult to determine. Therefore, to simplify and yet provide conservative release predictions two assumptions are employed for reactor cores for which little detailed information is available:

- (1) The release of iodine-131 is equal to the fractional release observed for xenon-131
- (2) The relationship between the fractional release, F , and heat rating, $\int KdT$, is based on the higher fractional releases observed for any heat rating.

The first assumption is conservative because it is equivalent to stating that all xenon-131 observed in the fuel-cladding gap is released from the fuel as iodine-131. Since xenon-131 is due largely to iodine-131 decay for in-pile conditions, iodine-131 release is expected to be no larger than the total xenon-131 release. P. Chenebault et al[57] gives experimental justification for the assumption. The second assumption is equivalent to assuming that all of the fuel pins in any reactor have a low fuel-to-sheath heat transfer coefficient and, therefore, have a high fuel surface temperature for all corresponding heat ratings, $\int KdT$.

A comparison of noble gas capsule data for a number of fuel pins with an analytical relationship predicted from the steady state release model is presented in this section. The analytical relationship provides a rapid method for determining release for any fuel pin heat rating. Important parameters that must be considered when interrelating capsule release data or performing release analyses are presented and the effect of these parameters on release is discussed. Use of the steady state release method is demonstrated by calculations of noble gas release to the fuel-cladding gap of a hypothetical core, Test Core I.

The release analyses currently used in several safety analysis reports are shown in this section to predict lower fuel-cladding gap releases for lower power rods in operating reactors than is observed for those fuel pins.

3.1 Steady State Release Curve and Identified Release Parameters

A clear understanding of the effect of various parameters on the release fraction is necessary for performing release analyses. In the next five sections, important parameters in the steady state release model and conservative assumptions employed to obtain a steady state release method for calculating maximum iodine release are discussed.

The basic equation derived from the steady state release model^[52] is:

$$\ln (1-F_i) = \frac{D_{oi} e^{-Q_i/RT_b}}{\lambda_i T_b b^2} f\left(\int_{T_b}^{T_o} KdT, i\right) \quad (13)$$

where F_i = fraction of i released to the fuel-cladding gap
 D_{oi} = diffusion constant for i in the fuel
 Q_i = activation energy for the diffusion of i in the fuel
 λ_i = decay constant for i
 T_b = surface temperature of fuel pellet
 b = radius of pellet
 R = ideal gas constant
 T_o = center-line temperature.

The term, $f\left(\int_{T_b}^{T_o} KdT, i\right)$, is a function dependent upon the heat rating, $\int KdT$, and upon thermodynamic constants which are dependent upon the chemical properties of the species, i .

3.11 Steady State Release Curve. A comparison of noble gas capsule release data[58-62], for a number of fuel pins at different heat ratings, with an analytical relationship predicted from Equation (13) is presented in Figure 64. The calculated steady state release curve shown in Figure 64 reflects higher release than is generally observed. This steady state release curve can be used to determine the maximum noble gas release for any given heat rating, $\int KdT$. The calculated steady state release curve in Figure 64 reflects a higher release than is generally observed because the curve corresponds to a pin with a low gap thermal conductivity, and therefore, a high surface temperature for each heat rating. A more detailed discussion of the steady state release curve and the experimental data has been presented previously[52]. The release fractions plotted in Figure 64 are the observed release fractions normalized to the release fraction for a fuel radius of 0.635 cm, as discussed in the next section.

FIG. 64 NOBLE GAS CAPSULE DATA AND STEADY STATE RELEASE FOR TYPICAL REACTOR CORE HEAT RATINGS.

3.12 Radial Dependence. For constant temperature and heat rating, Equation (13) may be written in the form:

$$(1-F) = \exp \left(- \frac{\alpha}{b^2} \right) \quad (14)$$

where

F = Fractional release

b = Fuel radius (cm)

α = $f(\int KdT)$, function of heat rating

$\int KdT = \frac{P}{4\pi}$, where P = average power output per unit length of solid cylindrical rod, heat rating, (W/cm).

Equation (14) indicates that smaller radius fuel gives larger releases for the same heat rating. Therefore, the differences in fuel radii must be accounted for when releases for various capsules with different radii are compared.

Differences in fuel radii for capsule release data can be seen from the following reports: The Atomic Energy of Canada Limited reports AECL-1676 [63], -1685[64], -1895[65], and -1952[66] give data for fuel radii of ~1 cm; the General Electric Company report GEAP-4314[60] gives data for fuel radii of 0.50 to 0.63 cm; and the Westinghouse Electric Corporation report WCAP-6087[58] gives data for fuel radii of ~0.37 cm. The effect of fuel radius on release can be seen in Figure 65. The calculated curves in Figure 65, like that in Figure 64, are based on the steady state release model and the conservative assumptions discussed previously. The release curves were calculated for fuel radii of 0.4, 0.6, 0.8, and 1.0 cm. Release fractions for different radii fuel pellets can be related by

$$(1-F_1)b_1^2 = (1-F_2)b_2^2 \quad (15)$$

where

F_1 = Release fraction for fuel of radius b_1

b_1 = Fuel radius (cm)

F_2 = Release fraction for fuel of radius b_2

b_2 = Fuel radius (cm).

Equation (15) is obtained from the steady state release model by assuming that the temperature of the fuel surface is a function of heat rating only. Therefore, releases from fuels with different radii may be normalized readily to a common radius.

FIG. 65 STEADY STATE RELEASE AS A FUNCTION OF FUEL RADIUS.

3.13 Surface Temperature Dependence. For pins of constant radius and heat rating, Equation (13) may also be written in the form

$$\ln(1-F) = \frac{-\beta}{\exp(Q/RT_b)} \quad (16)$$

where

F = Fractional release

β = $f(\int KdT)$, function of heat rating

Q = Activation energy (cal/mole)

R = Molar gas constant (cal/mole-°K)

T_b = Fuel surface temperature (°K)

$\int KdT$ = Heat rating (W/cm).

Equation (16) shows that as the fuel surface temperature, T_b , increases, noble gas release increases. For example, for small release values, Equation (16) indicates an approximate factor of two increase in release fraction for a 20 degree rise in fuel surface temperature (750 to 770°K). Therefore, the fuel-to-cladding gap is an important parameter because of its effect on the fuel surface temperature. C. N. Craig et al^[67] have given a concise discussion of the effect of the fuel-to-cladding gap on the fuel surface temperature and on related parameters. For example, Craig et al list the fuel surface temperatures on the basis of the radius of equiaxed grain growth for a rod which has a 2.2-mil diametral gap at start of life (with a heat rating of 18 kW/ft) and a rod which has a 10.1-mil diametral gap at start of life (with a heat rating of 17.8 kW/ft) as 1104°C, and $\leq 1650^\circ\text{C}$, respectively. The experimentally reported^[61] fission gas releases from these two rods are 12.7 and 57.2%, respectively. This information shows that the larger gap increases the fuel surface temperature and produces a larger release.

3.14 Other Release Parameters. Parameters identified by the steady state release model as affecting release (that is, fuel radius, b , fuel surface temperature, T_b , decay constant, λ , and activation energy, Q) have been evaluated and reported^[52]. Burnup could also be considered in the release analysis. The release analysis effort has not produced a definite burnup correlation to date. However, the success of the steady state release model in interrelating the capsule release data suggests that burnup may be of secondary importance for noble gas release. This suggestion is in accord with the conclusion reached by H. E. Williamson and D. C. Ditmore^[68] that within the burnup range covered by their data, shown in Figure 66, the effect of burnup on the release rate is indiscernible.

Other parameters, such as stoichiometry, density, distribution of impurities and porosity, will affect migration and release of fission products. Efforts are continuing to identify and describe the contribution of significant parameters.

3.15 Iodine Release from Operating Fuel. One of the most important conclusions indicated by the steady state release model is that release of fission products other than noble gases, particularly iodine release, cannot be related to noble gas release by an arbitrary multiplier.

Current analytical and experimental in-pile information is insufficient to describe clearly iodine release from operating fuels. The uncertainty concerning the relationship between noble gas and iodine release was reflected at the recent International Congress on the Diffusion of Fission Products^[35] where reported results, from in-pile experiments, indicated iodine release rates vary from being equal to noble gas release rates to being two orders of magnitude less. However, the only in-pile data reviewed by the authors of this section suggesting high iodine release^[36] (that is, iodine release rates equal to noble gas release rates) were obtained at low thermal gradients (300 to 700°K/cm). The results of the experiment reviewed by the authors are closer to the results of anneal-type experiments than results physically observed in operating reactors which have large thermal gradients ($>4000^\circ\text{K/cm}$).

To estimate the maximum iodine release to the fuel-cladding gap for operating reactors during steady state operation, a boundary assumption based on experimental observation was employed. The assumption is that the release

FIG. 66 COMPARISON OF PREDICTED AND OBSERVED FISSION GAS RELEASE [68].

of iodine-131 is equal to the fractional release observed for xenon-131, which is equivalent to stating that all xenon-131 observed in the fuel-cladding gap was released from the fuel as iodine-131 and decayed to xenon-131. P. Chenebault et al [57] give experimental justification for this assumption. W. A. Bezella's [58] fission gas release data indicate that xenon release is independent of the half-life of the iodine precursors, which again suggests that the fraction of the observed xenon released as iodine is small compared to the fraction released as xenon. Therefore, the assumption of iodine-131 release equal to the xenon-131 release is conservative.

3.2 Use of the Steady State Release Method for Calculating Noble Gas Release to the Fuel-Cladding Gap During Operation

The steady state release method developed by Idaho Nuclear Corporation for calculating noble gas release to the fuel-cladding gap during reactor operation involves: (a) division of the core into concentric groups of complete rods that have the same average heat rating, $\int KdT$, (b) use of the steady state release curve, shown in Figure 64, and (c) use of a weighted averaging technique:

$$\text{Percent core release to the fuel-cladding gap} = \frac{\sum_{i=1}^k n_i P_i F_i}{\sum_{i=1}^k n_i P_i} \times 100 \quad (17)$$

where

- n_i = Number of pins in a given core group operating at an average heat rating, P_i
- P_i = Average heat rating, $\int KdT$, for each pin in a given core group (W/cm)
- F_i = Noble gas release fraction for the fuel-cladding gap for pins of average heat rating, P_i , from Figure 64
- k = Number of groups into which the core pins are divided.

Fuel rods in the core are divided into concentric groups of complete rods that have the same average heat rating. This grouping may be accomplished, for example, by using the radial power profile. The release fraction for each core group may then be determined from the plot of fractional release, F_i , versus average heat rating, $\int KdT$. The release in Figure 64 is given in percent and must be converted to fractional values before being used in Equation (17). The Figure 64 release fraction must also be corrected to correspond to the fuel radius for the core under study, as discussed in Section V-3.12. The core fuel-cladding gap release is then calculated by weighting the pin release fractions according to power density and by summing, as described by Equation (17). The $n_i P_i$ weighting terms, given in Equation (17), are proportional to the noble gas inventory in a core region; therefore, the core noble gas release is weighted according to power density. As a specific example, Table VIII gives a breakdown of the calculation of the noble gas release to the fuel-cladding gap for a test core which was divided by radial power profile into thirteen core groups ($k = 13$).

In safety analysis reports, estimates of the number of pins perforating following the hypothesized loss-of-coolant accident are given for two nuclear facilities currently under construction[69,70]. Perforation estimates are 15 to 30% of the total pins in the core. The fraction of noble gas released to the reactor primary system for various numbers of pin perforations may be calculated by

$$\text{Fractional release to the reactor primary system for pin perforations} = \left[\frac{\sum_{i=1}^k a_i n_i P_i F_i}{\sum_{i=1}^k a_i n_i P_i} \right] \left[\frac{\sum_{i=1}^k a_i n_i P_i / \sum_{i=1}^k a_i n_i}{\sum_{i=1}^k n_i P_i / \sum_{i=1}^k n_i} \right] \left[\frac{\sum_{i=1}^k a_i n_i}{\sum_{i=1}^k n_i} \right] \quad (18)$$

Term 1

Term 2

Term 3

where

a_i = Fraction of pins that perforate per core group. The other parameters are defined by Equation (17).

Term 1 of Equation (18) is the average fractional release to the fuel-cladding gap for perforated pins, Term 2 is the average heat rating of the perforated pins divided by the average core heating rating, and Term 3 is the

average fraction of perforated pins for core groups having pin perforations. Terms 2 and 3 define the fraction of the total core inventory in the perforated pins. The procedure for using Equation (18) is similar to that described for Equation (17).

To calculate the curies released to the primary system, the release fraction from Equation (18) is multiplied by the core inventory. As an example, two cases for Test Core 1 are considered. Assuming that 1% of the pins of the test core (hottest 13 pins) perforates ($a_i = 0.141$ for $k = 1$ and $a_i = 0$ for $k = 2$ through 13) and releases the activity in the fuel-cladding gap to the primary system results in a 0.65% release of the noble gas inventory. Similarly, if the assumption is made that all the pins in the hottest two core groups, listed in Table VIII, perforate ($a_i = 1.0$ for $k = 1$ and 2 and $a_i = 0$ for $k = 3$ through 13), the release of the noble gas core inventory corresponds to 6.5%. Figure 67 gives the Test Core I noble gas release to the reactor primary system as a function of pin perforation.

TABLE VIII

NOBLE GAS RELEASE TO FUEL-CLADDING GAP FOR TEST CORE I

Pins per Core Group	Radial Profile	Average Heat Rating, $fKdT$, for Core Region	Group Pin Fraction of Total Core	Percent Release, F_1 (from Figure 64)	Percent Release, F_2 , Corrected ² , for Fuel Radius	M $fKdT$	MF ₂ $fKdT$
		(W/cm)	$M = \sum_{i=1}^k n_i$	(%)	(%)		
92	1.55	30.0(11.5) ^[a]	0.07	25.2	41.7	2.1	0.9
44	1.45	28.0(10.7)	0.03	22.7	38.0	0.9	0.4
140	1.35	26.1(10.0)	0.11	19.9	33.8	2.8	0.9
116	1.25	24.0(9.2)	0.09	16.6	20.6	2.1	0.6
100	1.15	22.2(8.5)	0.08	13.6	23.7	1.7	0.4
144	1.05	20.3(7.8)	0.11	10.5	18.6	2.2	0.4
144	0.95	18.3(7.0)	0.11	7.5	13.4	2.0	0.3
128	0.85	16.4(6.3)	0.10	5.0	9.1	1.6	0.1
132	0.75	14.3(5.5)	0.10	2.9	5.4	1.4	0.1
68	0.65	12.5(4.8)	0.05	1.7	3.1	0.6	---
100	0.55	10.7(4.1)	0.08	0.9	1.6	0.8	---
68	0.45	8.6(8.3)	0.05	0.3	0.6	0.4	---
24	0.35	6.8(2.6)	0.02	0.1	0.2	0.1	---
1300			1.00			18.7	4.1

$$\text{Total Core Release} = \frac{4.1}{18.7} \times 100 = 21.9\%$$

[a] Core group linear heat rating in kW/ft.

FIG. 67 CORE RELEASE VERSUS PIN PERFORATION FOR TEST CORE 1.

3.3 Comparison of the Idaho Nuclear Corporation Release Analysis with Release Analyses Reported in PSAR's

The safety analyses for fission gas activity in the fuel-cladding gap in safety analysis reports for several nuclear facilities currently under construction[69-73] are based either on an empirical fit of noble gas capsule data, for example, volumetric average temperature (VAT) method[62,68], or analytical concentration diffusion relationships (Booth-D' method)[58,74-77] for operating conditions, or TID-14844[56] for maximum credible accident conditions. In the following section the Booth-D' approach and the VAT methods are shown to predict lower values for noble gas release to the fuel-cladding gap for a typical operating reactor than the steady state release method. The TID-14844 method as indicated previously predicts higher releases.

The steady state release curve, Figure 64, indicates an increase in the fraction for noble gases released with increased heat rating that is generally consistent with experimental observations. Although the curve of Figure 64 appears similar in shape to the empirical release curve obtained from the volumetric-average-temperature method (VAT) reproduced in Figure 68, significant differences occur, particularly in the fuel-cladding gap release for pins with low heat ratings.

The calculated-versus-observed noble gas release fractions for the same experimental capsule data[60] as calculated by the empirical release curve (VAT) and as calculated by the steady state release model[52] are presented in Figures 69 and 70, respectively. If the calculated and observed release

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FIG. 68 FISSION PRODUCT RELEASE AS A FUNCTION OF TEMPERATURE AND BURNUP [78].

FIG. 69 RELEASE CALCULATED BY VOLUMETRIC-AVERAGE-TEMPERATURE METHOD VERSUS OBSERVED RELEASE.

fractions were identical all points would be on the 45 degree lines shown in Figures 69 and 70. Figure 69 shows that the calculated VAT release fractions are low with respect to the observed release fractions; that is, almost all points lie on the lower (observed) half of the plot. Figure 70 shows that the calculated steady state release fractions are close to the observed release fractions; that is, most of the points lie on or near the 45 degree line. Figure 71 shows that the Booth D' release curve (determined from G. W. Parker et al^[75] D' empirical method for $t = 10^8$ sec) also underestimates noble gas release from pins with low heat ratings and overestimates noble gas release from pins with high heat ratings with respect to the capsule data and the steady state release curve. The lack of agreement between the observed and calculated release fractions predicted by the Booth D' method can be seen by comparing the calculated-versus-observed noble gas release fractions

FIG. 70 RELEASE CALCULATED BY STEADY STATE RELEASE MODEL VERSUS OBSERVED RELEASE.

for the same experimental capsule data^[60]. Figure 72 shows that most of the calculated Booth (D' empirical) release fractions are low with respect to the observed release fractions; that is, most of the points lie on the lower (observed) half of the plot. As shown previously (Figure 70) the steady state release method correlates the calculated and observed releases well.

The comparisons in Figures 69 through 72 show that the empirical release curve (VAT) and the Booth D' release curve tend to underestimate the noble gas release from pins with low heat ratings. For a typical large operating reactor, the fuel pins with low heat ratings corresponds to $\geq 50\%$ of the core. When the Booth D' or VAT release analyses are applied to determine the release to the fuel-cladding gap of a typical core, a low release fraction is calculated for the lower power regions. For example, Table IX presents the

TABLE IX
PERCENT RELEASE TO THE CORE FUEL-CLADDING GAP

	Release(%)	
	Steady State Method	D' Empirical Method
Large BWR	15.8	0.5 ^[a]
Test Core I	21.9	2.2 ^[b]

[a] Release value for large BWR by the D' empirical method taken from ORNL-4428.

[b] Release value for Test Core I by the D' empirical method taken according to ORNL-4228.

FIG. 71 NOBLE GAS CAPSULE DATA WITH COMPARISON OF STEADY STATE AND BOOTH (D'EMPIRICAL) RELEASE CURVES.

FIG. 72 RELEASE CALCULATED BY THE BOOTH (D'EMPIRICAL) METHOD VERSUS OBSERVED RELEASE.

core fuel-cladding gap releases for two reactor cores, a large BWR^[78] and Test Core I, as calculated by the steady state release method and the D' empirical method discussed by G. W. Parker et al^[75].

The calculated steady state release for the large BWR, based on Figure 64 is a maximum release. If a more probable (lower) release curve were used in Figure 64, that is, a curve that is not a maximum, the percent release to the fuel-cladding gap would be lower than the maximum by a factor of about three to four. However, the conservative steady state release method is recommended for safety analyses. The closer agreement between the calculated values from the two methods for Test Core I releases is due to the fact that the majority of the Test Core I heat ratings lie in the narrow range for which the D' empirical method release curve approaches the steady state release curve shown in Figure 71.

TID-14844[56] suggests that for a hypothesized maximum credible accident 100% of the noble gases, 50% of the iodines, and 1% of the solids will be released into the reactor building. These deliberately conservative criteria are still used as a radiological siting standard. The steady state release method is believed by the authors to be appropriate for use in calculating maximum core fuel-cladding gap release under steady state operating conditions and for estimating releases for fuel-cladding failures. As indicated by N. R. Horton et al [78] sufficient release studies appear to have been conducted to indicate that release values lower than those listed in TID-14844 can be applied for situations in which significant core melting does not occur. The steady state release model appears to be analytically capable of describing transient cases; however, the modeling effort for this application is not yet complete.

3.4 Conclusions

A simple method for calculating the maximum release of noble gas and iodine from fuel operating at steady state conditions has been presented. To predict release, two conservative assumptions are employed:

- (1) The release of iodine-131 is equal to the fractional release observed for xenon-131
- (2) The relationship between the fractional release, F , and heat rating $\int KdT$, is based on the higher fractional release observed for any heat rating.

Two important parameters that must be considered when interrelating capsule release data or performing release analyses are fuel radius and fuel surface temperature. The general effects of these two parameters on release are increased release with decreased fuel radius and increased release with increased fuel surface temperature.

The release analyses currently used in several safety analysis reports have been shown to predict lower fuel-cladding gap releases for operating reactors than are observed. A comparison of the release fractions calculated by other currently employed analyses with the experimentally observed (capsule) release fractions shows that these other analyses underestimate the release from fuel pins with low heat ratings and which represent > 50% of the core. The steady state release model provides a better correlation for fission gas release data from those pins with low heat ratings. The steady state release method, which is based on the steady state release model, is believed appropriate for radiological safety analysis. Reactors are presently sited on the basis of a maximum credible accident defined in TID-14844. The releases prescribed for the maximum credible accident are much higher than the steady state releases calculated by any of the models described in this report.

VI. TECHNICAL ASSISTANCE IN REACTOR SAFETY ANALYSIS

(C. F. Obenchain)

The program of Technical Assistance in Reactor Safety Analysis provides analytical assistance to the AEC and its regulatory agencies^[79]. The scope of the program includes: (a) application of analytical models for determining reactor system behavior and evaluating engineered safety system performance under loss-of-coolant accident (LOCA) conditions for commercial nuclear plants of current or advanced design and (b) assessment of these analytical models by comparing calculated results with experimental data. Work is currently directed toward five major interrelated facets of the loss-of-coolant accident: (a) analysis of subcooled blowdown phenomena, (b) analysis of ECC performance, (c) analysis of saturated blowdown phenomena, (d) analysis of containment response, and (e) analysis of structural response.

The specific objectives being pursued in these areas are to:

- (1) Apply and assess methods used to determine the forces imposed on crucial core structures during the subcooled portion of the blowdown of a large PWR for credible break sizes, break locations, and break opening times
- (2) Apply and assess methods used to evaluate the performance of the ECC systems proposed for a large PWR or large BWR system for credible break sizes and locations
- (3) Apply and assess methods used to predict the thermal, hydraulic, and mechanical behavior of the fuel pins during the saturated portion of the blowdown of a large PWR or large BWR for credible break sizes and locations
- (4) Apply and assess methods used to determine the pressure and temperature in a PWR containment and a BWR pressure suppression containment as functions of time following a loss-of-coolant accident
- (5) Apply and assess methods used to predict the response and evaluate the structural adequacy of primary system components crucial to core integrity when these components are subjected to combined subcooled blowdown and seismic loads.

These objectives are pursued using currently available computer codes and experimental information; code development and experimental work are not included in the scope of this program.

The program of Technical Assistance in Reactor Safety Analysis is based on the need for safety assessments that are: (a) independent of industry assessments and (b) intimately coupled to nonproprietary analytical and experimental programs. The use of results of nonproprietary analytical and experimental programs is particularly important because such use allows incorporation of test and analytical results in the technical assistance tasks on a timely and meaningful basis and provides, in turn, an effective feedback on the adequacy of the analytical models and experiments.

Descriptions of specific studies completed during the past year are presented in the following sections. These sections include: (a) evaluation of analytical capabilities for predicting subcooled blowdown behavior, (b) loss-of-coolant accident analysis for a large pressurized water reactor, (c) development of correlations for predicting the maximum containment pressure following a loss-of-coolant accident, and (d) evaluation of the predictive capabilities of the CONTEMPT-PS code.

In the first study, experimental and analytical results of the subcooled blowdown portion of Semiscale Test 820-1, a double-ended 100% pipe area break test were compared. The analytical results were obtained through use of the WHAM computer code for subcooled blowdown[28]. Test 820-1 differed significantly from earlier semiscale tests initiated with smaller break areas in that two-phase conditions existed near the break during the subcooled decompression of the remainder of the system. WHAM was not designed to handle two-phase situations. The results of this study led to the conclusion that WHAM is capable of providing valuable subcooled pressure predictions for this type of break even though two-phase conditions were moving within the blowdown pipe. The initial decompression wave, which is of primary importance in determining subcooled blowdown loads throughout the system, was predicted accurately with WHAM.

In the second study, a loss-of-coolant accident was analyzed for an 840 MW(e) pressurized water reactor with two primary coolant loops, each with a single outlet or hot leg pipe, a single steam generator, two cold leg (or inlet) pipes, and two primary coolant pumps. The RELAP3[2] and THETA1-B[3] computer codes were used to calculate the transient behavior of the system for a fully open, double-ended offset shear of one primary coolant inlet pipe. The system calculational model, input parameters, assumptions, calculated results, and conclusions are presented and discussed. The effects of applying different assumptions and input data are shown by the results of additional RELAP3 and THETA1-B calculations.

In the third study, a correlation was developed to predict the maximum containment pressure following an LOCA in a large PWR. The correlation accounts for containment volume, initial fluid enthalpy in the primary system, heat transfer to the steam generators during blowdown, surface area exposed to the containment internal atmosphere, and the time required for decompression of the primary system. The correlation was compared with predictive data from the safety analysis reports of large PWR plants and found to be in good agreement.

In the fourth study the predictive capabilities of the CONTEMPT-PS pressure-suppression containment code[80] were evaluated. Several methods were used to model the Humboldt Bay and Bodega Bay test facilities with CONTEMPT-PS and the results of predictions obtained for several variables were compared with available test data. The conclusion was reached that the CONTEMPT-PS computer code is conservative; that is, it overpredicts the peak dry-well pressure with respect to most of the test data when the computer runs are made with the assumptions that heat transfer is neglected, the air in the drywell is forced out early in the transient, and essentially 100% of the liquid water is removed from the dry well during the transient.

1. WHAM ANALYSIS OF SEMISCALE DOUBLE-ENDED BLOWDOWN TEST 820-1

(G. E. Gruen)

In the unlikely event of a primary system rupture in a pressurized water reactor, severe structural loads can be generated during the resultant decompression. These loads are expected to be most severe during the subcooled decompression, or subcooled blowdown, that is, during the decompression from the operating pressure to the saturation pressure. The magnitude and duration of pressure gradients across the reactor core or core support structures during subcooled blowdown must be known in order that structural design limits may be established that will result in adequate margins of safety.

The WHAM [28] computer code was developed for analyzing the subcooled decompression of pressurized water reactor systems. Results obtained through use of the code are used as input to other analyses such as the structural analysis (Section IV-2). The capability of WHAM has been demonstrated by comparisons of data predicted by the code with experimental data obtained from various semiscale tests [81]. Predictions from WHAM applicable to the semiscale loop double-ended blowdown Test 820-1 are presented in this section. The greatest uncertainty in the WHAM predictions involves the subcooled loads for the large break area-to-pipe area conditions like those of Test 820-1, because two-phase conditions occur much sooner within the piping near the break than for smaller break area-to-pipe area conditions.

1.1 WHAM Computer Code

The WHAM computer code can model multiple-loop piping networks and, therefore, can be used to predict the subcooled blowdown behavior of a complete reactor primary system. The piping network consists of any number of dead ends, elbows, orifices, multiple junctions, flow passage areas, check valves, pumps, pressurizers or tanks, and exit valves. Hydraulic losses including those due to friction, expansion, contraction, elbows, and orifices can be considered. A transient is generally initiated by opening one or more exit valves in a prescribed manner [28].

WHAM uses the method of wave superposition [28] in the calculation of pressures throughout a network of pipe sections or legs of appropriate lengths and cross-sectional areas. Each leg is divided into segments bounded by nodes, each segment being of such length that the travel time for an acoustic wave across any segment in the system is equal to the fixed time increment specified in the data input portion of the computer program. In a typical reactor system the pressure and temperature vary throughout the system and the variations result in dissimilar acoustic velocities from leg to leg. Under such conditions the distance between adjacent node points (across a segment) varies from leg to leg.

In applications of the WHAM code to physical blowdown situations, the calculational results can be considered accurate only for that portion of the system in which the fluid is subcooled and into which no decompression or compression waves emanating from an area of two-phase properties have penetrated. Previous analyses of data from semiscale 10 and 30% pipe break

area blowdowns show that the fluid in the system remains subcooled during the time major pressure loadings are occurring^[81]. Predictions from these same analyses agree with experimental data to within 10% of the absolute pressures for positions within the semiscale system. The analysis discussed in this section for the 100% double-ended break has to contend with two-phase properties in the system during the time major pressure loadings are occurring.

1.2 Experimental System

The general layout of the major components and interconnecting piping for Semiscale Test 820-1 is shown in Figure 73. This figure also shows the instrument locations pertinent to this analysis. The volume of the system is 13.0 ft³.

The double-ended break of Test 820-1 was initiated by two blowdown nozzle assemblies rather than by a single assembly as employed for previous single-ended break tests. A crossover pipe connecting the two blowdown pipes allowed minor flow rate for developing the desired temperature distribution throughout the system prior to blowdown. A valve in the crossover pipe was closed just before blowdown was initiated.

Initial conditions for Test 820-1 were a nominal temperature of 588°F, a pressure of 2263 psia, and no system flow (pump off). The break area of each nozzle assembly was 100% (0.09 ft²) of the blowdown pipe area such that a blowdown through a total break area of 0.18 ft² was produced. A temperature gradient from the nominal system temperature of 599 to 571°F at the exit planes existed in each blowdown pipe.

The rupture for Semiscale Test 820-1 was initiated by underpressurizing the space between two rupture discs located in each blowdown nozzle assembly. For this method, the upstream disc ruptures first and starts a decompression wave into the system and a column of water out of the system. The column of water hits the downstream disc and starts a compression wave into the system. Finally, the downstream disc ruptures (about 2 msec later) and starts a second decompression wave into the system. Thus, the initial primary decompression process is composed of two parts that are dependent on the characteristics of the individual discs.

The data obtained from semiscale tests have been checked for anomalies and in some cases for repeatability. The experimental subcooled pressure histories presented here can be measured to within ± 7 psi, an acceptable level of confidence^[82].

A system description model depicting the configuration of Test 820-1 was input to WHAM. Also input to WHAM were an initial pressure of 2263 psia and a total discharge area of 0.18 ft². The boundary condition at the exit planes was set at 1180 psia on the basis of an isentropic decompression from 2263 psia and 571°F to saturation conditions. Input sonic velocity for the complete isothermal system was determined theoretically for compressed water at 2263 psia and 588°F.

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FIG. 73 SINGLE-LOOP SEMISCALE DOUBLE-ENDED-BREAK CONFIGURATION.

1.3 Results for Semiscale Test 820-1 Analysis

WHAM predictions for Locations P1, P3, P5, and P6 (Figure 73) were compared with experimental data from the same locations. P1 was a location in the blowdown outlet pipe connected to the pressure vessel. P3, P5, and P6 were locations along the outlet pipe connected to the heat exchanger.

Figure 74 demonstrates for Location P1 the capability of WHAM for predicting experimental pressure wave behavior for large breaks. The simulated offset shear break presents the greatest probability of significant two-phase conditions existing behind the initial decompression wave. The conclusion was reached that the two-phase frontal plane had moved within the outlet pipe 3.5 feet to the flanged-off tee position. Thus, the experimental pressure wave is traveling a much shorter distance for a round trip within the subcooled fluid than the WHAM analytical pressure wave, which is traveling the full 5.8 feet from the vessel to the fluid exit point of the outlet pipe. The two-phase frontal plane within the outlet pipe results in a pressure wave of higher frequency and a more rapid decompression for the experimental data than is indicated by the WHAM-calculated data that are showing the decompression based on a longer round-trip time for the pressure wave.

FIG. 74 EXPERIMENTAL AND ANALYTICAL PRESSURE RESPONSE AT LOCATION P1 FOR SEMI-SCALE TEST 820-1.

The circumstance was different in the other pipe -- the outlet pipe leading to the heat exchanger. The length of piping between the outlet and the heat exchanger was so long (about 25 feet) that the round-trip time of the pressure wave back to the two-phase frontal plane approached the total blowdown time and thus had little effect at Location P3 and no effect at Locations P5 and P6.

Figure 75 shows the comparison of WHAM analytical results and experimental data for Location P3. The WHAM and experimental traces show that the decompression wave reaches Location P3 about 1.7 msec after the rupture. The WHAM-calculated decompression wave passed Location P3 leaving the system pressure near the saturation level. At 15 msec a pressure pulse was calculated to pass Location P3. This pulse resulted from the original decompression wave traveling 21 feet from Location P3 to the heat exchanger tubes and then reflecting back to Location P3 as a compression wave. The experimental data do not show this pressure pulse indicating the two-phase frontal plane has moved within the outlet pipe past Location P3. The lower sonic velocity occurring behind the two-phase frontal plane has slowed the experimental pressure wave returning from the heat exchanger to the extent that it does not return to Location P3 during the time span shown in Figure 75. WHAM does not consider the two-phase conditions within the outlet pipe.

FIG. 75 EXPERIMENTAL AND ANALYTICAL PRESSURE RESPONSE AT LOCATION P3 FOR SEMI-SCALE TEST 820-1.

Figures 76 and 77 show pressure history comparisons at Locations P5 and P6, downstream and upstream of the heat exchanger. The WHAM-calculated results are in close agreement with the experimental results. Excellent transport time agreement also occurs with wave amplitude agreement being only fair at some times. The calculated saturation pressure agrees closely with the experimental saturation pressure.

FIG. 76 EXPERIMENTAL AND ANALYTICAL PRESSURE RESPONSE AT LOCATION P5 FOR SEMI-SCALE TEST 820-1.

FIG. 77 EXPERIMENTAL AND ANALYTICAL PRESSURE RESPONSE AT LOCATION P6 FOR SEMI-SCALE TEST 820-1.

1.4 Conclusions

WHAM appears to give useful subcooled pressure predictions for this 100% break, even though two-phase conditions were moving within the blowdown pipe. The initial decompression wave, which is of primary importance in determining subcooled blowdown loads throughout the system, was accurately predicted with WHAM for this double-ended break. The capability of WHAM is such that calculated pressure histories are sufficiently reliable for use as forcing function input to structural dynamics codes.

2. LOSS-OF-COOLANT ACCIDENT ANALYSIS FOR A LARGE PRESSURIZED WATER REACTOR

(S. E. Jensen)

A loss-of-coolant accident (LOCA) was analyzed for a nuclear power plant having a 840 MW(e) pressurized water reactor with two primary coolant loops, each having a single outlet (hot leg) pipe, a single steam generator, two inlet (cold leg) pipes, and two primary coolant pumps. The following is a brief summary of the analysis.

The RELAP3[2] and THETA1-B[3] computer codes were used to examine the thermal-hydraulic response of the primary system. Containment behavior and structural response to the LOCA were not analyzed as part of this study.

RELAP3 solves the mass, momentum, and energy conservation equations for a set of connected fluid volumes. The core volume can include a typical fuel rod in which the power generation is either specified as a function of time or is computed by using the point kinetics equation with Doppler, fluid temperature, and void reactivity feedback. The fuel-rod cladding surface temperature is computed by using the one-dimensional heat conduction equation. For a single core volume, the cladding surface temperature computed by RELAP3 is that for an average power fuel rod in the core. Heat transfer coefficient correlations are used in determining the energy transfer to the core fluid. Critical mass flow rates are computed from Moody's two-phase critical flow model[83].

THETA1-B computes the surface temperature of the hot spot or maximum power fuel rod. This code solves the two-dimensional heat conduction equation for a single fuel rod and the one-dimensional energy conservation equation for the fluid in the channel surrounding the fuel rod. The fluid inlet flow rate and enthalpy are specified as functions of time, as are the core pressure and fuel rod power generation. These values are obtained from RELAP3 output.

The analysis consisted of several parts: (a) a set of base-case calculations, (b) two additional THETA1-B calculations with variations from the base case, (c) four additional RELAP3 calculations with variations from the base case, and (d) a scoping study of temperature response to various assumed cooling conditions obtained with an analog computer.

The base case consisted of three calculations: (a) a RELAP3 blowdown calculation of the double-ended break of an inlet line, (b) a THETA1-B calculation of the maximum hot-spot temperature obtained by using smoothed flow results from the RELAP3 calculation and not considering the zirconium-steam reaction, and (c) an identical THETA1-B calculation considering the zirconium-steam reaction.

The two THETA1-B change case calculations were: (a) a calculation with the detailed oscillatory flow data from the RELAP3 calculation input instead of the smoothed flow data and (b) an upper limit surface temperature calculation obtained by imposing adiabatic surface conditions on the fuel rod.

The four RELAP3 change case calculations were: (a) an equivalent break calculation for a single volume rather than for two volumes, (b) a calculation with the core barrel check valve removed from the model, (c) a calculation with the discharge coefficient reduced from 1.0 to 0.6, and (d) a calculation of a double-ended hot leg break.

For the analog study, four sets of calculations were made with each set corresponding to one assumed coolant temperature transient. Five maximum fuel rod surface temperatures were computed for each set of calculations, or coolant temperature transient, with each of the five maximum surface temperatures corresponding to a constant assumed heat transfer coefficient having a value between 0 and 100 Btu/hr-ft²-°F.

2.1 Base Case

The base case was chosen to be the double-ended severance of a cold leg of the primary system. RELAP3 was used to compute the thermal-hydraulic response of the primary system and the thermal response of the average power fuel rod in the core. THETA1-B used the RELAP3 output to compute the thermal response of the hot-spot fuel rod.

The model used for the RELAP3 base case calculation is shown in Figure 78. Fluid volumes are indicated by circled numbers and junctions by uncircled numbers. The base case model is comprised of 17 volumes and 27 junctions. The break junctions are Junctions 20 and 21, and Junction 27 is a valve junction which is initially open but closes when the break junctions are opened. This configuration simulates a double-ended offset severance of the cold leg pipe.

The surface temperature of the average-power fuel rod obtained from RELAP3 for the base case is shown in Figure 79. The maximum temperature for the average-power rod is about 1280°F and occurs at 4.5 sec. The time dependence of the core power is shown in Figure 80. Initially, the power level rises because of the positive moderator reactivity coefficient for small void fractions. As more of the core is voided, the coefficient becomes negative and the power rapidly decreases. This behavior is characteristic of the core at the beginning of life. For a core burnup of over 1500 megawatt days per metric ton of uranium, the moderator reactivity coefficient is negative even for small void fractions. The fluid flow rate into the bottom of the core is shown in Figure 81. The flow initially is about 35,000 lb/sec but soon decreases to zero and reverses direction. The flow out of the top of the core is shown in Figure 82. The flow at this location initially oscillates with a frequency of

FIG. 78 BASE-CASE RELAP3 MODEL.

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FIG. 79 BASE-CASE AVERAGE-FUEL-ROD SURFACE TEMPERATURE.

INC-A-17351

FIG. 80 BASE-CASE NORMALIZED POWER.

INC-A-17352

FIG. 81 BASE-CASE CORE INLET FLOW RATE.

INC-A-17353

FIG. 82 BASE-CASE CORE OUTLET FLOW RATE.

about 3 Hz, which is characteristic of chugging. Later, at about 5 sec, the frequency increases to about 7.5 Hz. At this time, the fluid in the core is superheated, as can be seen in Figure 83 which is a plot of the fluid temperature in the core volume. At about this time, the average surface temperature of the average fuel rod drops from its peak value of 1280°F to about 1040°F. Evidently, the chugging flow oscillations in the core volume increase the heat transfer coefficient and cool the fuel rod surface. After 9 sec the negative flow through the core cools the surface of the average fuel rod to about 700°F at 14 sec. The downward flow through the core approaches zero at about 14 sec. The temperature of the surface of the average power-fuel rod subsequently increases to about 900°F. The fluid in the core volume occasionally superheats during this time, which results in spikes in the core inlet flow. At 28.5 sec, a relatively large positive flow spike occurs as the lower plenum is filled with emergency cooling water. At about the same time, the core fluid temperature drops from about 800 to 200°F and a slight cooling of the cladding surface occurs.

Coolant flow from the core flooding tanks began at about 11.5 sec.

For the THETA1-B base case, the RELAP3 calculated core inlet and outlet flow rates were smoothed to remove the oscillations. Whenever the flow rate at the core inlet and outlet had opposite signs, which occurs when the flows are either both into or both out of the core, a zero flow value was input to THETA1-B. Otherwise, the smoothed RELAP3 fluid flow value entering the core volume was input to the THETA1-B code. The smoothed flow is shown in Figure 84. For the hot spot calculations, the flow rate of Figure 84 was reduced by 10% to account for possible flow maldistribution.

FIG. 83 BASE-CASE CORE FLUID TEMPERATURE.

FIG. 84 THETA1-B CORE FLOW RATE.

A zero flow rate will cause the THETA1-B code to compute a heat transfer coefficient based on the Berenson pool film boiling correlation^[84] if liquid exists in the channel or to assume a minimum coefficient of 5.0 Btu/hr-ft²-°F for stagnant steam in the channel. The latter value is taken as a minimum coefficient and represents a reasonable magnitude for the coefficient that would be established by natural convection. In the THETA1-B model, the hot-spot fuel rod was modeled by 11 axial segments. The axial power profile, shown in Figure 85, was used with a peak-to-average power ratio of 1.7. The time dependence of the power input to THETA1-B was the same as in RELAP3. The maximum linear heat rate was 17.7 kW/ft.

FIG. 85 THETA1-B AXIAL POWER PROFILE.

Figure 86 shows the hot-spot fuel rod cladding surface temperature from THETA1-B. The maximum value without metal-water reaction is 2215°F and occurs at 5.5 sec. The second THETA1-B calculation which considered the metal-water reaction is also shown in Figure 86. The maximum calculated temperature for the hot spot for this case was 2295°F at 6 sec. Also shown for comparison is the surface temperature for the average power fuel rod as computed using RELAP3. The hot-spot surface temperature was limited by the negative flow, which started at 5 sec, as shown in Figure 84. The additional heat generation from the metal-water reaction increased the maximum cladding surface temperature by 80°F.

The base case results are as follows:

- (1) The maximum surface temperature of the fuel rod cladding for the average-power rod from RELAP3 was 1280°F at 4.5 sec
- (2) The maximum surface temperature of the fuel rod cladding for the hot-spot rod from THETA1-B was 2215°F at 5.5 sec when the metal-water reaction was neglected
- (3) The metal-water reaction heating increased the hot-spot maximum temperature by about 80°F to 2295°F
- (4) The cladding hot-spot surface temperature rise was terminated by the reverse fluid flow through the core, which began at about 5 sec

FIG. 86 BASE-CASE MAXIMUM AND AVERAGE FUEL-ROD-CLADDING TEMPERATURES.

- (5) The core flooding tanks began discharging fluid into the downcomer at 11.5 sec which is after the initial temperature rise is terminated; hence, the ECC injection did not terminate the temperature rise.

2.2 Change Cases

Two additional THETA1-B cases were run in which (a) the RELAP3 computer core flow rate, including oscillations, was not smoothed and (b) the fuel rod surface heat transfer coefficient was set at zero.

Four additional RELAP3 cases were run in which (a) the break was assumed to occur in one volume instead of two volumes, (b) the core barrel check valves were removed, (c) the discharge coefficient for the flow out the break was reduced to 0.6 from 1.0, and (d) the break was assumed to occur in the hot leg instead of the cold leg.

2.21 THETA1-B Change Cases. When the RELAP3-computed core flow rate, including oscillations, was input to THETA1-B, the maximum temperature of the surface of the fuel rod cladding, shown in Figure 87, was reduced by about 300°F to 1880°F. The time behavior of the cladding surface temperature was basically the same as for the smoothed flow case except for two dips at about 0.5 and 5 sec, both of which were caused by the large flow oscillations that occur at these times (Figures 81 and 82). Because of the additional cooling taking place at 0.5 and 5 sec, the surface temperature for this change case is lower than for the base case for the entire transient.

With the fuel rod surface heat transfer set to zero, the adiabatic case is obtained. The maximum cladding surface temperature for this THETA1-B case is shown in Figure 88 with the base case also shown for comparison. The adiabatic temperature, which is 2550°F at the time the base case temperature peaks (5.5 sec), monotonically increases to 3200°F at 30 sec.

2.22 RELAP3 Change Cases. Each of the four RELAP3 change cases was identical to the base case except for the specified change in either an input parameter or the model.

The first RELAP3 change case consisted of a change in the break configuration model. In the base case, the model simulated a double-ended offset severance of a cold leg pipe. In the base case model, the fluid coming from the steam generator was not mixed with the fluid coming from the downcomer (Figure 78). In the change case model, the fluids coming from the steam generator and the downcomer were allowed to mix before going out the equivalent area break. This change case model corresponds to a break in the side of the pipe rather than to a double-ended offset severance of the pipe. This is the model that is usually used for break areas less than the double ended offset shear and this comparison was made to determine whether the same model was applicable for breaks equivalent in area to an offset shear. The results from this change case were essentially identical to the base case results, so the two break configurations are essentially identical.

The second RELAP3 change case consisted of removing the core barrel check valves. These valves are installed in plants to prevent a potential water seal and steam binding problem. However, examination of the RELAP3 base case results showed that the check valves also provide a path for the coolant

FIG. 87 MAXIMUM FUEL-ROD-CLADDING TEMPERATURES FOR BASE CASE AND DETAILED FLOW CASE.

in the upper plenum to bypass the core on its way to the break. Figure 89 shows the RELAP3 calculated cladding surface temperature for the average-power fuel rod in the core for the base case and the change case in which the core barrel check valves are removed. The maximum average-power fuel rod surface temperature for the change case is about 1210°F compared with 1280°F for the base case. Figure 90 shows the fluid flow into the core and Figure 91

FIG. 88 MAXIMUM FUEL-ROD-CLADDING TEMPERATURES FOR BASE CASE AND ADIABATIC CASE.

FIG. 89 AVERAGE FUEL-ROD-CLADDING TEMPERATURES WITH AND WITHOUT CORE BARREL CHECK VALVES.

FIG. 90 CORE INLET FLOW RATE WITHOUT CORE BARREL CHECK VALVES.

FIG. 91 CORE OUTLET FLOW RATE WITHOUT CORE BARREL CHECK VALVES.

shows the fluid flow out of the core. Comparison of these flows with those for the base case (Figures 81 and 82) shows that the lower temperature of 70°F is the result of an increased positive fluid flow through the core after this time. In the case of the cold leg break, the removal of the core barrel check valves results in a lower initial maximum cladding surface temperature. This result does not imply that these valves are not needed, however, as they were designed to alleviate the water seal and steam binding problem that might occur later in the accident.

For the third RELAP3 change case the discharge coefficient is a multiplier of the fluid flow rate out the break; therefore, this change case is also equivalent to a break of 60% area with a discharge coefficient of 1.0. The cladding surface temperatures for the average-power fuel rod for this change case and for the base case are shown in Figure 92. The maximum surface temperature of the average-power fuel rod is about the same as for the base case, 1280°F, but occurs later in time. Figure 93 shows the flow into the core and Figure 94 shows the flow out of the core. For the change case, the first temperature transient is terminated at 1.5 sec because of the positive flow rate through the core. However, in the base case, the second temperature transient is terminated at 5 sec because of the large flow oscillations that start when the core fluid superheats. A negative flow through the core also starts at about this time, and further reduces the cladding temperature. The second temperature peaks in these two cases are about the same, 1280°F. The third temperature transient in the change case is terminated at 24 sec when flow oscillations start. In the base case, these flow oscillations start at 19 sec and limit the temperature transient to 900°F as compared to 1120°F for the change case. These

FIG. 92 AVERAGE FUEL-ROD-CLADDING TEMPERATURES FOR DISCHARGE COEFFICIENTS OF 1.0 AND 0.6.

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FIG. 93 CORE INLET FLOW RATE FOR DISCHARGE COEFFICIENT OF 0.6.

INC-A-17370

FIG. 94 CORE OUTLET FLOW RATE FOR DISCHARGE COEFFICIENT OF 0.6.

INC-A-17369

flow oscillations start soon after the pressure in the primary system reaches the input containment pressure of 68 psia.

In summary, the second temperature peak is the highest for both cases. For the base case, the temperature rise is terminated initially by the flow oscillations and later by the negative flow through the core. In the change case with the discharge coefficient reduced to 0.6 from 1.0, the large flow oscillations do not occur, and hence the temperature transient continues until the negative flow starts through the core. Based on this analysis, apparently a change case with a discharge coefficient between 0.6 and 1.0 could result in a higher peak temperature for the cladding of the average power fuel rod. The same possibility is expected to exist for the hot spot.

The fourth RELAP3 change case consisted of a double-ended offset severance of a hot leg. In this case, the voids are swept out of the core relatively soon after they are formed and hence the power increases before enough voids are formed to terminate the transient. The time dependence of the power used for this calculation is shown in Figure 95. The RELAP3 model for the hot leg break change case is slightly different from that for the cold leg base case; the model for the hot leg break is shown in Figure 96. Figure 97 shows the cladding surface temperature for the average-power rod for the change case

FIG. 95 NORMALIZED POWER FOR HOT LEG BREAK.

INC-A-17362

FIG. 96 HOT LEG BREAK RELAP3 MODEL.

INC-A-17349

INC-A-17363

FIG. 97 AVERAGE FUEL-ROD-CLADDING TEMPERATURES FOR HOT AND COLD LEG BREAKS.

for the hot leg break. For the first 0.75 sec of the hot leg break the cladding temperature decreases slightly and then rapidly increases to a peak value of 960°F at 1.5 sec. A second peak of 940°F occurs at 15 sec. Figures 98 and 99 show the core inlet and outlet flows. Initially, a large positive flow spike occurs and cools the cladding. Then the flow rate essentially stagnates while the system pressure drops from the hot leg saturation pressure of about 1550 psia to the cold leg saturation pressure of about 1050 psia, as shown in Figure 100, over the time span of 0.5 to 1.5 sec. This time span is the duration over which the cladding surface temperature increases rapidly. Then a large positive flow starts through the core and the temperature transient is terminated. The core flooding tanks start emptying fluid into the downcomer at 7.5 sec and the pressure starts decreasing faster. At about this same time the decaying positive core flow starts decreasing more rapidly toward zero. The core flow rate stagnates at about 14 sec, which is about the same time that the primary system pressure reaches the containment pressure and the flow out the break ceases. Flow oscillations start about this time as does a slight positive core flow. This flow terminates the second temperature transient.

2.3 Analog Simulation

As illustrated by the RELAP3 calculations discussed previously, some uncertainty exists as to the exact fluid conditions (flow, temperature, and quality) existing in the core as the reactor system depressurizes. Because of this uncertainty and some uncertainty in the coefficient of heat transfer between the core and the coolant for any fixed fluid condition, a scoping study was performed of the thermal response of the surface temperature of the core

FIG. 98 CORE INLET FLOW RATE FOR HOT LEG BREAK.

INC-A-17364

FIG. 99 CORE OUTLET FLOW RATE FOR HOT LEG BREAK.

A-17365

FIG. 100 CORE PRESSURE FOR HOT LEG BREAK.

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hot-spot cladding. Selected values of the fluid coolant temperature and surface-to-coolant heat transfer coefficient were imposed on an analog simulation of the hot-spot nuclear fuel rod. The analog simulation was used to compute the transient radial heat conduction in the fuel and cladding for the axial hot spot of the hot rod as a function of the imposed boundary conditions.

One set of results is shown in Figure 101. For these results the coolant temperature is assumed to remain at 600°F for three seconds and then to change to 1400°F for the remainder of the calculation. This temperature history was chosen on the basis of THETA1-B calculations. The initial cladding surface temperature is about 650°F. The cladding surface temperature history is shown for various values of the heat transfer coefficient, h , imposed. The plot with $h = 0$ Btu/hr-ft²-°F corresponds to an adiabatic heatup or limiting condition. Any possible metal-water reaction is not accounted for in these calculations.

FIG 101 ANALOG HOT-SPOT CLADDING SURFACE TEMPERATURE FOR FIRST COOLANT TEMPERATURE TRANSIENT.

For the fluid temperature history assumed in Figure 101, a heat transfer coefficient of about 50 Btu/hr-ft²-°F would be required to limit the maximum cladding temperature to 2300°F. The flat spot at about 1580°F in the temperature response curves corresponds to a phase change in the zircaloy cladding.

The analog simulation utilized thermal properties and geometry typical of those of a large PWR. These differed slightly from those used previously for the RELAP3 and THETA1-B calculations. The decay power curve imposed on the analog simulation was also slightly different. The initial steady state hot-spot power generation, however, was 17.7 kW/ft as was used in the RELAP3 and THETA1-B analysis described previously.

As can be seen by comparing the results with those of Figures 88 and 102, the analog results are essentially identical to THETA1-B results for the same conditions in spite of the model and property differences.

FIG. 102 THETA1-B MAXIMUM FUEL-ROD-CLADDING TEMPERATURE FOR CONSTANT HEAT TRANSFER COEFFICIENT OF 40 BTU/HR-FT²-°F.

Figure 103 shows the thermal response for the case in which the coolant temperature remains at 600°F for three seconds and then increases to 1200°F rather than 1400°F. Comparison of this plot with Figure 101 shows that with a lower coolant temperature a smaller heat transfer coefficient will maintain a cladding temperature at the hot spot below 2300°F. A value of about 42 Btu/hr-ft²-°F is sufficient for the coolant condition of Figure 103.

For the results shown in Figure 104 the coolant was maintained at 600°F for one second rather than three seconds before being increased to 1200°F. The heat transfer coefficient required was about 45 Btu/hr-ft²-°F.

For Figure 105 the coolant temperature was increased to 1400°F after one second at 600°F. The required coefficient was about 55 Btu/hr-ft²-°F.

FIG. 103 ANALOG HOT-SPOT CLADDING SURFACE TEMPERATURES FOR SECOND COOLANT TEMPERATURE TRANSIENT.

FIG. 104 ANALOG HOT-SPOT CLADDING SURFACE TEMPERATURES FOR THIRD COOLANT TEMPERATURE TRANSIENT.

2.4 Conclusions

The conclusions derived from this analysis are as follows:

- (1) For the base case, a double-ended cold leg break:
 - (a) With metal-water reaction heating included, the maximum hot-spot temperature was 2295°F at 6 sec from THETA1-B
 - (b) Without metal-water reaction, the maximum fuel rod cladding surface temperature for the hot-spot rod was 2215°F at 5.5 sec from THETA1-B

FIG. 105 ANALOG HOT-SPOT CLADDING SURFACE TEMPERATURES FOR FOURTH COOLANT TEMPERATURE TRANSIENT.

- (c) The maximum fuel rod cladding surface temperature for the average power rod was 1280°F at 4.5 sec from RELAP3
- (d) The surface temperature rise of the cladding hot-spot was terminated by the reverse fluid flow through the core beginning at about 5 sec
- (e) The core flooding tanks did not start discharging fluid into the downcomer until 11.5 sec; hence, the ECC was not responsible for terminating the initial temperature rise.

- (2) When the core flow oscillations, as calculated with RELAP3, were included in the THETA1-B calculations, the hot-spot cladding temperatures were reduced to about 1880°F as compared to 2215°F for the base case.
- (3) For the four RELAP3 change cases:
- (a) For the large 100% inlet break, an equivalent 100% inlet break from a single volume gave essentially the same results as the double-ended configuration; thus, for this case the two configurations are essentially equivalent.
 - (b) The core barrel check valves inserted in the reactor to prevent a potential water seal in the low portion of the coolant loops and to relieve the steam binding situation also have the effect of providing a path for primary coolant in the external loops and reactor vessel upper plenum to bypass the core in flowing to the break during the blowdown. Increased core flow rates were calculated for the case without the core barrel check valves indicating that a lower hot spot maximum temperature would have been calculated for the case without the core barrel check valves. The maximum core average temperature calculated for the case without the check valves was 1280°F, for the base case.
 - (c) Reducing the discharge coefficient or decreasing the break area may produce a case with a higher maximum cladding temperature or a higher temperature history or both.
 - (d) The hot leg break case results show a much shorter blowdown time, increased flow through the core, and significantly lower core temperatures than the inlet break case (960 versus 1280°F for the maximum surface temperature of the average power fuel rods); thus, the hot leg break does not produce as severe conditions as does the cold leg break.
- (4) The analog studies indicated that for the specific coolant temperature histories assumed (based on THETA1-B calculations) heat transfer coefficients in the range from approximately 40 to 60 Btu/hr-ft²-°F are sufficient to limit the cladding temperature to 2300°F or below. Any possible metal-water reactor was not accounted for in these calculations.

3. DEVELOPMENT OF CORRELATIONS TO PREDICT THE MAXIMUM CONTAINMENT PRESSURE FOLLOWING A LOSS-OF-COOLANT ACCIDENT

(D. C. Slaughterbeck)

The predicted pressure and temperature transients in a containment building following an accidental loss of reactor coolant in a large nuclear generating station affect building and equipment design, calculation of possible leakage of radioactive contaminants to the environment, and overall plant operation and recovery. Assessment of the adequacy of the containment building to isolate the nuclear system from the outside environment requires extensive predictive analyses to determine the pressure and temperature transients. The maximum pressure presents an important load on the containment structure; an independent means of calculating this pressure is desired for design studies, licensing reviews, and to allow rapid checks of more detailed analyses. The following sections summarize the methods and assumptions used to develop maximum pressure correlations. The correlations that result from the analysis can be used to estimate maximum containment pressures given selected primary system and containment parameters.

Since the maximum pressure in dry-well containment buildings normally occurs near the end of decompression but before engineered containment safety systems become operational, correlations for predicting the maximum pressure can be developed from energy source and sink terms whereas operational parameters such as start times for containment sprays systems can be neglected. The predicted pressures from these pressure-energy correlations can then be compared with the pressures from more rigorous analyses to establish the adequacy of the method.

A series of containment problems, solved with the CONTEMPT^[85] computer program, provided data necessary to derive the correlations. Variations in the containment calculations to account for initial fluid enthalpy, energy transfer to and from the primary system, and heat absorption in the containment structures during blowdown were considered.

3.1 Adiabatic Cases

Various amounts of constant-enthalpy water were assumed released in an arbitrary containment volume to simulate the blowdown following a hypothesized loss-of-coolant accident. Several enthalpies were chosen to permit comparisons. In the adiabatic cases the following were assumed:

- (1) Constant volume of the containment (100 ft³)
- (2) Constant enthalpy of the primary system effluent for each case
- (3) No accumulators or other safety systems
- (4) Constant time required for decompression of the primary system (14.4 sec)
- (5) No heat transfer from the containment atmosphere to heat conducting regions or to the liquid region.

These assumptions can be used successfully to approximate the early containment transients during decompression of the primary coolant system because the short duration of this portion of the accident does not allow significant heat transfer to the containment walls.

The maximum containment pressures calculated for the adiabatic cases were plotted against the ratio of energy released to volume of the containment, as shown in Figure 106. In this figure, the relationship among maximum containment pressure, fluid enthalpy, and the ratio of energy released to volume of the containment is explicitly defined for normal ranges of the parameters. The correlating parameter is simply the average specific energy in the containment volume due to blowdown, in Btu/ft³. The curves show how the predicted maximum pressure increases as the specific energy added to the containment volume increases. The assumed initial coolant enthalpy has relatively small effect on the resulting pressure as shown by the similarity of the 550 and 650 Btu/lb curves.

EXAMPLE: A nuclear plant which operates with an average fluid enthalpy of 590 Btu/lb undergoes an LOCA in which the ratio of energy released to volume of the containment is 150 Btu/ft³. The approximate maximum pressure from the figure is 50.5 psig.

FIG. 106 RELATION AMONG MAXIMUM CONTAINMENT PRESSURE, FLUID ENTHALPY, AND RATIO OF ENERGY RELEASED TO CONTAINMENT VOLUME.

For the preceding figure, the assumption was that all of the initial coolant energy was transferred to the containment atmosphere during blowdown. However, for a hypothesized loss-of-coolant accident, the assumption usually made is that only a portion of the initial coolant energy is transferred to the steam generators. The effect of this energy transfer on the containment response was determined by assuming that the primary coolant, initially at 600 Btu/lb, lost a portion of its energy to the steam generators during the blowdown process. The results were compared to results from cases without energy transfer to the steam generators and the differences are shown in Figure 107 as a function of the ratio of initial coolant energy to the volume of the containment. Hence, if the total energy initially stored in the primary coolant is used as an approximation for the energy released, the predicted pressure read from Figure 106 can be modified by using Figure 107 to account for energy transfer to the steam generators. The modified pressure would be found by subtracting from the pressure read in Figure 106, the appropriate pressure read in Figure 107. Although Figure 107 was developed for coolant initially at 600 Btu/lb, the curves are equally applicable for other enthalpies.

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FIG. 107 EFFECT OF HEAT TRANSFERRED TO STEAM GENERATORS DURING BLOWDOWN ON THE MAXIMUM CONTAINMENT PRESSURE.

3.2 Nonadiabatic Cases

Additional containment cases were solved in which the last two assumptions of the foregoing analysis regarding decompression and heat transfer were deleted so that the relationships between decompression time and heat absorption could be defined. The following assumptions were substituted for those deleted:

- (1) Ten heat-conducting structures were added to simulate the building and its internals. The relative area of each of these structures was held constant and the total area was varied. These relative areas were based on the containment structure of Reference 86 and are listed in Table X.

TABLE X
RELATIVE SURFACE AREAS USED IN THE ANALYSIS^[86]

Building Surface	Material	Thickness (ft)	Percent of Total Building Area	Percent of Total Area
Containment dome	Steel-lined concrete	3.5	27.0	18.0
Upper walls of containment	Steel-lined concrete	4.5	31.1	20.7
Lower walls of containment	Steel-lined concrete	4.5	28.0	18.6
Floor of containment	Steel-lined concrete	9.0	13.6	9.6
Miscellaneous Surfaces	Material	Thickness (in.)	Percent of Total Miscellaneous Area	Percent of Total Area
Flat Slab 1	Steel	0.25	13.3	4.4
Flat Slab 2	Steel	0.50	8.3	2.7
Flat Slab 3	Steel	0.75	8.3	2.7
Flat Slab 4	Steel	1.0	25.0	8.3
Flat Slab 5	Steel	1.5	16.7	5.6
Flat Slab 6	Steel	2.0	28.4	9.4

- (2) Heat transfer coefficients for the condensing steam were varied linearly from a value of 2 Btu/hr-ft²-°F at the initiation of the accident to a maximum at the end of decompression. The maximum value was computed from the correlation recommended by Tagami^[87].

Figure 108 shows the difference in containment pressure with and without heat-conducting structures as a function of the ratio of surface area to volume. This ratio is simply the average surface area to which each cubic foot of the containment volume is exposed. The curve was developed for a constant mass addition, a constant energy addition, constant maximum heat transfer coefficient, and a constant time required for blowdown (20 sec). The slight nonlinearity is a result of diminishing the temperature of the containment atmosphere at the high ratios of surface area to volume.

FIG. 108 THE EFFECT OF HEAT ABSORPTION IN CONTAINMENT STRUCTURES ON THE MAXIMUM CONTAINMENT PRESSURE FOR A BLOWDOWN TIME OF 20 SEC.

A family of similar curves for different decompression times can be drawn to define the relationship between containment pressure, surface area, and time for decompression. This relationship is shown in Figure 109 for wide ranges of the parameters. To generalize Figure 109 so that it can be used to estimate pressure reductions for cases with different enthalpies and different ratios of energy released to volume of the containment, a correction factor was derived which reflects the dependence of Q/V in the Tagami heat transfer coefficient and the nonlinearity of the pressure reduction with the heat transfer coefficient. The correction factor, C_T , is plotted in Figure 110 as a function Q/V . To apply the correction factor for a given Q/V , the adjustment read in Figure 109 is multiplied by C_T . This procedure results in a corrected value of pressure reduction that can be used in conjunction with the previous curves.

Collectively, the curves can be used to estimate the maximum containment pressure in a large PWR. For example, a preliminary pressure can be estimated from Figure 106 on the basis of the average fluid enthalpy, and the total energy released per unit of containment volume. If the energy initially in the primary coolant is used as an approximation for the energy released, the pressure can be modified to account for heat transfer to the steam generators during blowdown using Figure 107. A second modification to account for heat absorption in the containment structures can be obtained from Figures 109 and 110. The following illustrative example is given to demonstrate the method. The values are based on calculations by Idaho Nuclear Corporation of a hypothesized accident in a typical four-loop plant.

FIG. 109 REDUCTION IN MAXIMUM CONTAINMENT PRESSURE AS A RESULT OF ENERGY ABSORPTION IN CONTAINMENT STRUCTURES FOR VARIOUS RATIOS OF SURFACE AREA-TO-VOLUME AND DECOMPRESSION TIME.

FIG. 110 CORRECTION FACTOR VERSUS RATIO OF ENERGY RELEASED TO VOLUME OF THE CONTAINMENT.

3.3 Estimate Based on Idaho Nuclear Corporation Calculations with RELAP3^[2]:

Energy released during decompression = 3.08×10^8 Btu

Mass released during decompression = 5.37×10^5 lb_m

Time for decompression (to 50 psia) = 22 sec

Exposed surface area in containment = 2.16×10^5 ft²

Volume of containment = 1.98×10^6 ft³

then

$$\frac{\text{Energy released}}{\text{Containment volume}} = 155 \frac{\text{Btu}}{\text{ft}^3}$$

and

$$\frac{\text{Energy released}}{\text{Mass released}} = 575 \frac{\text{Btu}}{\text{lb}_m}$$

also,

$$\frac{\text{Surface area}}{\text{Containment volume}} = 0.109 \text{ ft}^{-1}.$$

Then from Figure 106 the maximum containment pressure can be estimated to be about 51.5 psig using 155 Btu/ft³ and 575 Btu/lb_m. This value is modified by -4.5 psi from Figures 109 and 110 (4.1 times 1.1) to give a final estimate of 47.0 psig. An actual CONTEMPT run of this case gave 47.7 psig.

To determine the usefulness of the correlations developed, predictive data were gathered from several preliminary safety analysis reports. These data are shown in Table XI. The listed containment pressures are the maximum values achieved near the end of the primary system decompression and may not always be the overall maximum in the pressure transient.

The vendor data are compared to the energy-volume correlation in Figure 111. The solid line is the prediction from the energy-volume correlation for an average enthalpy of 590 Btu/lb, typical of most of the plants in Table XI. The typical large PWR of Figure 111 is that of Reference 86. No heat transfer to the steam generators or to the heat conducting regions was included for the predictions from the energy-volume correlation. The agreement between the calculated curve and the vendor data is good considering that the vendor data reflect many different assumptions relative to the behavior of the primary system and safety systems as well as assumptions relative to the containment pressure reduction due to heat transferred to the steam generators or to the heat conducting regions.

TABLE XI

VENDOR DATA ^[a] USED IN COMPARISONS WITH MAXIMUM PRESSURE CORRELATIONS ^[86]

	Diablo Canyon	Rancho Seco	Davis Besse	Palisades	Crystal River 3 and 4	Midland #1	Turkey Point	Oconee
MW(t)	3250	2452	2633	2200	2452	2452	2097	2452
MW(e)	1060	800	872	722	825		652	839
Vendor	Westinghouse Electric Corporation	Babcock & Wilcox	Babcock & Wilcox	Combustion Engineering Inc.	Babcock & Wilcox	Babcock & Wilcox	Westinghouse Electric Corporation	Babcock & Wilcox
Containment Volume (10 ⁶ ft ³)	2.61	2.005	2.8	1.64	2	1.67	1.55	1.91
Primary System Volume (ft ³)	12,600	11,800	11,440	10,900	11,800	11,800	9,388	11,478
Containment Maximum Pressure ^[b]	42	53.7	34	51	52.1	53	50	53
Corresponding Energy Released (10 ⁶ Btu)	332	310	300	270	320	300	260	320
Average Enthalpy (Btu/lb)	575	588	591	571	588	588	580	588
Temperature of Outlet Coolant (°F)	602	603	608	591	603	602.8	600.5	604.7

[a] Data were taken from recent safety analysis reports and may not all be on the same basis.

[b] The maximum pressure was taken to be due to blowdown only; additional energy may produce higher pressure.

FIG. 111. COMPARISON OF PRELIMINARY ENERGY-VOLUME CORRELATION WITH VENDOR PREDICTIVE DATA.

Although the analysis of all possible combinations of variables is impractical, a reasonable assumption is that many of the variables are of secondary importance and simplifying assumptions can be made to derive approximate correlations for a general class of containment problems. As such, the correlations derived are intended to be used to approximate the maximum containment pressure rather than to give an exact solution of the containment pressure.

4. EVALUATION OF THE PREDICTIVE CAPABILITIES OF THE CONTEMPT-PS COMPUTER CODE BY COMPARISON OF CALCULATED RESULTS WITH RESULTS FROM THE HUMBOLDT BAY AND BODEGA BAY PRESSURE SUPPRESSION TESTS

(R. L. Miller)

Studies for the evaluation of the CONTEMPT-PS^[80] computer code were made by comparing calculations made with this code with selected cases from Humboldt Bay^[88] and Bodega Bay^[89] series of pressure suppression tests.

The design basis accident (DBA) for General Electric Company boiling water reactors is the instantaneous severance of a water recirculation line resulting in a double-ended offset break. The test cases from the Humboldt Bay and Bodega Bay series selected for detailed study included scaled-down break areas corresponding to the DBA and larger break areas. The break size determines the rate of addition of mass and energy to the dry well and is the variable that has the greatest influence on peak dry well pressure for a given containment design. The location of the break relative to the entrance of the vent system and the resistance of the vent are important in determining the peak dry well pressure; however, these variables are of secondary importance to mass and energy addition in determining peak dry well pressure.

In performing pressure-suppression analyses, the rate at which air, steam, and entrained liquid water flow through the vent must be calculated. The present state-of-the-art of two-phase flow calculations does not permit, a priori, accurate calculations of the rate at which air and entrained liquid enter the vent. Accordingly, different rates of air and entrained liquid movement were analytically investigated in the present study. A bounding type of approach was taken in this study; that is, no attempt was made to calculate dry well and wet well pressures exactly, but rather a calculational approach that results in a conservative estimate (over-prediction) of peak dry well pressure was sought.

The RELAP3^[2] computer code was used to calculate the mass flow rate and specific enthalpy from the simulated reactor vessel to the containment vessel of the test facility.

Sufficient information is unavailable on the temperature and pressure transducers and the attendant data acquisition instruments^[88,89] to assess the accuracy and precision of the experimental data.

4.1 Analytical Model

The development of analytical models of the Humboldt Bay and Bodega Bay test facilities was necessary to generate input data for the CONTEMPT-PS code. The modeling included the mass and energy addition rate, the dry well, the vent system, and the wet well.

4.1.1 Mass Flow Rate and Energy. Blowdown rate data for the Humboldt Bay and Bodega Bay test series are not available in the literature. Mass flow rate and energy data for input to the CONTEMPT-PS code were obtained by analysis of the pressure history of the simulated reactor vessels in these tests through use of the RELAP3 blowdown code^[2] as noted previously. In this technique RELAP3 was required to predict a water discharge time (within ± 0.1 sec) equal to that reported by Ashworth et al^[90]. In using the RELAP3 code in this manner the assumption was made that the mass flow rates predicted by the code would be the same as those of the actual tests. Several examples of pressure histories for the simulated reactors are shown in Figures 112 through 115. The experimental data were taken from Reference 90. The divergence between the test pressure history and that predicted by the RELAP3 code is attributed to inaccuracies in available test data; for example, the inaccuracies are due to the graphs from which the pressure histories were taken and to inaccuracies in modeling the test assemblies with the RELAP3 computer code.

FIG. 112. COMPARISON OF THE SIMULATED REACTOR VESSEL PRESSURE HISTORY WITH THAT PREDICTED BY RELAP3 FOR HUMBOLDT BAY TEST 15.

FIG. 113. COMPARISON OF THE SIMULATED REACTOR VESSEL PRESSURE HISTORY WITH THAT PREDICTED BY RELAP3 FOR HUMBOLDT BAY TEST 16.

FIG. 114 COMPARISON OF THE SIMULATED REACTOR VESSEL PRESSURE HISTORY WITH THAT PREDICTED BY RELAP3 FOR HUMBOLDT BAY TEST 17.

FIG. 115 COMPARISON OF THE SIMULATED REACTOR VESSEL PRESSURE HISTORY WITH THAT PREDICTED BY RELAP 3 FOR HUMBOLDT BAY TEST 33.

Output from RELAP3 includes mass flow rate, in lb_m/sec , and the corresponding specific enthalpy as functions of time. The CONTEMPT-PS computer code interpolates between data points in the table of mass and energy addition to the dry well. Graphs of mass flow rate as a function of time were prepared to find those points, in time, at which the slope changed significantly; these points were then selected as input datapoints for running a problem. An example of these input data is shown in Table XII for Humboldt Bay Test 15.

TABLE XII
DATA FROM RELAP3 FOR TIME, MASS FLOW RATE, AND ENTHALPY
FOR INPUT TO CONTEMPT-PS, HUMBOLDT BAY TEST 15

Time (sec)	Mass Flow Rate (lb_m/sec)	Specific Enthalpy (Btu/lb)
0	106.8	580.6
2	104.5	576.1
4	102.3	571.1
6	101.9	565.5
8	100.6	559.3
10	98.2	552.5
12	94.3	545.1
14	92.8	536.7
16	91.4	527.0
17.18	89.4	521.0
17.22	23.7	1109.1
20	12.7	1118.0

4.12 Dry Well. The configuration of the dry well and the structures internal to the dry well are not modeled in CONTEMPT-PS except as they may affect the flow of fluid into the vent entrance or except as these structures are modeled for heat loss calculations.

The effect of the variation of internal fixtures has not been studied in sufficient detail. The vent entrance and any baffle plate in front of this entrance were considered to be part of the vent system rather than the dry well.

The CONTEMPT portion of the code has provision for several heat loss calculations[85]. Heat losses have the effect of reducing the pressure in the containment. Use was made of the heat transfer calculational capabilities of the CONTEMPT portion of the code to obtain predictions of the peak pressures found in the Humboldt Bay and Bodega Bay pressure suppression tests.

The available structural data for the Humboldt Bay and Bodega Bay test assemblies do not include the wall thickness of the various members; these thicknesses and the dimensions of the structures are necessary for heat loss calculations. This lack of data for the construction of the Humboldt Bay dry well required that several assumptions be made. The thickness, t , of the cylindrical walls of the dry well was estimated from the equation[91]:

$$t = PR / (SE - 0.6P)$$

where P is the design pressure of the vessel (assumed to be 200 psi for the Humboldt Bay dry well), R is the radius in inches, S is the maximum allowable stress (taken to be 15,000 psi[91] for carbon steel), and E is the joint efficiency (taken to be approximately 0.6[91]). By using a diameter of 39.0 inches, a thickness of about one inch was calculated. The height of the cylindrical portion was adjusted to 6.93 feet to give a volume of 230 ft³.

The ends of the dry well were assumed to be torispherical heads[91]; the thickness of these heads was calculated from[91]

$$t = 0.885 PL / (SE - 0.1P)$$

where L is the radius of the inside of the crown in inches and the other variables are as previously defined. A thickness of about 1.5 inches was calculated by using a value of $L = 78$ inches. The combined area of the two heads was 71.1 ft².

The Bodega Bay dry well was analyzed in a similar manner except that the design pressure was 150 psi. The cylindrical portion was estimated to have a thickness of about 0.75 inch and a height of 28 feet. The heads were estimated to be 1.25 inches thick and to have a combined area of 78.8 ft².

4.13 Vent System. The Humboldt Bay and Bodega Bay vent systems were geometrically similar, differing in pipe diameter, pipe length, and the replacement of a short radius elbow by a capped tee. The pressure drop in the vent systems may be charged to frictional losses in straight sections and to changes in cross-sectional area or flow direction in fittings.

The pressure drop in a straight pipe due to dissipative effects may be expressed as:

$$\Delta p = \frac{fL}{D} \left(\frac{\rho v^2}{2g_c} \right)$$

where f is the Moody friction factor, L is the length of a pipe of diameter D , v is the fluid velocity, ρ is the fluid density, and g_c is the gravitational constant. Changes in cross-sectional area, as in the vent entrance, or changes in direction, as in branched flow through a tee or flow through an elbow, cause an additional pressure drop given by:

$$\Delta p = K \frac{\rho v^2}{2g_c}$$

where K is the irreversible pressure drop flow loss coefficient for changes in area, direction, or shape. The total pressure drop is then:

$$\Delta P_t = K \cdot \frac{\rho v^2}{2g_c} + \frac{fL}{D} \cdot \frac{\rho v^2}{2g_c}$$

As a first approximation K may be equated to $\frac{fL_{eg}}{D}$ where this L_{eg} is the equivalent pipe length.

Characteristic irreversible pressure drop loss coefficients for two-phase flow in fittings are not generally available. The CONTEMPT-PS computer code uses a single-phase pressure drop loss coefficient, based on downstream velocity, and multiplies this coefficient by a two-phase multiplier to obtain the pressure drop for two-phase flow. Single-phase pressure drop loss coefficients were taken from Reference 92. The Baroczy^[93] two-phase pressure drop correlation is used in the CONTEMPT-PS computer code.

Schematic diagrams for the Humboldt Bay and Bodega Bay vent systems are shown in Figures 116 and 117 and are described in Tables XIII and XIV, respectively. One method of modeling the vent system is to define each length of straight pipe and each pipe fitting as a separate pipe element. Straight pipes are further classified in the CONTEMPT-PS code as Type 1 pipe elements and fittings are classified as Type 2 pipe elements. In using this technique, the length of each straight section together with its absolute roughness and the pressure drop loss coefficient of each Type 2 element must be considered. The vent entrance was modeled as one Type 1 (Section 1 of Figures 116 and 117) and two Type 2 (Sections 2 and 3 of Figures 116 and 117) pipe elements. The first section in the vent is required to be a Type 1 element; some versions of the CONTEMPT-PS computer code require that the second section be a Type 2 element. The second Type 2 element (Section 3 of Figures 116 and 117) was used to represent a constant cross-section element and was used to account for the pressure drop in the vent entrance.

An absolute roughness of Type 1 sections was taken from Moody's data^[94]. The vents were assumed fabricated from commercial steel with an absolute roughness of 0.00015 foot.

A second method of modeling the vent system is to find the equivalent length of the vent fittings shown in Figures 116 and 117 and described in Tables XIII and XIV for the Humboldt Bay and Bodega Bay vent systems, respectively. The total equivalent length of the vent fittings plus the length of the straight pipe was arbitrarily divided into 10 to 13 sections. These sections were generally of decreasing length with the final section having a length of 1 to 2 feet; the sum of these section lengths was equal to the total equivalent length of the vent system.

A third technique, much like the second, is to input the equivalent length of each fitting and each straight section into the code in the same order they occur in the vent system. In modeling the vent in this manner, various fittings were assumed separated sufficiently far that they did not interact, or that any interaction was accounted for in the appropriate loss coefficient.

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FIG. 116 SCHEMATIC OF THE HUMBOLDT BAY VENT SYSTEM.

FIG. 117 SCHEMATIC OF THE BODEGA BAY VENT SYSTEM.

4.14 Water Carryover. The water carryover model used in CONTEMPT-PS has the following characteristics. At the end of any given time step the water remaining after the expansion is retained in the vapor region of the dry well rather than being assigned to the floor of this chamber. Retaining the water in the vapor region allows the liquid entrained in the atmosphere to enter the vents without having to be collected off the floor. This model is used until the pressure in the wet well equals or exceeds the dry well pressure. Subsequent to this time the water is assigned to the floor and calculations are made as in the original CONTEMPT version of the code.

In the code, the mass fraction of water in the vent entrance (RW) and the mass fraction of water in the dry well atmosphere (RPW) are related by

$$RW = (RPW)(PCO)$$

where PCO is a water carryover parameter used to account for the separation of the liquid and vapor phases. This parameter may be thought of as the ratio

TABLE XIII

DESCRIPTION OF THE HUMBOLDT BAY VENT SYSTEM
AND APPLICABLE IRREVERSIBLE PRESSURE DROP LOSS
COEFFICIENT, K, FOR EACH TYPE 2 SECTION

Section	Description	K	Length (ft)
1	Entrance section, required by code, Type 1	---	0
2	Entrance reducer section, required by code	0	
3	Pressure drop section for entrance	0.85	
4	Straight pipe, Type 1	---	1.0
5	Tee, capped with flow through branch	1.50	
6	Straight pipe, Type 1	---	15.0
7	Long Radius elbow	0.14	
8	Straight pipe, Type 1	---	6.0
9	Convoluted expansion joint	0.10	
10	Straight pipe, Type 1	---	6.0
11	Short radius elbow	0.17	
12	Straight pipe, Type 1	---	21.0
13	Straight pipe, vent exit, Type 1	---	2.0

Total K from fittings 2.76

Total length of 1.104-ft ID straight pipe = 51 ft

Assumed Moody friction factor = 0.013

$K = fL/D = 0.53$

Total K 3.29

Total equivalent length = $L = KD/f = \sim 280$ ft

of the mass fraction of water in the vent entrance to the mass fraction of water in the dry well atmosphere, that is, $PCO = RW/RPW$. As PCO is varied from zero to unity the fluid entering the vent varies from dry stream, or air ($PCO = 0$), or both, to fluid that has the same wetness as the dry well atmosphere ($PCO = 1$). (The liquid water carried into the wet well varies from zero for

TABLE XIV

DESCRIPTION OF THE BODEGA BAY VENT SYSTEM AND APPLICABLE
IRREVERSIBLE PRESSURE DROP LOSS COEFFICIENT,
K, FOR EACH TYPE 2 SECTION

Section	Description	K	Length (ft)
1	Entrance section, required by code, Type 1	---	0
2	Entrance reducer section, required by code	0	
3	Pressure drop section for entrance	0.85	
4	Straight pipe, Type 1	---	1.0
5	Tee, capped with flow through branch	1.50	
6	Straight pipe, Type 1	---	18.0
7	Long radius elbow	0.14	
8	Straight pipe, Type 1	---	6.0
9	Convoluted expansion joint	0.10	
10	Straight pipe, Type 1	---	6.0
11	Tee, capped with flow through branch	1.50	
12	Straight pipe, Type 1	---	13.0
13	Straight pipe, vent exit, Type 1	---	1.0

Total K from fittings 4.12

Total length of 1.958-ft-ID straight pipe = 45 ft

Assumed Moody friction factor = 0.013

$K = fL/D = 0.30$

Total K 4.42

Total equivalent length = $L = KD/f \approx 666$ ft

PCO = 0 to 100% of that entrained to the dry well atmosphere for PCO = 1.)
PCO may be varied to account for separation of the liquid phase from the
vapor phase in the dry well; however, for any given run PCO is fixed by the user.

4.15 Wet Well. Several input parameters are necessary to perform vent clearing calculations and to determine the mass and energy balance in the wet well. The input parameters are the depth of submergence of the vent pipe in the pool, the depth of water from the vent exit to the bottom of the pool, the area of the pool annulus, the volume of the wet well atmosphere, the total volume of the wet well, the initial mass of air in the wet well, the temperature and pressure of the wet well atmosphere, and the initial mass of water vapor in the wet well atmosphere.

4.2 Results

A series of computer runs was made to determine the effect of changes in the water carryover parameter and vent resistance on the peak dry well pressure. The air forced from the dry well to the wet well was determined. The pressure in the wet well was calculated with respect to the amount of air that entered from the dry well. The effect of heat transfer on peak pressure was investigated using the Tagami coefficient, the Uchida coefficient, and various constant coefficients.

Vent clearing calculations were made for Humboldt Bay tests for a wide range of blowdown rates and submergence depths. The time to clear the vent was found to decrease with increasing blowdown rate and, as expected, the longer vent required a longer clearing time.

4.21 Water Carryover. To determine the effect of changes in PCO and vent resistance (fL/D) on the amount of water carried into the wet well, a series of computer runs was made in which the vent resistance and PCO were varied. The results of this study are compiled in Table XV. Water carryover is calculated as the percent of water initially in the simulated reactor vessel that is carried into the wet well.

The data shown in Table XV indicate that the percentage of water carryover is strongly dependent on PCO. This result is expected from the relation for the mass fraction of water in the vent entrance discussed in Section 4.14. The change in water carryover due to variations of vent resistance is small and tends to decrease as the vent resistance increases. This effect could be anticipated because the amount of fluid entering the vent decreases with increasing vent resistance.

4.22 Peak Dry Well Pressure. The computer runs used to study the water carryover parameter (PCO) were analyzed to gain an insight into peak dry well pressure changes due to changes in the water carryover parameter, PCO, and the vent resistance, in terms of fL/D .

The data for these runs are tabulated in Table XV and plotted in Figure 118. Conditions other than PCO and fL/D are no heat transfer and early water carryover. If an equation of the form

$$\log P = n \log (fL/D)$$

is assumed, the slope, n , is approximately 0.25 for the curves shown. Table XV shows that the variation of percent water carryover with fL/D is small. However, for a given value of fL/D the strong dependence of both peak pressure and percent water carryover on the value of PCO is evident. Due to the increase

TABLE XV

RESULTS OF CONTEMPT-PS CALCULATIONS FOR HUMBOLDT BAY
TEST 15 WITH EARLY AIR CARRYOVER AND NO HEAT TRANSFER

PCO	fL/D	Peak Containment Pressure (psia)	Water Carryover (%)
1	3.29	55.6	99.3
	2.91	53.7	99.3
	2.53	52.0	99.4
0.8	3.29	53.5	98.1
	2.91	51.8	98.3
	2.53	50.2	98.3
0.7	3.29	51.6	93.6
	2.53	48.5	93.0
0.6	3.29	48.8	86.1
	2.53	46.0	86.0
0.5	3.29	46.3	78.5
	2.91	44.9	78.5
	2.53	44.2	79.0
0	3.29	44.2	57.2
	2.53	44.2	57.3

in fluid density with increasing mass fraction of water in the vent (as controlled by PCO) the pressure drop through the vent increases with increasing PCO resulting in a predicted increase in peak dry well pressure.

The break of the curve of Figure 118 for PCO = 0.5 and the apparent zero slope for PCO = 0 are explained as follows. The peak dry well pressure for PCO = 0 is controlled by the vent clearing transient; that is, the peak pressure is reached just at the time the vent clears. Thus, the peak dry well pressure is independent of fL/D for this situation only. The same is true for PCO = 0.5 as long as log fL/D is less than about 0.44. As log fL/D is increased above 0.44 for PCO = 0.5 the vent resistance becomes sufficiently large with respect to the resistance offered during the vent clearing transient (moving out the water

FIG. 118 CORRELATION OF PEAK DRY-WELL PRESSURE AND PCO FOR HUMBOLDT BAY TEST 15.

slug) that the peak pressure is then controlled by the vent resistance. If fL/D increased beyond 3.29 ($\log fL/D > 0.52$) then the peak pressure would also eventually become dependent on fL/D for $PCO = 0$. Similarly, for $PCO > 0.5$, if fL/D is reduced sufficiently the peak pressure would become independent of fL/D and controlled by the vent clearing transient.

The assumption of early air carryover (the air in the dry well was assumed to be forced into the wet well prior to vent flow of the steam-water fluid) used in making comparison runs listed in Tables XVI and XVII had rather pronounced consequences with respect to the Bodega Bay tests. The back pressure created by the air in the wet wall, especially during the portion of the blowdown when mass and energy addition to the containment was greatest, caused pressures in the dry well to be greatly overpredicted. As a result of these calculations, the conclusion was reached that considerable air was retained in the dry well, at least during the early portion of the blowdown.

TABLE XVI
SUMMARY OF RESULTS ON COMPARISON CALCULATIONS
FOR SELECTED HUMBOLDT BAY TESTS

Test ^[a]	rL/D ^[b]	Dry Well Pressure (psia)		Wet Well Pressure (psia)		Water Carryover (%)		Break Area Design Basis Accident Break (%)
		Test	CONTEMPT-PS	Test	CONTEMPT-PS	Test	CONTEMPT-PS	
5	3.29	57.7 ^[c]	53.00	21.7	24.2	97.1	99.0	100
6	2.94	40.7	53.6	NA	24.6	83.0	99.0	100
7	2.94	43.7	53.6	22.7	24.6	85.0	99.0	100
8	3.29	45.7	57.4	23.2	25.1	60.7	99.0	100
9	3.29	59.7	71.8	22.2	24.3	63.7	98.5	164
10	3.29	71.7	80.4	21.2	24.2	65.2	93.6	200
12	3.29	52.7	60.0	22.7	25.6	[d]	95.5	100
13	3.29	46.7	55.0	22.8	22.5	69.8	99.3	100
14	3.29	46.7	56.0	22.2	23.9	73.4	99.3	100
15	3.29	47.7	56.0	23.5	23.9	89.4	99.3	100
16	3.29	78.7	99.9	22.7	24.0	92.1	99.5	200
17	3.29	104.7	125.4	23.2	23.4	93.9	99.3	300
20	3.29	47.7	51.9	21.2	22.9	90.6	99.5	100
24	3.29	46.7	56.0	24.0	28.9	92.9	99.2	100
25	3.29	46.7	56.3	23.2	23.5	61.8	99.5	100
26	3.29	47.7	56.3	23.2	23.5	[d]	99.5	100
27	2.94	39.7	50.9	22.7	22.6	81.7	99.5	100
28	2.94	39.7	50.9	22.5	22.6	82.5	99.5	100
29	3.23	47.7	53.7	21.7	23.5	92.6	97.7	100
30	3.23	49.5	53.5	29.2	23.3	91.4	96.6	100
32	3.23	73.7	84.8	21.9	23.1	90.5	98.5	200
33	3.29	142.7	158.1	23.2	21.5	95.1	98.2	400
36	3.29	49.7	56.0	22.9	22.9	90.1	99.3	100
37	2.94	47.7	53.2	22.7	24.2	87.8	99.3	100
38	2.94	46.7	53.8	[d]	23.6	91.1	99.3	100
39	2.94	46.7	54.3	[d]	22.6	[d]	99.2	100
44	3.29	50.7	54.7	22.2	22.5	98.1	99.5	100
45	3.29	79.9	82.9	22.5	23.1	91.2	99.3	200

[a] Some tests listed in Reference 88 have been omitted because the break area was significantly smaller than the design basis accident (Tests 31, 23, and 43) or because insufficient data were available for accurate modeling (Tests 11, 40, 41, and 42).

[b] Variations in vent geometry or length are responsible for differences in these values.

[c] The peak dry well pressure for this test is significantly larger than for similar tests (for example, Tests 15 and 44) suggesting that the experimental pressure shown is in error.

[d] Data for these points are not available.

The peak dry well pressure for the Bodega Bay tests was not underpredicted by any of the air management approaches. An overprediction of more than 50% resulted for the largest break area test (250% of DBA). The overpredictions of the 100% of DBA breaks were as high as 91% (Bodega Bay Test 9). These very large overpredictions were attributed to the relatively large dry well and the manner in which the air was managed during the calculations; that is, early air carryover was assumed.

4.23 Wet Well Pressures. The pressure in the wet well is due largely to the air that enters from the dry well during the blowdown. CONTEMPT-PS is programmed to calculate mass and energy balances in the wet well on the basis of an assumed equilibrium between the atmosphere and liquid regions in this chamber; that is, the atmosphere is saturated with water vapor at the

TABLE XVII

SUMMARY OF RESULTS ON COMPARISON CALCULATIONS FOR
SELECTED BODEGA BAY TESTS

Test [a]	fL/D [b]	Dry Well Pressure (psia)		Wet Well Pressure (psia)		Temperature (°F)				Break Area
		Test	CONTEMPT-PS	Test	CONTEMPT-PS	Dry Well Test	CONTEMPT-PS	Wet Well Test	CONTEMPT-PS	Design Basis Accident Break (%)
5	4.11	37.7	51.6	[c]	16.0	[d]	283	[d]	87	100
6	4.11	23.7	25.1	[c]	16.0	[d]	240	[d]	86	26
7	4.11	45.7	54.0	39.7	51.9	257	286	128	120	26
8	4.11	38.7	59.5	35.7	52.6	240	292	111	126	58.6
9	4.11	35.7	68.1	31.7	53.1	229	301	110	130	100
10	4.11	45.7	74.2	33.7	55.4	222	307	108	129	133.5
11	4.11	56.7	88.1	32.7	54.5	220	319	117	132	200
12	4.11	69.7	105.7	35.7	55.0	230	332	131	147	250
14	4.42	51.7	69.5	41.7	52.2	248	302	97	118	100
15	4.42	54.7	71.2	40.7	52.4	242	303	100	126	133
16	4.42	77.3	98.0	40.7	53.1	239	326	110	132	250
17	4.42	51.7	68.3	42.7	53.9	252	301	105	125	100
19	4.42	50.7	64.7	42.7	51.2	247	297	92	113	100
20	4.42	66.7	10.4	41.7	50.7	239	320	97	122	200
21	4.42	50.7	69.0	41.7	52.5	240	302	96	121	100
22	4.42	50.7	68.3	42.7	52.7	254	301	101	125	100
23	4.42	51.7	82.3	43.7	57.2	253	314	97	143	100
24	4.42	48.7	67.1	36.7	49.3	261	300	110	131	100
25	4.42	50.7	66.7	42.7	52.0	251	300	102	120	100
26	4.42	47.7	52.5	43.7	52.6	268	326	102	120	38
27	4.42	52.7	70.3	43.7	54.0	245	303	112	134	100

[a] Some tests listed in Reference 89 have been omitted because the break area was significantly smaller than the design basis accident break (Tests 13 and 18) or because insufficient data were available to permit accurate modeling (Tests 1 through 4 and 39 through 45).

[b] Variations in vent entrance geometry or length are responsible for differences in these values.

[c] The wet well was open to the atmosphere in this test.

[d] Data for these points are not available.

temperature of the liquid region at all times. As the steam-water mixture enters the wet well, the steam is totally condensed and added, with the entering water, to the liquid water in this chamber. The increase in water temperature due to its absorption of the heat of vaporization of the condensing steam results in an increase in the pressure of the air and in the vapor pressure of the water. For comparison purposes the code was programmed to assume that the air mass in the dry well of the test facility was forced into the wet well atmosphere early in the decompression.

The wet well pressures predicted by CONTEMPT-PS for the Humboldt Bay tests are generally within 4 psi of the test pressure (Table XVI). The Bodega Bay tests show a greater divergence between predicted and test pressures for the wet well (Table XVII). The greater divergency noted for the Bodega Bay tests is largely due to the early carryover air forced into the wet well, as discussed in Section 4.22.

The predicted wet well pressure would have been lower if credit had been taken for heat loss from the dry well atmosphere or if normal air behavior was allowed (Section 4.24).

4.24 Air Removal From the Dry Well. During blowdown, the air is forced from the dry well into the wet well where it attains thermal equilibrium with the steam and water in this chamber and thus increases in pressure as the wet well temperature increases. The back pressure exerted by this air has significant effect on the vent flow rate and the dry well pressure. This effect may be seen by examining the data for Bodega Bay Tests 6 and 7, Table XVII. In Bodega Bay Test 6, the wet well was open to the atmosphere so that the air originally from the dry well escaped about as rapidly as it entered the suppression chamber. Bodega Bay Test 7 was identical to Test 6 except that the air was retained in the suppression chamber in Test 7 thereby developing a back pressure that resulted in a peak pressure that was 29 psi greater than in Test 6.

The dry well in the Bodega Bay system was relatively larger than in the design of the full scale plant. The relatively larger dry well resulted in the presence of a corresponding relatively larger air mass which could be forced into the wet well and which resulted in high dry well pressures. To study air behavior, two tests were chosen for detailed study. Bodega Bay Test 17 had a peak dry well pressure late in the blowdown transient, at about 8 sec. This time corresponds closely to the time that the water discharge from the simulated reactor vessel is completed. Bodega Bay Test 20 had a peak dry well pressure early in the blowdown transient, at about 2.5 sec. This time is about half the time required for the water to be discharged from the simulated reactor vessel. Because of the difference in time at which the peak pressure is reached, these two tests were chosen to study air behavior during blowdown.

As water enters the containment during any time step, it is instantaneously mixed with the other fluid in the dry well vapor region. The air in the dry well may be managed in three general ways, all investigated in this study. In normal air removal, the homogeneous, two-component, two-phase air-steam-water mixture in the dry well atmosphere is removed through the vents and any water separation is accounted for by the water carryover parameter PCO. In early air removal the air leaves the dry well as a single-component gas before any of the steam-water fluid. When all of the air is retained in the dry well it is separated from the steam and water and the latter flow through the vents leaving the air in the dry well. Table XVIII presents data on the effect of air removal on peak pressure for Bodega Bay Tests 17 and 20.

- (a) Normal Air Carryover. The model for air removal from the dry well has been described previously [80]. When normal air carryover is specified, the mass fraction of air in the vent entrance is related to its mass fraction in the dry well atmosphere.
- (b) Early Air Carryover. One extreme mode for the air carryover results from the assumption that the fluid entering the containment dry well acts somewhat like a piston and forces all of the air out of this chamber through the vents into the wet well before any of the steam-water mixture is allowed to flow into the vent. After all of the air is expelled from the dry well, the mass fraction of water and steam in the vent entrance is calculated as described elsewhere [80]. When the air is forced into the wet well early in the blowdown it acts to increase the pressure in this chamber early in the transient when the mass flow and energy input to the containment are greatest. As illustrated in Table XVIII, this

TABLE XVIII

EFFECT OF AIR REMOVAL ON PEAK PRESSURE

Conditions: PCO = 1.0

No heat transfer

 $fL/D = 4.42$

<u>Modes of Air Control</u>	<u>Peak Dry Well Pressure (psia)</u>	
	<u>Bodega Bay Test 17</u>	<u>Bodega Bay Test 20</u>
Air removed early	68.3	90.4
All air retained in dry well	51.7	81.7
Normal air carryover	64.1	84.9
Test	51.7	66.7

increased pressure during the portion of the transient for which the decompression is occurring at a high rate leads to higher dry well pressures than in the case of normal air carryover.

- (c) Air Retention. The third mode of air behavior studied was that of retention of all the air in the dry well throughout the blowdown transient. Although this mode of air behavior is not considered reasonable in single compartment dry wells, it would be an important consideration for more complex systems. Since no air enters the wet well, the back pressure from this chamber will be relatively small; peak dry well pressures predicted by using this method of air control were found to be less than those predicted by using normal air carryover, as illustrated in Table XVIII.

4.25 Heat Transfer to Dry Well. Heat loss through the walls of the dry well reduces the peak pressure in this chamber during a blowdown transient. The time from blowdown initiation to peak pressure is too short for significant heat losses to the atmosphere. The heat lost during this portion of the transient is used in heating the walls and internal structure of the containment. Three heat transfer correlations were investigated: (a) the Tagami heat transfer correlation[95], (b) the Uchida heat transfer correlation[96], and (c) a constant heat transfer coefficient. Results of these studies are collected in Table XIX for Bodega Bay Tests 17 and 20.

TABLE XIX

PEAK DRY-WELL PRESSURES AND TEMPERATURES AS PREDICTED
WITH CONTEMPT-PS FOR VARIOUS ASSUMED HEAT TRANSFER COEFFICIENTS^[a]

Heat Transfer Correlation	Bodega Bay Test 17		Bodega Bay Test 20	
	Peak Dry Well Pressure (psia)	Peak Dry Well Temperature (°F)	Peak Dry Well Pressure (psia)	Peak Dry Well Temperature (°F)
$h = 0^{[b]}$	68.2	301.2	90.4	320.6
$h = 1,000^{[b]}$	64.8	297.8	83.5	315.0
$h = 10,000^{[b]}$	54.1	286.0	81.2	313.1
$h = 100,000^{[b]}$	54.5	286.5	81.2	313.1
Uchida	66.1	299.1	87.3	318.1
Test	51.7	252	66.7	239

[a] These calculations were made on the basis of maximum water carryover (PCO = 1.0), early air removal from the dry well, large fL/D, and no heat transfer from the dry well walls to the external atmosphere.

[b] Btu/hr-ft²-°F.

- (a) Tagami Heat Transfer Coefficient. The Tagami heat transfer correlation was developed for use in a dry containment in which all of the air and a substantial amount of the energy is retained in the containment^[95]. This situation is substantially different from a pressure suppression type of containment subsequent to the clearing of the vents. A maximum heat transfer coefficient of about 2300 Btu/hr-ft²-°F was found for Humboldt Bay Test 15. The maximum heat transfer coefficient reported in Tagami's data is less than one-third this value. Because of the fundamental difference between the pressure suppression type of containment and the dry containment, the Tagami correlation is not applicable to the pressure suppression type containments.
- (b) Uchida Heat Transfer Coefficient. The Uchida correlation was derived from data for the Japan Power Demonstration Reactor^[96]. The data are tabulated in Reference 85. This correlation relates the heat transfer coefficient to the ratio of steam to air in the dry well atmosphere and has been included as an integral part of the CONTEMPT-PS computer code. The Uchida correlation is valuable in that the air concentration varies in the dry well atmosphere according to the amount of steam and water that have been injected into the dry well and the mode of air control. The results obtained by using the Uchida heat transfer coefficient are given in Table XIX.

- (c) Constant Heat Transfer Coefficient. In the absence of definitive data for heat transfer in pressure suppression type containments, constant heat transfer coefficients were selected which were thought to provide upper and lower bounds of expected conditions during a loss-of-coolant accident. Bodega Bay Tests 17 and 20 were selected for this aspect of the investigation because more complete experimental data were available for comparison with the calculations.

Since heat is transferred to the wall of the dry well, the heat transfer coefficient is greater than zero and $h = 0$ Btu/hr-ft²-°F represents a lower bound; a value of $h = 10,000$ Btu/hr-ft²-°F represents an upper bound.

The effect of heat loss is seen by comparing the peak dry well pressure for different values of heat transfer coefficient for one of the tests, for example, Bodega Bay Test 20. The correlations would be similar for Bodega Bay Test 17. These two tests differ in that Bodega Bay Test 17 represents a 100% of design basis accident (DBA) break whereas Bodega Bay Test 20 was 200% of DBA break. For Bodega Bay Test 20, the peak dry well pressure for $h = 10,000$ Btu/hr-ft²-°F is about 90% of that for $h = 0$ Btu/hr-ft²-°F which indicated considerable energy loss by heat transfer from the atmosphere region of this vessel. Because of the shorter blowdown time in Bodega Bay Test 20 and the time dependent nature of heat conduction, relatively less heat was transferred to the dry well walls for Test 20 than for Test 17. For example, when $h = 10,000$ Btu/hr-ft²-°F in Bodega Bay Test 17 the peak dry well pressure was 78% of that for the case where $h = 0$ Btu/hr-ft²-°F. The Uchida heat transfer correlation yields a peak dry well pressure that is about 97% of that for $h = 0$ Btu/hr-ft²-°F, indicating substantially less energy loss due to heat transfer as compared with the larger value of $h = 10,000$ Btu/hr-ft²-°F. The Uchida case gives a more nearly accurate estimate of the heat loss than the case for $h = 10,000$ Btu/hr-ft²-°F. The calculated peak dry well pressure for $h = 0$ Btu/hr-ft²-°F is about 130 to 135% of the test pressure for both tests.

Table XIX shows that the dry well pressure for the case in which $h = 10,000$ Btu/hr-ft²-°F is the same as for the case in which $h = 100,000$ Btu/hr-ft²-°F. Thus, these results reflect the maximum effect of heat transfer on calculated dry well pressures for Test 20. The amount of heat transferred through the wall is dependent on time, thermal conductivity, and the temperature gradient in the wall; thus the amount of heat transferred is limited. For very large values of heat transfer coefficient the maximum temperature and pressure attained in the dry well become independent of this parameter and depend only on the rate of mass and energy influx from the simulated reactor vessel, the rate of mass and energy efflux to the wet well, and the thermal properties of the containment walls.

Much of the overprediction of peak dry well pressure is caused by the assumption that the air is forced out of the dry well before any steam-water fluid is allowed to flow in the vent. The Bodega Bay dry well was relatively larger than the Humboldt Bay dry well although the wet wells in the two test facilities were of comparable volume. The requirement that the air be transferred from the dry well to the wet well early in the transient leads to high

wet well pressures and consequently large back pressures. In addition, the fluid in the vents would be expected to be wetter (more dense) in the absence of air which would lead to larger pressure drops in this system.

4.26 Maximum Dry Well Temperature. The fluid entering the dry well from the simulated reactor is flashed to steam and water which are then intimately mixed with the fluid in the dry well. The steam and water in the dry well atmosphere are at the same temperature, this temperature being the saturation temperature of the water.

When heat transfer to the dry well is not included in the calculations, the maximum temperature of the dry well atmosphere is higher, as noted in Table XIX, than when heat transfer is allowed. The maximum temperature occurs at about the same time as the peak pressure. A comparison of the calculated and test data for the Bodega Bay test series (shown in Table XVII) indicates that in every case the predicted temperature in the dry well and the wet well are greater than the experimental temperature. The temperature calculations were made with no heat loss to the containment walls, and the lack of heat loss would account for some of the differences between the calculated and test pressure data. Temperature transducer placement and inaccuracy may also account for part of the differences.

Temperature data were not reported for the Humboldt Bay test series. For this reason predicted temperatures were omitted from Table XVI.

4.27 Vent Clearing Time. Vent clearing calculations were made for Humboldt Bay Tests 5, 12, 15, 16, 17, and 33. These tests cover a wide range of blowdown rates and two submergence depths. The blowdown rates used in the calculations are the same as those used for the peak dry well pressure data shown in Table XVI.

The results of this study are shown in Table XX. The time to clear the water from the vents is seen to decrease with increasing blowdown rate (break diameter). The vent clearing time is longer for the longer vent as would be expected. The calculated vent clearing time is longer than the experimental vent clearing time in all but Test 33. The reason for the longer vent clearing time during this test is unknown at this time.

The time step size was varied from 20 milliseconds to one millisecond to find the effect of time step size on the time of vent clearing. These data were obtained for Humboldt Bay Test 5 as the basis for comparison; this case was chosen because of the shorter computer time involved. The results of this aspect of the study are tabulated in Table XXI and shown graphically in Figure 119. The results of hand calculations for Bodega Bay Test 20 are included in Figure 119 for comparison. The two curves are nearly parallel over most of the region covered by Humboldt Bay Test 5. The calculated vent clearing time changes very little for time step sizes smaller than about one or two milliseconds and appears to approach a minimum value.

A time step of 0.001 or 0.002 second is recommended for calculating the vent clearing transient. Smaller time steps are not recommended because of the increase in computer time required for the calculation.

TABLE XX

VENT CLEARING TIME FOR SELECTED HUMBOLDT BAY TESTS AS A FUNCTION OF BREAK
DIAMETER AND DEPTH OF SUBMERGENCE

Test	Break Diameter (in.)	Submergence Depth (ft)	<u>Vent Clearing Time (sec)</u>		<u>Dry Well Pressure at Time of Vent Clearing (psia)</u>	
			Test	CONTEMPT-PS	Test	CONTEMPT-PS
12	1.64	12.5	0.50	0.563	49	50.6
15 [a]	1.64	6.0	0.30	0.301	45	41.0
16 [a]	2.32	6.0	0.26	0.273	58	56.5
17 [a]	2.84	6.0	0.25	0.242	73	62.3
33 [a]	3.24	6.0	0.24	0.224	87	73.0
5	1.64	6.0	0.37	0.375	[b]	36.2

[a] Test data from Reference 97.

[b] Dry well test pressure not available.

TABLE XXI

RESULTS OF VARYING TIME STEP SIZE FOR CALCULATIONS
OF VENT CLEARING TRANSIENT

Time Step size (sec)	Vent Clearing time	Pressure at time of vent clearing (psia)
0.020	0.3748	36.2
0.010	0.3521	36.0
0.002	0.3459	35.2
0.001	0.3451	35.2

FIG. 119 VENT CLEARING TIME AS A FUNCTION OF TIME STEP SIZE.

4.3 Conclusions

The principal conclusions drawn from the study are:

- (1) The present version of CONTEMPT-PS will predict containment pressures in excess of most of the test data from either the Humboldt Bay or Bodega Bay test series when the conditions of no heat transfer, early air removal, large fL/D , and maximum water carryover are specified.
- (2) In studies of the decrease in peak dry well pressure due to heat loss the Uchida heat transfer correlation predicted, for the cases investigated, a peak dry well pressure that was about 95% of that for no heat transfer. A series of constant heat transfer coefficients was used to bound the conditions expected to occur during a loss-of-coolant accident. The peak dry well pressures were found to be less than those for the Uchida case for all heat transfer coefficients used except $h = 0 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$.
- (3) The Tagami heat transfer coefficient was found inapplicable to pressure suppression type containments.
- (4) The peak dry well pressure increases with increasing fL/D and the pressure increase may be approximated by $\Delta P = K(fL/D)^{0.25}$. As the vent resistance increases, the amount of fluid entering the wet well decreases with a consequent increase in pressure in the dry well.
- (5) A value of $PCO = 1.0$ was found to give higher peak pressures in the dry well and higher water carryover than values less than 1.0, mainly because the fluid density in the vent system increases with consequent larger pressure drops between the dry well and the wet well.
- (6) Early air carryover yields higher peak dry well pressures than normal air behavior or the retention of all the air in the dry well. The increase in pressure in the dry well is related to the higher pressure in the wet well when early air carryover is specified.
- (7) Time steps of 1 or 2 milliseconds are recommended for calculating the vent clearing transient. Longer time steps tend to give higher dry well pressures and shorter time steps require excessive computer time.

4.4 Recommendations

The CONTEMPT-PS computer code is recommended for use in predicting peak dry well pressures in large power reactors. Large values of fL/D should be used for calculating the pressure in the containment until additional data, especially data on two phase flow, become available. A value of $PCO = 1.0$ is recommended for use in general calculations of peak dry well pressure and water carryover. This value of PCO leads to high peak dry well pressures and maximum water carryover predictions. Calculations based on early air removal from the dry well should be used in predicting the peak dry well pressure.

VII. SEPARATE EFFECTS TESTING

(J. O. Zane)

Safety evaluations for large power reactors require an in-depth understanding of the hydraulic, thermodynamic, and mechanical phenomena occurring as a result of decompression in the primary system following a loss-of-coolant accident (LOCA). This understanding must encompass the thermal and hydraulic processes associated with reflooding the core as the plant engineered safety systems are actuated to mitigate the consequences of the accident. Because of the coupling between the various phenomena, the required understanding can best be obtained from calculational results derived from comprehensive analytical models which have been tested against data from experiments. The purpose of the separate effects testing program is to provide the experimental data needed for developing and assessing the comprehensive analytical models. Existing and planned experiments cover the time in the loss-of-coolant accident sequence from initiation of a break in the primary system until the emergency core cooling (ECC) systems have reflooded the core. Included within the separate effects testing program are the Semiscale Blowdown and Emergency Core Cooling (ECC) Project, the Full-Length Emergency Cooling Heat Transfer (FLECHT) Test Projects, and the Scoping Emergency Cooling Heat Transfer (SECHT) Project. The SECHT Project has been completed and the results extensively documented. The basic testing in the FLECHT Projects is complete and documentation is in progress.

1. SEMISCALE BLOWDOWN AND EMERGENCY CORE COOLING (ECC) PROJECT

(R. F. Farman, H. W. Heiselmann, J. F. Whitbeck)

The Semiscale Blowdown and Emergency Core Cooling (ECC) Project^[26] is part of the Water Reactor Safety Program of the U. S. Atomic Energy Commission. The experiments in this project are conducted by establishing, in small systems, operating conditions similar to those expected in a power reactor and then initiating a simulated primary piping break of controlled size and location. The phenomena occurring in the system after the break are observed and recorded for subsequent analysis.

The substantial and significant differences between a large nuclear reactor and the semiscale apparatus, and the differences in the related phenomenologies during postulated loss-of-coolant accidents, make invalid any direct extrapolation of test results to a reactor.

A primary objective of the semiscale project is to provide an experimental data base to help assess the capability and adequacy of analytical models which are used for quantifying thermal-hydraulic phenomena which could occur in a water-cooled power reactor system during a hypothetical LOCA. Information is needed which will assist in developing and assessing an analytical representation of the following:

- (1) System decompression characteristics and mass efflux
- (2) The availability of primary coolant to the core during blow-down

- (3) The heat transfer mechanisms controlling core thermal response during system decompression
- (4) The requirements for ECC delivery to the core and the importance of various system and break parameters affecting those requirements
- (5) The effect of ECC injection on primary system decompression
- (6) The forces generated during blowdown and the mechanical response of the system components to those forces.

The semiscale tests also provide data to assist in meeting the overall objectives of the Loss-of-Fluid Test (LOFT) Program.

The approach employed in this project is to quantify individual phenomena in simple geometries with break size and location as independent variables. The coupling among the various phenomena controlling hydraulic behavior during decompression is then investigated by subsequent tests in systems of increasing complexity. The semiscale tests have been divided into a number of test series, as shown in Table XXII. The initial semiscale experiments (500, 600, and 700 tests) investigated coolant depressurization in small pressure vessels with and without the use of unheated simulated reactor internals. Results from these tests have been reported[98].

The most recent semiscale experiments (800 tests) were performed in a system which consisted of a vessel to which had been added a single primary loop. These single-loop semiscale tests have been subdivided into three categories. In the first category, the effect of adding a loop to a vessel without internals was investigated; the results of these tests have also been reported[99]. In the second category, the effects on blowdown behavior of adding a simulated reactor core and core heat were investigated. Results for the unheated core tests in this category, performed during 1969, have been published[100, 101]. Heated-core tests of this category were completed in 1970 and selected results are presented in this report. In the third category, the effect of ECC injection on fluid decompression and on system and component mechanical response is being investigated. Selected results from tests in this category performed in 1970 are included in this report.

The single-loop test system that had a 9-inch core is being replaced with a test system that is to provide a small-scale model of the Loss-of-Fluid Test (LOFT) system[1]. The new system, the 1-1/2-loop semiscale system, is to have a 66-inch core, a passive second loop, and loop volumes and pressure drops corresponding to those of the LOFT system. Activities of 1970 in support of 1-1/2-loop semiscale are included in this section.

Preparations are being made for advanced semiscale tests to be conducted in a two-loop system, modeled to represent more nearly the significant hydraulic and mechanical phenomena which occur during an LOCA and subsequent ECC injection in a current generation PWR. Although the two-loop semiscale system concept was subsequently deferred and emphasis was placed on the 1-1/2-loop system the system design and test program planning efforts during 1970 for the two-loop semiscale system are reported in this section.

TABLE XXII

SEMISCALE BLOWDOWN AND EMERGENCY CORE COOLING (ECC) PROJECT

	<u>Test Numbers</u>	<u>Schedule (Fiscal Year and Quarter)</u>	<u>Experimental System</u>	<u>Scope of Project</u>
Vessel-Only Semiscale	500 Tests 600 Tests 700 Tests	FY-66-1, 2, 3, 4 FY-67-1, 2 FY-67-3, 4 FY-68-1, 2, 3, 4		Blowdown tests with and without unheated internal configurations. About 100 tests.
Single-Loop Semiscale	800 Tests Empty Vessel (Tests 801 through 820-1) Core and Core Heat (Tests 821 through 826-1) ECC Injection (Tests 845 through 851)	FY-69-1, 2, 3, 4 FY-70-1, 2, 3, 4 FY-71-1 FY-71-2, 3		Blowdown tests with the semiscale system with and without electrical core heat and ECC injection.
1-1/2 Loop Semiscale	900 Tests	FY-72-2, 3, 4		Blowdown tests with the semiscale system with electrical core heat and ECC injection. About 10 tests.
Two-Loop Semiscale	1000 Tests	FY-74-4 FY-75		Blowdown tests with system having two loops for ECC injection and electrical core heat. About 25 tests.

1.1 Single-Loop Semiscale

During 1970 single-loop semiscale testing with a heated core was completed and tests with a heated core and ECC injection were initiated. Table XXIII presents a list of tests performed during the year along with the test conditions and a brief statement of the purpose of each test. The objective of these tests was to obtain information on:

- (1) The effects of core power and the associated enthalpy difference across the core on blowdown behavior
- (2) Departure from nucleate boiling (DNB) characteristics during blowdown
- (3) The effects of pump operation on blowdown behavior
- (4) The effects of ECC injection on blowdown behavior.

1.11 Tests Involving Core Heat. Semiscale tests involving core heat were performed to investigate the effects of the enthalpy difference between the inlet and outlet plenum fluids on system decompression, particularly on the pressure differences between the vessel inlet and outlet plenums. The core mass flow during the subcooled blowdown and the initial portion of saturated blowdown was also investigated.

Typically for the heated core semiscale tests the temperature difference across the core was on the order of 40°F, the inlet temperature was about 560°F, and the system pressure was about 2300 psig. If the fluids in the inlet and outlet plenums both simultaneously followed isentropic decompression to the respective plenum saturation temperature, the resulting pressure difference across the core should be about 390 psi. The duration of simultaneous isentropic decompression to the saturation temperature in the plenums is primarily dependent on the sonic velocity in the core region, the length of the core, and the break size and location. The single-loop semiscale system, which has only one loop and a short core (9-inch heated length) cannot provide data representative of a PWR. The semiscale system can, however, provide a basis for evaluating the potential for significant pressure differences across the cores in large systems.

In these tests, the pressures and fluid temperatures were measured in the vessel inlet and outlet plenums during the blowdown transient. For some tests the pressure differential across the core was measured directly. The vessel pressures and temperatures for the subcooled blowdown and the initial portion of the saturated blowdown obtained from tests in which the system was ruptured at the vessel hot leg (outlet) nozzle with a break area to system volume ratio of 0.007 ft⁻¹ (Tests 824 and 825) are shown in Figures 120 through 123.

TABLE XXIII

SEMISCALE BLOWDOWN TESTS PERFORMED DURING 1970

Test	Break		Initial Conditions					ECC Injection		Purpose
	Location	Size ^[a] (%)	Pressure (psig)	Flow (gpm)	Core ΔT (°F)	Inlet Temperature (°F)	Power (MW)	Rate ^[b] (gpm)	Location	
824	Outlet Nozzle	10	2250	---	50	590	1.1	---	---	Outlet blowdown heated core test; establish effects of core ΔT .
825	Outlet Nozzle	10	2270	222	40	560	1.1	---	---	Outlet blowdown heated core test; establish effects of core ΔT .
826	Inlet Nozzle	30	2280	229	30	572	0.9	---	---	Inlet blowdown heated core test; establish effects of core ΔT .
826-1	Inlet Nozzle	30	2160	224	31	565	1.0	---	---	Inlet blowdown heated core test; establish effects of core ΔT .
845	Cold Leg	30	2180	238	32	558	1.1	270	Inlet Plenum	Determine effects of ECC injection; high ECC flooding rate.
846	Cold Leg	10	2290	220	38	555	1.1	30	Inlet Plenum	Determine effects of ECC injection; low ECC flooding rate.
847	Cold Leg	10	2290	225	37	556	1.1	30	Inlet Plenum	Determine effects of increased system pressure drop.

[a] The break area to system volume ratio for a 10% break is 0.0007 ft^{-1} and for a 30% break it is 0.0021 ft^{-1} .

[b] ECC injection rate based on design cold flooding rate for 150 psi pressure difference between ECC accumulator and vessel inlet plenum.

FIG. 120 VESSEL PLENUM TEMPERATURES DURING THE INITIAL PORTION OF SATURATED BLOWDOWN -- TEST 824.

FIG. 121 VESSEL PLENUM TEMPERATURES DURING THE INITIAL PORTION OF SATURATED BLOWDOWN -- TEST 825.

FIG. 122 VESSEL PLENUM PRESSURES DURING THE INITIAL PORTION OF SATURATED BLOW-DOWN -- TEST 824.

FIG. 123 VESSEL PLENUM PRESSURES DURING THE INITIAL PORTION OF SATURATED BLOW-DOWN -- TEST 825.

Figures 120 and 121 show that the temperature of the fluid in the outlet plenum drops sharply immediately after rupture (due to subcooled expansion of the fluid) and then decreases at a relatively constant rate until the temperatures in the two plenums are about the same. The temperature of the fluid in the inlet plenum remains relatively constant over this period. The temperature data shown in Figures 120 and 121 when used in conjunction with the pressure data presented in Figures 122 and 123 indicate that the fluid in the outlet plenum reaches saturation conditions within 0.2 sec after rupture whereas the fluid in the inlet plenum remains subcooled for 2.3 sec (Test 824) and 3.2 sec (Test 825) after rupture. The data presented in Figures 122 and 123 and also in Figures 124 and 125 (for the first 80 msec of blowdown) show that at no time during the subcooled blowdown and the initial portion of the saturated

FIG. 124 VESSEL PRESSURES AND PRESSURE DIFFERENCE DURING SUBCOOLED DECOMPRESSION -- TEST 824.

FIG. 125 VESSEL PRESSURES AND PRESSURE DIFFERENCE DURING SUBCOOLED DECOMPRESSION -- TEST 825.

blowdown does the pressure difference across the core reach the 390 psi representative of simultaneous isentropic decompression of the inlet and outlet plenums. Further, the data show that, for Test 824, the core pressure difference does not exceed 75 psi until saturation conditions are reached in the inlet plenum.

The entire pressure and temperature histories for Tests 824 and 825 are shown in Figures 126 and 127; the maximum recorded pressure difference across the core for both tests was about 145 psi (3.5 sec after rupture in Test 824).

The conclusion is that fluids in the vessel inlet and outlet plenums did not simultaneously decompress to the saturation pressures corresponding to the individual plenum temperature following a hot leg break in the semiscale system. Rather, the fluid in the system decompressed to a pressure corresponding to the saturation temperature in the outlet plenum and hot leg. The fluid in the inlet plenum and cold leg remained subcooled until the system decompressed to a pressure corresponding to cold leg saturation conditions.

Comparable data on the core pressure difference were obtained from cold leg break blowdown tests in which the system was ruptured near the vessel inlet nozzle (below the core elevation) with a break area to system volume ratio of 0.0021 ft⁻¹. The vessel fluid temperatures and pressures as function of time for an inlet break test (Test 826-1) are shown in Figures 128 through 130.

FIG. 126 VESSEL PRESSURES AND FLUID TEMPERATURES -- TEST 824.

FIG. 127 VESSEL PRESSURES AND FLUID TEMPERATURES -- TEST 825.

FIG. 128 VESSEL PLENUM FLUID TEMPERATURES -- TEST 826-1.

FIG. 129 VESSEL PRESSURES DURING THE INITIAL PORTION OF SATURATED BLOWDOWN -- TEST 826-1.

FIG. 130 VESSEL PRESSURES AND FLUID TEMPERATURES -- TEST 826-1.

Examination of Figures 128 and 129 shows that, unlike the case for the hot leg break, the temperature of the fluid in the inlet plenum does not remain subcooled until the system has decompressed to saturation conditions corresponding to the inlet plenum temperature. The temperature of the fluid in the outlet plenum begins to drop immediately, indicating saturation conditions exist in the outlet plenum. The temperature of the fluid in the inlet plenum remains relatively constant for approximately 0.1 sec, then begins to increase and becomes essentially equal to the outlet plenum saturation temperature within 0.2 to 0.3 sec after rupture. The inlet plenum fluid temperature rises because the fluid flow through the core reverses at rupture and the fluid from the outlet plenum flows down through the core and into the inlet plenum. Figure 129 shows the negative pressure difference between the inlet and outlet plenums which induces the reverse flow. The pressure difference across the core attains a maximum of 150 psi at 0.04 sec after rupture and then decreases to 50 psi at 0.8 sec after rupture. The magnitude of the core differential pressure during subcooled blowdown is significantly greater for Test 826-1 than for Tests 824 and 825 primarily because the break area was three times greater in Test 826-1.

Figures 131 and 132 show the measured pressure differential between the vessel inlet and outlet nozzles and the core fluid flow rate for the first second after rupture in Test 826-1. The pressure difference between the vessel inlet and outlet nozzles was measured with a differential pressure transducer and water-filled lines connecting the transducer to the inlet and outlet nozzles. Because of the attenuation of the pressure wave in the water-filled connecting lines, the magnitude of the pressure difference measured by the differential pressure transducer is less than the pressure difference between the inlet and outlet plenum indicated in Figure 129. Agreement between the two pressure difference measurements, however, is within about 15% after the first 0.1 sec following rupture.

FIG. 131 PRESSURE DIFFERENTIAL BETWEEN VESSEL NOZZLES -- TEST 826-1.

FIG. 132 COMPARISON OF CALCULATED AND MEASURED CORE FLOW RATES -- TEST 826-1.

The flow rate indicated on Figure 132 was obtained through use of a drag disc flow meter and a fluid density detector, both located in the inlet plenum. These detectors were sufficiently close to the lower grid plate to permit the assumption that the measured inlet plenum flow rate is representative of the core flow rate. The core flow rate calculated from the pressure difference between the inlet and outlet plenums and the measured inlet plenum flow rate is also shown in Figure 132 for comparison.

1.12 Departure from Nucleate Boiling and Heat Transfer Coefficients During Blowdown. During the single-loop semiscale tests, data were obtained that provided some experimental insight into the time to departure from nucleate boiling (DNB) and the heat transfer coefficients that exist in the semiscale core during blowdown. Although the 9-inch electrically heated semiscale core^[102] is not representative of a PWR core, useful base-line information was obtained.

The capability of simulating decay power did not exist in the single-loop semiscale system. Core power could either be turned off at time of rupture or continued after rupture until the maximum permissible cladding temperatures (about 900°F) were reached.

A comparison of the results of Test 824 (power off at rupture) and Test 825 (core power continued 14 sec after rupture) shows the effect of core heat addition during the blowdown transient.

Typical pin cladding temperatures for Test 824 are shown in Figure 133. The temperature of the cladding drops to approximately the fluid saturation temperature within 2 to 3 sec after rupture and for the most part follows

FIG. 133 TYPICAL PIN CLADDING TEMPERATURES -- CENTER PIN, TEST 824.

fluid saturation temperatures during the major portion of the blowdown. An exception is the temperature two inches below the top of the heated length which indicates that the upper portion of the core does not cool as rapidly as the lower portion.

Typical pin cladding temperatures for Test 825 in which the full 1.1-MW core power was maintained until the cladding temperature reached approximately 900°F are presented in Figure 134. These cladding temperatures show that, although full core power was maintained after system rupture, DNB did not occur until 12.5 sec into the blowdown and then only toward the top of the heated length. Prior to DNB the pin cladding temperatures generally follow the fluid saturation temperatures. The pin pitch and the pin diameter for the semiscale core results in a volume-to-heated-surface-area ratio that is on the order of three times that in a typical PWR core. The portionately larger volume of liquid in the core is one possible explanation for the long time before the occurrence of DNB. Once DNB occurred the rate of temperature rise was rapid (about 160°F/sec) until the core power was terminated 14 sec into the blowdown (Figure 134). Wetting of the pin cladding surface was reestablished rapidly; by 16 sec after rupture the pin cladding temperature was again at the fluid saturation temperature.

Pin surface temperatures and heat transfer coefficients calculated from the pin surface temperatures and fluid conditions are presented in Figures 135 and 136 for a typical pin. During steady state operation the value of the heat transfer coefficient for the heater pins varied from 3300 Btu/hr-ft²-°F at the bottom of the heated length to 6000 Btu/hr-ft²-°F at the top. The heat transfer coefficient increased rapidly at system rupture and reached a value in excess of 10,000 Btu/hr-ft²-°F within 0.1 sec after rupture. Heat transfer

FIG. 134 TYPICAL PIN CLADDING TEMPERATURES -- CENTER PIN, TEST 825.

coefficients at various locations in the core ranged from about 10,000 to 32,000 Btu/hr-ft²-°F for the first 12 sec of blowdown. At the time of DNB, the heat transfer coefficient for the locations where DNB occurred dropped sharply to a value between 5 and 10 Btu/hr-ft²-°F until core power was terminated and rewetting of the cladding surface was reestablished (about 16 sec after rupture).

1.13 Performance of Semiscale Circulating Pump During Blowdown. An understanding of the performance of the single-loop semiscale pump during decompression is important because pump coastdown characteristics affect fluid flow. The direction of the pressure drop across the pump depends on pump operation; the pump may act as a driving force or a restriction to flow. Pump effects are particularly important during saturated decompression and during periods of ECC injection and core reflooding.

Data relative to the effects of the semiscale circulating pump on the fluid behavior during the blowdown transient were obtained during the heated core and ECC tests. The pump speed and pressure drop were measured during the blowdown transient as were the fluid temperature, pressure, and density in the pump discharge (cold) leg.

The data presented in Figures 137 and 138 were taken from Semiscale Test 826-1 for which the system was ruptured at the vessel inlet nozzle (cold leg). The break area to system volume ratio was 0.0021 ft⁻¹. For Test 826-1 the pump was operated at the initial speed of 3550 rpm. The fluid flow rate in the semiscale system was controlled by throttling the flow with a butterfly valve located in the pump discharge leg. The pump head prior to rupture, at the initial flow of 224 gpm, was 62 psig. The major portion (about 58 psi) of the system

FIG. 135 CALCULATED SURFACE TEMPERATURE FOR TEST 825 -- PIN 116 (TM-116-T).

FIG. 136 HEAT TRANSFER COEFFICIENT FOR TEST 825 -- PIN 116 (TM-116-T).

pressure drop for the initial flow conditions occurred at the butterfly valve and the balance occurred in the loop piping and across system components. Electrical power to the pump motor was shut off 1.0 sec after rupture.

The pressure drop across the pump during the blowdown transient is shown in Figure 137. At rupture the pressure difference across the pump reversed momentarily from the 62 psi (discharge pressure greater than suction pressure) to 108 psi (suction pressure greater than discharge pressure). The reversal in pressure difference across the pump indicates that at rupture the fluid in the pump discharge leg decompressed at a rate faster than the pump could supply fluid and that the fluid in the pump suction leg momentarily drove the pump (that is, the pump was a restriction to flow). A positive pump pressure difference was quickly reestablished (in less than 0.05 sec) and the pump maintained a positive pressure head until 4.2 sec after rupture.

FIG. 137 PUMP PRESSURE DIFFERENTIAL AND COASTDOWN SPEED DURING BLOWDOWN -- TEST 826-1.

Power to the pump motor was terminated one second after rupture and, as shown in Figure 137, the pump speed began to decrease immediately. The pump speed decreased to 1200 rpm by 4.2 sec after rupture at which time the pressure difference across the pump effectively decreased to zero. The positive pressure difference across the pump for the time interval between approximately 0.05 sec after rupture until 4.2 sec after rupture shows that the pump was capable of pumping the fluid in the normal flow direction even though the pump speed was decreasing.

The cold leg fluid density, temperature, and pressure are presented in Figure 138. The cold leg fluid density decreased to less than 10 lb/ft³ by 4 sec after rupture. The data for Test 826-1 indicate that the pump was capable of maintaining a positive head and did not cavitate even though the speed had decreased to 1200 rpm and the fluid density on the discharge side (the cold leg) had decreased to less than 10 lb/ft³. However, for this semiscale test an atypical situation existed in that the major portion of the loop pressure drop occurred across the butterfly valve located between the pump discharge nozzle and the break. The butterfly valve produces a major restriction to flow that would not be present in a typical power reactor system.

The data presented in Figures 139 and 140 were taken from Semiscale Test 845. For this test the system was ruptured in the pump discharge leg (cold leg break) at an elevation about 10 feet from the pump discharge port. The break area to system volume ratio was 0.0021 ft⁻¹. The initial fluid flow rate for Test 845 was controlled at 238 gpm primarily by controlling the pump speed at about

FIG. 138 COLD LEG FLUID PROPERTIES -- TEST 826-1.

FIG. 139 PUMP PRESSURE DIFFERENTIAL AND COASTDOWN SPEED DURING BLOWDOWN -- TEST 845.

FIG. 140 FLUID PROPERTIES IN PUMP DISCHARGE LEG -- TEST 845.

1700 rpm. The total system pressure drop at the initial fluid conditions was 10 psi with the major portion of the drop occurring in the loop piping and across system components. Unlike Test 826-1 the butterfly valve produced a pressure drop of only about 5 psi.

Data presented in Figure 139 show that the pressure difference across the pump reversed immediately after rupture (suction pressure greater than discharge pressure) and did not again become positive (discharge pressure greater than suction pressure) for the entire blowdown transient. The power to the pump motor was terminated at 0.3 sec after rupture, and, as shown in Figure 139, the pump speed had decreased to 1200 rpm by 3.3 sec after rupture. The pump coastdown data presented in Figure 137 for Test 826-1 and in Figure 139 for Test 845 show that the time required for the pump to slow from 1700 to 1200 rpm is 1.65 sec for Test 826-1 and 2.9 sec for Test 845.

The fluid temperature, pressure, and density data for the pump discharge leg for Test 845 are presented in Figure 140. Comparison of the fluid properties in the pump discharge leg with those for Test 826-1 (Figure 138) shows that the density and pressure decrease faster for Test 845 than for Test 826-1.

The fluid mass flow rate through the pump discharge leg for Test 845 is presented as a function of time in Figure 141. At rupture the mass flow rate increased in the normal flow direction from 25 to 65 lb/sec and decreased to essentially zero at a nearly linear rate between 0.5 and 3.1 sec after rupture.

FIG. 141 MASS FLOW RATE FROM PUMP DISCHARGE LEG -- TEST 845.

The data presented in this section show a strong coupling between system hydraulic resistance and pump behavior. Figure 137 shows that a pump does not necessarily cavitate and become a resistance simply because fluid at the pump suction reaches saturation conditions.

1.14 Semiscale ECC Injection Tests. A simulated emergency core cooling system was added to the single-loop semiscale system and a series of decompression tests involving both an electrically heated core and accumulator ECC injection was initiated. Three of these tests were conducted during 1970. The primary purpose of the semiscale ECC injection experiments was to investigate the rate of energy exchange between the cool (150°F) ECC fluid and the hot saturated fluid mixture in the system during the later stages of system decompression. System pressure behavior and fluid mass flow rates in the core were of particular interest.

The results of the semiscale ECC injection tests cannot be directly applied to safety evaluations of reactor systems because of the dissimilarity between the system configurations of semiscale and a PWR. The semiscale results can, however, be used in evaluating the capability of analytical codes to describe the thermal-hydraulic behavior when relatively cold water is injected into a system undergoing decompression.

The semiscale accumulator ECC system was designed to contain a volume of liquid (2.6 ft³) that would flood the semiscale vessel to a level sufficient to cover the electrically heated core. Pressurized nitrogen provided the driving force for ECC injection. The ECC injection tests were performed with the ECC liquid at about 150°F and with the system set to inject ECC at about 660 psig. The semiscale accumulator ECC system was connected to the vessel as shown in Figure 142. For the first ECC injection test (Test 845), the ECC

FIG. 142 SEMISCALE ACCUMULATOR ECC SYSTEM.

was injected into the annulus between the vessel wall and core flow skirt; for subsequent tests, ECC was injected directly into the inlet plenum. The semiscale tests involving ECC injection performed during 1970 have been listed in Table XXIII. ECC tests were performed with break area to system volume ratios of 0.0021 ft^{-1} (Test 845) and 0.0007^{-1} (Tests 846 and 847) with the simulated break in the cold leg at an elevation above the core.

Figures 143 through 146 present results for Test 845 which was performed from the initial conditions of 2180-psig system pressure, 1.1-MW core power, 238-gpm primary fluid flow rate, and temperatures of 590 and 558°F for the hot and cold legs, respectively. The ECC accumulator liquid was at 660 psig

FIG. 143 INLET PLENUM PRESSURE AND PRESSURE DIFFERENCE BETWEEN ECC ACCUMULATOR AND VESSEL INLET PLENUM -- TEST 845.

FIG. 144 ECC FLOW RATE -- TEST 845.

FIG. 145 FLUID DENSITY DURING TEST 845.

and 144°F and was injected into the inlet plenum annulus between the flow skirt and the vessel inside diameter. The design cold flooding rate was 11 in./sec (270 gpm) at 150-psi pressure difference between the accumulator and inlet plenum.

The test results for vessel inlet plenum pressure and the pressure difference between the vessel inlet plenum and ECC accumulator are shown in Figure 143 and the ECC flow rate is shown in Figure 144. ECC flow started at 3.2 sec when the system pressure had decreased to 600 psig and continued at an average rate of about 160 gpm for 8 sec. All of the ECC had been injected by 11.2 sec after rupture.

FIG. 146 TYPICAL PIN CLADDING TEMPERATURES -- TEST 845.

The average densities measured in the inlet nozzle and the inlet plenum (at the inlet nozzle elevation) are shown in Figure 145. The data show that the injection of the ECC did not cause the average density of the fluid in the inlet plenum at the inlet nozzle elevation to increase as would be expected if the vessel inlet plenum were being flooded by the ECC liquid. Rather, the ECC flowed out the inlet nozzle, as indicated by the increase in the average fluid density in the inlet nozzle, and subsequently out the break.

The response of the heater pin cladding temperatures to the ECC injection is shown in Figure 146. All pin cladding temperatures shown are in excess of 600°F from 8 to 12 sec, a portion of the blowdown time interval during the latter phase of ECC injection. If the ECC liquid had reached the bottom portion of the core, the pin cladding temperatures would have reflected a drop in temperature. Generally, the pin cladding temperatures follow the primary system fluid saturation temperature until localized dryout in the area of the thermocouple occurs (5 to 8 sec). For example, Figure 146 shows dryout at about 7 sec for Pins 50 and 61.

The heater pin cladding temperature for an outside pin (Pin 116) at a point two inches below the top of the heated length indicates that DNB first occurred 2.7 sec after rupture. Subsequently the pin cladding temperature rose at a rate of 115°F/sec for about 1.1 sec, then decreased to essentially the fluid saturation temperature. DNB again occurred at the same location

at 4.8 sec with the pin cladding temperature subsequently increasing at about 135°F/sec until core power was shut off (8.3 sec). The cladding temperature for Pin 116 did decrease between 3.8 to 4.4 sec (ECC injection was initiated at 3.2 seconds). On the basis of the temperature and fluid flow (not presented) data, reverse flow through the core from the outlet plenum increased sharply for about 0.5 sec after the start of ECC injection. Apparently this reverse flow through the core caused the drop in cladding temperature for Pin 116 between 3.8 and 4.4 sec.

Similar results for Test 847 are shown in Figures 147 through 151. This test had a break area to system volume ratio of 0.0007 ft^{-1} and an ECC design cold flooding rate of 1.25 in./sec (30 gpm) at a pressure difference of 150 psi between the ECC accumulator and the vessel inlet plenum. For this test ECC was injected directly into the inlet plenum at the vessel inlet nozzle elevation. In addition, the resistance (pressure drop) of the semiscale loop was increased by the addition of orifices in the hot, cold, and pump suction legs in order to study system pressure drops.

FIG. 147 INLET PLENUM PRESSURE AND PRESSURE DIFFERENCE BETWEEN ECC ACCUMULATOR AND VESSEL INLET PLENUM -- TEST 847.

FIG. 148 ECC FLOW RATE -- TEST 847.

FIG. 149 FLUID DENSITY DURING TEST 847.

Vessel inlet plenum pressures and the pressure difference between the ECC accumulator and the vessel inlet plenum are shown in Figure 147; the ECC flow rate is shown in Figure 148. ECC injection started 10 sec after rupture at an average rate of 40 gpm until 39 sec after rupture at which time all of the 2.6 ft³ of ECC had been injected into the inlet plenum.

The average densities of the fluids in the core, vessel inlet plenum, and vessel inlet nozzle are shown in Figure 149; and the vessel flooding rate based on the ECC injection flow rate is shown in Figure 150. As shown in Figure 149, the density of the fluid in the inlet nozzle decreased to about 1 lb/ft³ by 7.5 sec after rupture and began to increase at 10 sec after rupture when ECC injection was initiated. The increase in the fluid density in the inlet nozzle after the initiation of ECC injection indicates that some of the ECC liquid was entrained in the fluid blowdown stream and carried out the break. By 19 sec after rupture the density of the fluid in the inlet nozzle had risen to 8.5 lb/ft³ and remained

FIG. 150 VESSEL FLOODING RATE BASED ON ECC INJECTION FLOW RATE -- TEST 847.

FIG. 151 TYPICAL PIN CLADDING TEMPERATURES -- TEST 847.

relatively constant between 19 and 22.5 sec after rupture. At 22.5 sec after rupture the density of the fluid in the inlet plenum below the core, at the elevation of the nozzle center line, began to increase and subsequently increased rapidly between 28 to 32 sec after rupture until the fluid density was 55 lb/ft³ at 32 sec after rupture. On the basis of the inlet plenum fluid density data, the assumption can be made that the liquid level in the inlet plenum below the core reached the elevation of the inlet nozzle center line (the elevation of the inlet plenum fluid density detector) 32 sec after rupture. The difference between the volume of ECC liquid injected into the inlet plenum, based on the ECC injection flow rate, and the volume accounted for by the liquid level in the vessel (based on the inlet plenum fluid density detector) is 0.55 ft³. Therefore, the ECC carried out of the vessel (and subsequently through the break) up to 32 sec after rupture by entrainment in the blowdown stream is about 30% of the amount injected, or 21% of the total ECC accumulator inventory. On the basis of the loss of 0.55 ft³ of ECC due to entrainment and assuming no further loss during the balance of the duration of ECC injection, the liquid level would have risen to about the level of the lower grid plate at 39 sec after rupture. Therefore, apparently none of the ECC liquid reached the heated portion of the core. The typical pin cladding temperatures shown in Figure 151 for the period of 32 to 38 sec after rupture support this conclusion.

The cladding temperature data shown in Figure 151 indicate a sharp drop in temperature about 41 sec after rupture. The fluid density data for the vessel inlet plenum, as shown in Figure 149, indicate that the density of the fluid in the inlet plenum also begins to drop rapidly at 41 sec after rupture, and by 43 sec after rupture it is about 1.5 lb/ft³. Both decreases can be attributed to the introduction of the ECC accumulator gas (temperature of about 50°F) into the vessel inlet plenum after all of the ECC liquid has been injected. The volumetric flow rate of the ECC accumulator gas is on the order of four times the volumetric flow rate of the ECC liquid. When the large volume of ECC gas enters the inlet plenum at about 40 sec after rupture, the pressure in the vessel above the liquid increases and causes the liquid to be pushed from the inlet plenum up the external downcomer and out the break. The increase in pressure in the vessel inlet plenum, as shown in Figure 147, begins at 40.5 sec after rupture and lasts until about 48 sec after rupture. The sharp decrease in cladding temperature as shown at 40.5 sec after rupture in Figure 151 is caused by the cold ECC gas passing over the pin cladding. (The ECC accumulator gas temperature drops to approximately 50°F during ECC injection because of gas expansion.)

On the basis of the data presented, the conclusion can be drawn that some of the ECC accumulator liquid will be entrained in the blowdown stream and carried out the break. In addition, the ECC accumulator gas, if trapped in the vessel as was the case for Test 847, can contribute to the potential for driving the ECC out the break.

1.15 Activities in Support of Future Semiscale Tests. Work was done during the second half of 1970 on design, procurement, and fabrication of the necessary equipment to construct a new semiscale test system. The new system, the 1-1/2-loop semiscale system, is to include a 66-inch electrically heated core, a complete operating primary loop and a passive second loop, and a system geometry, loop volumes, and pressure drops which model the LOFT system. The primary emphasis has been placed on providing a vessel and core having the features in the core region, such as pin pitch, power density, axial power profile, and fluid

velocities, similar to those in a PWR. The vessel is to include an internal downcomer with high inlet ports and the provision for nozzles for a passive loop to be used for simulating pipe ruptures not occurring in an operating loop. The volume distribution within the vessel has been designed to be as representative as possible of LOFT within the constraints imposed by the facility, power supply, and circulating pump capabilities.

A core heated length equal to the length of the LOFT nuclear core (66-inches) was selected. The core is to consist of 32 electrically heated stainless steel-clad heater pins which are to have a 0.560-inch pitch and a 0.422-inch diameter. The heater pins are to be placed in a 6 x 6 square array with the corner pins removed. The central 16 high-power pins require 65.4 kW at full power. The high-power pins are to have a chopped cosine axial power profile peaked at 19.6 kW/ft. A sketch of the vessel with the 66-inch core is shown in Figure 152.

The steam generator and pressurizer are to be located at an elevation that is above the vessel outlet nozzle and provisions are to be made for injecting ECC into any combination of the inlet and outlet nozzles on either the operating loop or passive loop piping. Provision is also to be made in the piping to accommodate simulated ruptures both in the operating and passive loop piping. The capability will exist for using either nozzles or orifices for controlling break size. The 1-1/2-loop semiscale system is shown in Figure 153.

1.2 Two-Loop Semiscale -- System Design^[a]

The specific objectives of the two-loop semiscale tests are:

- (1) To provide experimental data for developing and assessing the analytical models used in predicting the thermal, hydraulic, and mechanical consequences of an LOCA and subsequent ECC injection.
- (2) To improve understanding of the transient thermal, hydraulic, and mechanical behavior which occurs during the blowdown resulting from an LOCA and during the subsequent ECC injection.
- (3) To provide support for the LOFT Program by evaluating the adequacy of a break in a passive loop for simulating the blowdown behavior of a system with two or more operating loops.

The two principal factors to be considered in arriving at a test system configuration are hydraulic behavior and heat transfer. Each of these factors imposes constraints upon the system if that system is to exhibit similarity in behavior to a large PWR during an LOCA. Hydraulic similitude (with respect to a PWR) requires similar volume, pressure, and temperature distributions and similar mass velocities in the core. To ensure similar heat transfer, a one-to-one relationship in length and power density of the core heat transfer surfaces is required.

[a] Following the period covered by this document, the two-loop semiscale concept was deferred and emphasis placed on the 1-1/2-loop system. This section is presented only for completeness in describing the 1970 activities.

FIG. 152 1-1/2-LOOP SEMISCALE VESSEL ASSEMBLY WITH 66-INCH CORE.

INC-A-17825

FIG. 153 1-1/2-LOOP SEMISCALE SYSTEM.

A two-loop commercial PWR currently under construction was selected as the reference system for conceptual design of the two-loop test facility. A two-loop test system was selected rather than three- or four-loop systems in the interest of practicality and economy.

The conceptual two-loop test system was selected to have a volume distribution which is nearly the same as that of the reference plant two-loop commercial PWR. Vertical dimensions in the test system were preserved on a nearly one-to-one basis; a full length (12 foot) core, steam generator, and pressurizer were provided. In addition, the test system was selected to have operating parameters representative of those of the reference PWR.

An investigation was conducted using the RELAP3[2] code to compare the decompression behavior of the two-loop PWR and the two-loop test system. In this study both inlet and outlet double-ended breaks were investigated.

The decompression behavior can be characterized in terms of the transient behavior of system pressure and flow at various system locations. Flow is considered to be the more sensitive index of system behavior and, therefore, is examined in greater detail than pressure.

The results of the comparative analysis are shown in Figures 154 through 161. The inlet break behavior is shown in Figures 154 through 157 and the outlet break behavior is shown in Figures 158 through 161. Figures 154 and 158 show comparisons of the pressure responses of the reference plant and the modeled system. The pressure behavior of the modeled system is seen to agree quite well with that of the reference reactor. The remaining figures show comparisons of flow rate for various system locations. The core flow is of greatest interest because it has major influence on the core thermal response. Figures 155 and 159 show that good agreement in both the magnitude and shape of the mass flux at the core inlet is achieved between the reference and the test systems. Flow comparisons at the other locations, shown in the remaining figures, are not in as close agreement as those for the core; however, flows are in similar directions and have similar cyclic behavior.

The calculated blowdown behavior of the conceptual two-loop test system appears to be sufficiently similar to that calculated for the reference PWR system to fulfill the program objectives.

INC-A-16087

FIG. 154 OUTLET PLENUM PRESSURE, 100% INLET BREAK.

INC-A-16082

FIG. 155 MASS VELOCITY AT CORE INLET, 100% INLET BREAK.

INC-A-16084

FIG. 156 MASS VELOCITY AT VESSEL OUTLET, UNBROKEN LOOP, 100% INLET BREAK.

INC-A-16083

FIG. 157 MASS VELOCITY AT STEAM GENERATOR MIDPOINT, UNBROKEN LOOP, 100% INLET BREAK.

FIG. 158 OUTLET FLENUM PRESSURE, 100% OUTLET BREAK. INC-A-16103

FIG. 159 MASS VELOCITY AT CORE INLET, 100% OUTLET BREAK. INC-A-16085

FIG. 160 MASS VELOCITY AT VESSEL OUTLET, UNBROKEN LOOP, 100% OUTLET BREAK. INC-A-16081

FIG. 161 MASS VELOCITY AT STEAM GENERATOR MIDPOINT, UNBROKEN LOOP, 100% OUTLET BREAK. INC-A-16079

2. BOILING WATER REACTOR - FULL LENGTH EMERGENCY COOLING HEAT TRANSFER (BWR-FLECHT) TESTS PROJECT

(R. W. Griebe)

The purpose of the BWR-FLECHT Project^[103] is to provide experimental data concerning the thermal and hydraulic phenomena associated with terminating an LOCA core thermal transient by means of ECC in boiling water reactors. Tests are conducted employing bundles of 49 electrically heated rods of full length (12 feet). The heater pins are designed to simulate the geometry (pin radius and pitch) and the fuel pin temperature and decay heat characteristics of boiling water reactor fuel pins. Tests employing both core spray (top injection) and core flooding (bottom injection) ECC systems have been conducted in the BWR-FLECHT Project. The data from the tests are being used for analytical model development and evaluation.

The BWR-FLECHT test program^[104] was initiated in 1968 by General Electric Company under subcontract to Idaho Nuclear Corporation. Bundles of electrically heated stainless steel- or zircaloy-clad fuel rod simulators were used in the tests. Test results were obtained for various bundle powers, flow rates, and initial temperatures up to 1800°F. Tests were conducted at atmospheric pressure under both transient and steady state power conditions (power in the bundle continuously reduced to follow postulated power decay or power held constant with respect to time).

Results of BWR-FLECHT tests conducted prior to 1970 have been reported^[105-110] and are summarized in Tables XXIV through XXVI. Table XXIV presents a brief summary of conditions and significant results for the transient bottom flooding tests at atmospheric pressure. Table XXV summarizes the conditions and significant results for transient top spray tests, and Table XXVI summarizes the results for the combined transient spraying and flooding tests. All of the tests summarized in Tables XXIV through XXVI were performed with stainless steel-clad bundles. Two experiments using zircaloy-clad heater rod bundles were performed prior to 1970. The first of these, the molybdenum-performance test, provided initial data with zircaloy for top spray conditions. The second zircaloy experiment, a demonstration type test, provided data on spray cooling for internally pressurized fuel rod simulators.

The BWR-FLECHT test program was completed in October 1970. Activities of the BWR-FLECHT Tests Project during 1970 were concentrated in three areas. The first area was analysis of data from the top spray cooling tests performed in 1969 with stainless steel-clad Bundle SS-2N. The second area included a theoretical investigation to determine the feasibility and limitations of employing zircaloy-clad bundles with molybdenum filaments at high temperatures followed by actual tests and subsequent analysis of test data. The third area included both top spray and bottom flooding parametric testing at system pressures up to 300 psi employing stainless steel-clad bundles.

TABLE XXIV

RESULTS OF BWR-FLECHT TRANSIENT BOTTOM FLOODING TESTS

Test	Initial Power (kW)	Flooding Rate (in./sec)	T _i [a] (°F)	T _{max} [a] (°F)	ΔT [a] (°F)	t _{max} [b] (min)	t _{2/3} [b] (min)	t _{sat} [b] (min)	h(t _{max}) [c] (Btu/hr-ft ² -°F)
SS1N-1	240	3.70	1340	1360	20	0.5	2.0	2.7	8.31
SS1N-2	240	3.70	1560	1560	10	~0.2	2.0	2.0	6.90
SS1N-3	240	3.70	2000	2000	~0	~0.0	2.0	2.8	[d]
SS1N-4	300	3.70	1330	1360	30	0.5	2.0	2.8	11.09
SS1N-5	300	3.70	1720	1735	15	0.4	2.0	4.5	7.15
SS1N-6	300	3.70	2150	2150	0	0.0	3.5	3.0	6.40
SS1N-7	390	3.70	1400	1450	50	0.8	2.0	2.7	15.20
SS1N-8	390	3.70	1605	1620	15	~0.2	2.5	4.2	11.52
SS1N-9	390	3.70	1820	1820	~0	~0.1	2.5	4.4	10.85
SS1N-10	390	3.70	2010	2010	~0	~0.0	2.5	4.4	8.50
SS1N-11	240	0.63	1400	1550	150	2.5	7.0	7.5	4.86
SS1N-12	240	1.32	1300	1370	70	1.0	3.5	3.5	8.30
SS1N-13	240	3.70	1340	1360	20	~0.5	2.0	2.7	8.31
SS1N-14	240	4.87	1430	1430	~0	0.5	1.4	2.3	10.27
SS1N-15	240	6.05	1350	1350	~0	~0.3	1.0	1.8	11.07
SS1N-16	390	1.32	1430	1540	90	1.3	4.0	4.5	11.60

TABLE XXIV (Contd.)

RESULTS OF BWR-FLECHT TRANSIENT BOTTOM FLOODING TESTS

Test	Initial Power (kW)	Flooding Rate (in./sec)	T_i [a] ($^{\circ}\text{F}$)	T_{max} [a] ($^{\circ}\text{F}$)	ΔT [a] ($^{\circ}\text{F}$)	t_{max} [b] (min)	$t_{2/3}$ [b] (min)	t_{sat} [b] (min)	$h(t_{\text{max}})^{[c]}$ (Btu/hr-ft ² - $^{\circ}\text{F}$)
SS1N-17	390	3.70	1400	1450	50	0.8	2.0	2.7	15.20
SS1N-18	390	6.05	1380	1420	40	~0.4	2.0	2.0	13.90

[a] T_i = initial maximum temperature of the bundle

T_{max} = maximum bundle temperature

$$\Delta T = T_{\text{max}} - T_i.$$

[b] t_{max} = time at which maximum temperature occurred

$t_{2/3}$ = time for bundle to flood to the two-thirds full (8-ft) level

t_{sat} = time for entire bundle to cool to saturation temperature.

[c] $h(t_{\text{max}})$ = film coefficient at time of maximum temperature.

[d] Insufficient data.

TABLE XXV

RESULTS OF BWR-FLECHT TRANSIENT TOP SPRAY TESTS

Test	Initial Power (kW)	Spray Rate (gpm)	Maximum Initial Bundle Temperature (°F)	Maximum Temperature (°F)	Time of Maximum Temperature (min)
SS2N-1	100	2.45	1320	1435	6.8
SS2N-2	200	2.45	1365	1805	8.4
SS2N-2 ^[a]	200	2.45	1330	1840	8.4
SS2N-2 ^[b]	200	2.45	1340	1885	8.3
SS2N-3	250	2.45	1360	1925	8.8
SS2N-6	100	2.45	1595	1635	1.2
SS2N-7 ^[c]	200	2.45	1645	2000	5.0
SS2N-8	250	2.45	1660	1970	5.0
SS2N-10	100	2.45	1815	1885	0.5
SS2N-11	200	2.45	1840	1940	2.0
SS2N-13	100	2.45	965	1185	7.6
SS2N-14	250	2.45	875	1890	10.5
SS2N-15	250	2.45	1025	1935	13.7
SS2N-16	250	3.25	865	1880	11.7
SS2N-17	100	3.25	1350	1390	1.0
SS2N-18	250	3.25	1340	1880	8.9
SS2N-21	200	3.25	1630	1780	9.4
SS2N-22	250	3.25	1670	1960	4.2
SS2N-23	100	3.25	1850	1875	0.4
SS2N-24	250	3.25	1850	2150	4.0
SS2N-26	250	5.0	1340	1750	8.9
SS2N-26 ^[d]	250	5.0	1360	1920	6.3
SS2N-28	100	5.0	1840	1855	0.3

[a] Repeat of Test SS2N-2 late in life of bundle.

[b] Repeat of Test SS2N-2 late in life of bundle using different heatup power.

[c] Test performed twice because of data acquisition system malfunction.

[d] Repeat of Test SS2N-26.

TABLE XXVI

RESULTS OF BWR-FLECHT COMBINED SPRAYING AND FLOODING TRANSIENT TESTS

Test	Initial Power (kW)	Spraying Rate (gpm)	Flooding Rate (in./sec)	Maximum Initial Bundle Temperature (°F)	Time of Flooding Initiation (min)	Maximum Temperature (°F)	Time of Maximum Temperature (min)
SS3-17	200	2.0	3.0	1365	2.0	1620	2.1
SS3-18	250	2.5	3.0	1335	2.0	1680	2.2
SS3-19	250	2.5	4.7	1360	2.1	1680	2.2
SS3-20	325	3.25	3.0	1335	1.9	1805	2.0
SS3-21	325	3.25	6.0	1360	2.0	1835	2.0
SS3-22	250	2.5	6.0	1600	2.0	1900	2.0
SS3-23	250	2.5	6.0	1870	1.5	2110	1.5
SS3-26	250	2.5	2.0	1330	2.1	1680	2.2

2.1 Analysis of Test Data for Top Spray Emergency Core Cooling

Transient (decaying power) top spray cooling tests were performed on stainless steel-clad Bundle SS-2N for a range of initial bundle powers and initial cladding temperatures with spray rates of 2.45, 3.25, and 5 gpm as shown in Table XXV. The primary test data were cladding temperature versus time at various elevations on selected rods. The analysis of the test data was a three-step process, shown schematically in Figure 162. The temperature-time data and power decay history were programmed as input to the DATAR (DATA Reduction)^[111] computer code which solved the inverse heat conduction equation for the total heat flux at the rod surface. The total heat transfer coefficient was then calculated assuming the coolant was at saturation temperature.

The second step in the data reduction process was calculation of the radiation heat transfer within the bundle from rod to rod and from rod to canister. The computer code RADHT (RADIation Heat Transfer)^[112] was used to calculate the radiation heat transfer coefficient. This code assumes half-symmetry within the bundle, assumes that the medium is transparent to radiation heat transfer, and includes a temperature-dependent emissivity. Once the total coefficient and the radiation coefficient were calculated, the coefficient for energy transfer directly to the coolant (convection heat transfer) was obtained by subtraction (the third and final step).

2.11 Effect of Parameters on Heat Transfer During Top Spray Cooling Tests. The parameters studied and discussed in the succeeding paragraphs are initial bundle temperature, initial bundle power, and spray rate.

(a) Effect of Initial Temperature. Figures 163 and 164 present the temperatures and total heat transfer coefficients at the bundle midplane of the center rod and of an outside rod for several initial temperatures. The highest and lowest initial temperatures differ by about a factor of two. The total heat transfer coefficient shows little sensitivity to initial temperature. The differences in maximum temperatures attained during the tests (100 to 200 Fahrenheit degrees) are much smaller than the differences in initial temperatures (700 to 800 Fahrenheit degrees) for the midplane elevations. Figures 165 and 166 show the temperatures and heat transfer coefficients for the lowest and highest instrumented elevations for rods located in the central region of the bundle. Apparently the effect of initial temperature on total heat transfer coefficient increases somewhat for locations above the bundle midplane and diminishes for locations below the bundle midplane.

(b) Effect of Spray Rate. Figures 167 and 168 show the cladding temperature and total heat transfer coefficient at the bundle midplane of the center rod and of a rod on the outside edge of the bundle for tests with the same initial temperature and bundle power but with different spray rates. The main effect of increasing the spray rate is to reduce the temperature of the heater somewhat more rapidly; the effect on the heat transfer coefficient is not appreciable at the bundle midplane until after the temperature rise is terminated (temperature turnaround). Figures 169 and 170 show the effect of spray rate at the highest and lowest instrumented elevations of the center and outside rods. The lowest instrumented elevations are unaffected by spray rate. The highest instrumented elevations, however, show a sizeable increase in the total heat transfer coefficient

DATAR - Step 1

RADHT - Step 2

Convection - Step 3

$$h_c = h_T - h_r$$

FIG. 162 BWR-FLECHT DATA ANALYSIS TECHNIQUE.

FIG. 163 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT THE MIDPLANE OF THE CENTER ROD FOR SEVERAL INITIAL BUNDLE TEMPERATURES.

FIG. 164 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT THE MIDPLANE OF AN OUTSIDE ROD FOR SEVERAL INITIAL BUNDLE TEMPERATURES.

FIG. 165 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT THE LOWEST INSTRUMENTED ELEVATION OF THE CENTER ROD FOR SEVERAL INITIAL BUNDLE TEMPERATURES.

FIG. 166 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT THE HIGHEST INSTRUMENTED ELEVATION OF A CENTRAL ROD AS FUNCTIONS OF INITIAL BUNDLE TEMPERATURE.

FIG. 167 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT THE 5-FT ELEVATION OF THE CENTER ROD AS FUNCTIONS OF SPRAY RATE.

FIG. 168 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT THE MIDPLANE OF AN OUTSIDE ROD AS FUNCTIONS OF SPRAY RATE.

FIG. 169 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS FOR THE HIGHEST AND LOWEST INSTRUMENTED ELEVATIONS ON THE CENTER ROD AS FUNCTIONS OF SPRAY RATE.

FIG. 170 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS FOR THE HIGHEST AND LOWEST INSTRUMENTED ELEVATIONS ON AN OUTSIDE ROD AS FUNCTIONS OF SPRAY RATE.

for the higher spray rate. The temperature histories for locations nearer the spray nozzles reflect greater sensitivity to the spray flow rate than the temperature histories for locations further down the bundle. The highest power region of the bundle is near the midplane elevation, but this location is apparently far enough from the spray nozzle that no significant increase in heat transfer occurs from increased spray rate.

(c) Effect of Bundle Power. Tests were performed at three power levels with the same spray rates and initial temperatures. Figure 171 shows the effect of bundle power at the midplane for a rod at the center of the bundle. The strong dependence of maximum cladding temperature and time of rod surface quenching on bundle power are quite apparent at this elevation; however, little apparent dependence of the total heat transfer coefficient on the bundle power occurs up to the time of rod quenching at this elevation. Comparisons at other bundle locations, presented in Figures 172 and 173, also indicate a strong dependence of the maximum temperature on power level. Heat transfer coefficients at top and bottom elevations throughout the bundle show only small dependence on bundle power. The heat transfer coefficient at bundle midplane of an outside rod exhibits a significant dependence on power level. At the midplane of the outside rod both the maximum temperature and the total heat transfer coefficient are larger for the higher powers.

2.12 Radiation Heat Transfer During Top Spray Cooling Tests. The radiation heat transfer in the BWR-FLECHT bundle was computed assuming diffuse grey-body radiation through a transparent medium. Only the midplane elevation was used for the analysis because it singularly was sufficiently instrumented to permit accurate calculations.

Figures 174 and 175 present plots of the total heat transfer coefficient and the radiation component of the coefficient for an outside rod one row removed from the outside of the bundle for top spray cooling tests. The data presented in Figures 174 and 175 show that the largest part of the total heat transfer coefficient is due to body-to-body radiation rather than convection heat transfer directly to the coolant. The radiation heat transfer coefficient for any given rod depends, of course, on its radiation properties of transmissivity, absorptivity, and reflectivity as well as the temperature of every other rod in the bundle and the temperature of the canister.

The sensitivity of the calculated radiation heat transfer to the emissivity used in the code has been considered. Values used in the calculations were obtained from a temperature-dependent expression from the literature^[113]. A comparison of the radiation heat transfer coefficient obtained using this temperature-dependent expression (GEMP-482) with that obtained using representative constant values for emissivity is presented in Figure 176 for selected rods. The dependence of radiation heat transfer coefficient on the emissivity value increases with distance from the bundle center. That is, rods having a greater view of the canister have radiation heat transfer coefficients that are more dependent on the emissivity than rods near the center of the bundle. The greater dependence of outside rods on emissivity is due to the fact that the passive canister quenches to saturation temperature early in the transient.

FIG. 171 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT THE MIDPLANE ELEVATION OF THE CENTER ROD AS FUNCTIONS OF BUNDLE INITIAL POWER.

FIG. 172 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS FOR THE HIGHEST INSTRUMENTED ELEVATION OF A CENTRAL ROD AS FUNCTIONS OF BUNDLE INITIAL POWER.

FIG. 173 TEMPERATURES AND TOTAL HEAT TRANSFER COEFFICIENTS AT TWO ELEVATIONS OF AN OUTSIDE ROD AS FUNCTIONS OF BUNDLE INITIAL POWER.

FIG. 174 TEMPERATURE PROFILE AND CORRESPONDING TOTAL AND RADIATION HEAT TRANSFER COEFFICIENTS AT THE MIDPLANE ELEVATION OF AN OUTSIDE ROD.

FIG. 175 TEMPERATURE PROFILE AND CORRESPONDING TOTAL AND RADIATION HEAT TRANSFER COEFFICIENTS AT THE MIDPLANE ELEVATION OF A ROD ONE ROW FROM THE OUTSIDE.

FIG. 176 DEPENDENCE OF RADIATION HEAT TRANSFER COEFFICIENT ON EMISSIVITY FOR THREE DIFFERENT RODS.

The dependence of the local radiation heat transfer on rod position in the bundle is also shown in Figures 177 and 178 in which a temperature profile and corresponding radiation heat transfer coefficient profile along the bundle diagonal are presented at several times.

A more comprehensive picture of bundle temperature response and the associated radiation heat transfer coefficient response at bundle midplane is presented in Figure 179. These isometric plots have as the x- and y-axes

FIG. 177 RADIAL TEMPERATURE PROFILE AT BUNDLE MIDPLANE FOR VARIOUS TIMES.

FIG. 178 RADIAL RADIATION HEAT TRANSFER COEFFICIENT AT BUNDLE MIDPLANE FOR VARIOUS TIMES.

FIG. 179 ISOMETRIC PLOTS OF TEMPERATURE AND ASSOCIATED RADIATION HEAT TRANSFER COEFFICIENT RESPONSE AT BUNDLE MIDPLANE.

-3
MINUTES

-2
MINUTES

-1
MINUTE

0
MINUTES

1
MINUTE

2
MINUTES

ANC-E-332-H (Left)

3
MINUTES

4
MINUTES

5
MINUTES

6
MINUTES

7
MINUTES

8
MINUTES

9
MINUTES

10
MINUTES

11
MINUTES

ANC-E-333-H (Right)

the square rod array and the temperature or radiation heat transfer coefficient as the z-axes (vertical).

2.13 Convection Heat Transfer During Top Spray Cooling. The dependence of the calculated radiation heat transfer coefficient on rod emissivity affects the calculated convection heat transfer coefficient because the convection coefficient is determined as the difference between the total and radiation coefficients. Also, from the test data, radiation and, hence, convection coefficients can be calculated only for the midplane elevation.

The difference between the total and the radiation heat transfer coefficients presented in Figures 174 and 175 indicates the general behavior of the convection coefficient. For a significant portion of the temperature excursion, radiation from rod-to-rod and rod-to-canister appears to be the most significant heat transfer mechanism. During the period when maximum temperatures are obtained, the radiation heat transfer coefficient is nearly equal to the total heat transfer coefficient; the contribution of convection heat transfer is very small. Thus, radiation heat transfer, which appears to be controlled by canister quenching, is an important factor relative to the effectiveness of top spray emergency cooling.

Data shown in Figure 180 illustrate the behavior of the convection coefficients as a function of time after canister quenching for five rods during a test conducted from an initial temperature of 800°F. Although the magnitudes and time responses differ slightly from rod to rod, all rods shown in Figure 180 demonstrate nearly equivalent response of the convection coefficient. A rather abrupt change in convection heat transfer characteristics of the bundle was noted after Test 13 (Table XXV) of the testing sequence. Some change apparently occurred within the bundle, such as gross rod bowing or other distortion. Data obtained from tests after Test 13 have largely been neglected because of the unexplained behavior.

FIG. 180 TYPICAL BEHAVIOR OF CONVECTION HEAT TRANSFER COEFFICIENT.

2.2 High Temperature Zircaloy-Clad Bundle Experiments

The purpose of the tests with the zircaloy-clad bundle was to obtain information regarding the effect of cladding material on ECC effectiveness and information regarding the behavior of zircaloy under ECC conditions at temperatures up to 2400°F. Prior to testing, an evaluation was performed to determine the feasibility and limitations of the experiments performed with zircaloy-clad heater rods with specially wound molybdenum filaments and aluminum oxide insulator material.

2.21 Heater Rod Evaluation. Earlier testing at lower temperatures with stainless steel-clad bundles having molybdenum filamented heaters had shown that this type of bundle was susceptible to power shifting due to the temperature dependent resistivity of molybdenum. Axial power shifting within each rod tended to concentrate the power density at the hot spot in the rod, whereas radial power shifting from rod to rod in a bundle shifted power away from the hotter rods to the colder rods.

The radial power shifting could be controlled to some degree by using separate power supplies. A six-unit silicon-controlled rectifier power supply was selected for the tests with the rods being grouped to minimize the radial shifting within each group. The axial power shifting, however, could not be controlled. Therefore, analytical studies were performed to determine effects of axial power shifting on the outcome of the proposed experiments.

The SINDA-3G^[114] code was programmed into a 65-node representation (5 radial zones, 13 axial zones) of a zircaloy-clad heater having a molybdenum filament as shown in Figure 181. Input to the code consisted of total rod power and heat transfer information obtained from the stainless steel-clad bundle testing described earlier. The code was used to compute the temperature behavior of the rod and resultant axial power shifting. Rod powers used in the study were equivalent to those planned for the experiments.

The curves presented in Figure 182 show the shape of the axial power profile at several times for a central rod in the bundle. As indicated by the profile at time zero (transient initiation), the axial power profile has shifted upward along the rod. After the top spray is turned on, the profile shift reverses with the location of maximum power density being below the midplane. Since the top of the bundle is cooled first, the location of the maximum axial power density would be expected to shift to lower elevations as time increases.

The curves presented as Figure 183 show calculated linear power densities at the midplane elevation of a rod during a simulated transient test. Under worst case conditions when energy from metal-water reaction is considered, the portion of the total power attributable to molybdenum power shifting at the midplane is only about 10%. Although calculations at other conditions would yield different values for the power shifting contribution, the 10% figure generally characterizes the result of the SINDA power shifting study. The conclusion reached was that axial power shifting would not seriously affect the test results.

2.22 Results of the Three High-Temperature Zircaloy-Clad Bundle Tests. The response observed for each of the three high-temperature zircaloy-2 clad bundle tests was unique. A brief summary of the initial conditions and responses of the bundle are shown in Tables XXVII and XXVIII.

FIG. 181 HEATER ROD MODEL USED FOR SINDA-3G CALCULATIONS.

FIG. 182 REPRESENTATIVE AXIAL POWER SHIFT FOR A HIGH POWER DENSITY CASE, INITIAL TEMPERATURE OF 2150°F.

FIG. 183 CALCULATIONS OF POWER DENSITY AT ROD MIDPLANE FOR A REPRESENTATIVE HIGH POWER SITUATION.

The first test in the group, Test Zr-3, exhibited a rather severe skewing of the power and temperature profiles during the heatup phase of the transient. Steam entering at the bottom of the bundle produced increased cooling in the lower half of the bundle, causing the power profile to have its maximum power density at the eight-foot elevation rather than the six-foot elevation as shown in Figure 184. This condition was eliminated in the Zr-4 and -5 bundle tests. The maximum temperature profiles for the three bundles are compared in Figure 185. Although Tests Zr-3 and -4 exhibited similar temperature behavior, the delayed flooding of the Zr-5 test considerably shortened the time needed to cool the bundle.

Posttest bundle disassembly revealed that considerable heater rod damage occurred during all three high-temperature zircaloy tests. For Test Zr-3, the area of damage was limited to approximately the seven to eight-foot elevation. Damage consisted of rods that were fractured and fused together, indicating cladding melting had occurred. For Test Zr-4, the area of damage extended from just below bundle midplane to the bottom of the heated length. Cladding fragmentation, melting, and fusing of rod surfaces were extensive. For Test Zr-5, the area of cladding damage was limited to elevations near midplane and

TABLE XXVII

TEST CONDITIONS AND RESULTS FOR BWR-FLECHT ZIRCALOY-CLAD BUNDLES

Bundle Number ^[a]	Zr-3	Zr-4	Zr-5
Initial Temperature (°F)			
Planned	2150	2100	2200
Actual	2300	2290	2310
Maximum Indicated Temperature (°F)	2940	2827	2915
Elevation of Maximum Temperature (ft)	8	4	6
Rod Exhibiting Maximum Temperature	25	17	17
Initial Power Level (kW)	240	240	300
Corresponding Full Power Level [MW(t)]	4.8	4.8	6.0
Spray Rate (gpm)	2.45	2.45	3.25
Flooding Rate (in./sec)	--	--	6
Outer Channel Flow Rate (gpm)	0.49	0.49	0.65
Subcooling (°F)	80	80	80
Flooding Delay Time (sec)	--	--	52

[a] The bundle number (Zr-3, Zr-4, and Zr-5) is only an identification of the test conducted and should not be confused with the cladding material which was zircaloy-2 for all zircaloy-clad bundles.

was much less severe than for the previous two tests. In general, pieces of cladding were missing and some fracture occurred for Test Zr-4. In all three zircaloy-clad bundles, no damaged rods were found in the outer course of rods, that is, at those locations closest to the canister.

Analysis of the data from the zircaloy-clad bundles showed sudden temperature excursions, possibly related to the cladding damage, in many of the temperature-time profiles for the hotter locations in the bundle. Neither pretest predictions nor posttest simulations using SINDA showed temperature increases with rates equivalent to those measured in the high-temperature tests. Behavior of this type was not experienced in the earlier stainless steel-clad bundle experiments. Posttest metallurgical examination of rod samples from the zircaloy bundles did, however, provide an explanation for the temperature excursion. The results and conclusions from the metallurgical investigation are described in the following section.

2.23 Metallurgy of the High-Temperature Zircaloy-Clad Bundles. Samples of tubing were removed from all three zircaloy bundles for metallurgical examination of the cladding. The findings generally agreed with previous work^[115] performed on reactor grade zircaloy cladding samples. Of particular

TABLE XXVIII

TEST SUMMARY FOR BWR-FLECHT ZIRCALOY-CLAD BUNDLES

Bundle	
Zr-3	Power profile skewed upward at transient initiation due to excessive steam input in bottom of bundle during heatup. Maximum temperature occurred at 8-ft elevation instead of at midplane. Posttest examination showed cladding damage occurred at the 8-ft elevation of the center rods in the bundle. Rod electrical failures occurred during the experiment.
Zr-4	Transient initiated with symmetric power profile but data show indications of water accumulation in bundle bottom during first few minutes of test. Later the maximum temperature occurred at elevations below the midplane. Significant cladding damage below the midplane was found during bundle disassembly. Rod electrical failures occurred during the experiment.
Zr-5	Shortly after transient initiation, thermocouple behavior became erratic with very high temperatures indicated by many thermocouples. Delayed bottom flooding was initiated after approximately one minute. Localized areas of cladding damage were found at bundle disassembly. Some rod failures occurred during the experiment.

importance in these examinations, however, was the information regarding the cause for the unexpected temperature excursions and subsequent cladding degradation.

A photograph of a rod cross section, shown in Figure 186, illustrates an area of cladding damage at the inner surface. This region of damage was determined by electronmicroprobe analysis to consist of alloyed aluminum and zirconium, indicating interaction between the aluminum oxide insulator material and the zircaloy cladding which had not been recognized during the original heater rod design studies.

Differential thermal analysis (DTA) tests were performed on samples of Al_2O_3 and Zr-2 to measure the heat of formation of the alloy. The DTA tests showed that the reaction between the insulator and cladding was not significantly exothermic or endothermic, indicating that this chemical reaction was not responsible for the thermal excursions.

Samples of reactor grade zircaloy tubing having either Al_2O_3 or ZrO_2 as filler material were then heated in an induction furnace in both steam and argon atmospheres. All samples with the Al_2O_3 filler underwent a reaction such that a liquid metal film appeared on the surface of the cladding. The liquid metal film from the Al_2O_3 -filled sample in the steam atmosphere simultaneously underwent a brilliant pyrotechnic-type reaction whereas the sample in argon did not. No samples containing ZrO_2 filler reacted in this manner.

FIG. 184 AXIAL POWER PROFILES FOR THE THREE HIGH-TEMPERATURE ZIRCALOY EXPERIMENTS.

FIG. 185 MAXIMUM TEMPERATURE ENVELOPES FOR THE THREE HIGH-TEMPERATURE ZIRCALOY BUNDLE TESTS.

FIG. 186 A CROSS-SECTION MACROGRAPH OF ROD 11 AT THE 8-FT ELEVATION -- TEST ZR-3.

These investigations indicate the following likely sequence of events as the cause of the temperature excursions during the high temperature BWR-FLECHT tests[116].

- (1) The zirconium in the cladding reduced the Al_2O_3 insulator material by a slightly endothermic reaction, freeing the aluminum.
- (2) The free aluminum alloyed with the cladding. The resultant alloy has a sufficiently low melting point to be liquid at test temperatures.
- (3) The liquid metal reached the outside of the cladding either by penetrating along grain boundaries or by consuming the cladding.
- (4) Extremely rapid oxidation of the exposed liquid metal occurred. Because of the molten state of the rod surface, buildup of a reaction-limiting oxide layer was inhibited.
- (5) The energy released by the essentially unlimited metal-water reaction caused an abrupt temperature increase.

The insulator-cladding reaction also appears responsible for the cladding fracture and the overall cladding degradation observed during posttest bundle disassembly. Regions of alloyed material were extremely brittle.

The severe cladding degradation experienced in the high-temperature zircaloy-clad tests should not be considered typical of nuclear fuel bundles because aluminum oxide is not present in nuclear fuel pins.

2.3 High-Pressure Stainless Steel-Clad Bundle Experiments

Two sets of high-pressure experiments were performed on a stainless steel-clad bundle. The first set consisted of top-spray-only ECC tests at pressures up to 300 psi. The second set consisted of bottom-flooding ECC tests at pressures up to 300 psi. Run conditions are listed in Tables XXIX and XXX, respectively.

2.31 Top Spray ECC Tests at Elevated Pressures. Table XXIX presents a brief summary of conditions and significant results of high-pressure top spray ECC tests. In general, the effectiveness of top spray improves with increasing system pressure. Figure 187 presents the maximum bundle temperature as a function of system pressure for an initial temperature of about 1300°F, initial power of 250 kW, an initial spray temperature of 150°F, and a spray rate of 2.45 gpm. The curve in Figure 187 should be considered only approximate; additional analysis is in progress to produce a more explicit relationship between maximum bundle temperature and system pressure.

2.32 Bottom Flooding ECC Tests at Elevated Pressures. A brief summary of the conditions and results of the high-pressure bottom flooding-only stainless steel-clad bundle experiments is presented in Table XXX. Data from these tests show that the effectiveness of bottom flooding ECC, as measured by the heat transfer coefficient and corresponding time to rod quenching, improves with increasing system pressure.

TABLE XXIX

RESULTS OF BWR-FLECHT HIGH-PRESSURE TOP SPRAY TESTS

Test	Initial Power (kW)	Spray Rate (gpm)	Pressure (psia)	Initial ^[a] Temperature (°F)	Maximum ^[b] Temperature (°F)	Time of ^[c] Maximum Temperature (min)
SS4N-1	250	2.45	15	1365	2135	11.2
NI	250	2.45	15	1020	2090	14.1
SS4N	250	2.45	15	1656	2199	8.9
SS4N-2	250	2.45	30	1267	2014	11.1
SS4N-3	250	2.45	60	1340	2043	9.5
SS4N-4	250	2.45	90	1340	1986	8.1
SS4N-5	250	2.45	120	1380	1792	6.2
SS4N-6	250	2.45	200	1329	1849	7.8
SS4N-7	250	2.45	300	1361	1691	5.5

[a] Highest temperature indication at transient initiation.

[b] Highest recorded temperature in the bundle.

[c] Time after transient initiation of maximum temperature.

TABLE XXX

RESULTS OF BWR-FLECHT HIGH-PRESSURE FLOODING TESTS

Test	Initial Power (kW)	Flooding Rate (in./sec)	Pressure (psia)	Coolant Temperature (°F)	Initial ^[a] Temperature (°F)	Maximum ^[b] Temperature (°F)	Time of ^[c] Maximum Temperature (sec)
SS4N-8	250	3	15	150	1486	1517	20
SS4N-9	250	3	30	150	1479	1526	10
SS4N-10	250	3	45	150	1525	1586	7.5
NI	250	3	60	100	1429	1532	17
SS4N-11	250	3	60	150	1451	1459	9
NI	250	3	60	200	1415	1456	12
SS4N-12	250	3	100	150	1422	1455	3.5
SS4N-13	250	3	200	150	1461	1467	2
SS4N-14	250	3	300	150	1440	1445	2
SS4N-17	250	3	60	150	1703	1731	7
SS4N-18	250	6	60	150	1760	1774	6

[a] Highest temperature recorded in the bundle at transient initiation.

[b] Highest temperature recorded by thermocouple used to monitor initial temperature.

[c] Time after transient initiation of maximum temperature indicated by thermocouple used to monitor initial temperature.

FIG. 187 SENSITIVITY OF TOP SPRAY COOLING TO PRESSURE.

Figure 188 presents the temperature and heat transfer coefficient profiles for the midplane elevation of a corner rod in the bundle. As pressure was increased from 15 to 200 psia, the heat transfer coefficient increased with proportionate reduction of time to rod quenching. However, a slight decrease in the relative heat transfer effectiveness was noted as the pressure was increased from 200 to 300 psia. A similar response is apparent in the data for the midplane elevation of a central rod, as shown in Figure 189. In general, some decrease in the heat transfer coefficient was noted at 300 psia for all locations in the bundle.

A possible explanation for the change in response as a function of pressure for the 300-psi tests involves the test procedures. For all flooding tests, the technique used was to continue flooding into the bundle at the cold flooding rate until a static head of eight feet of water was reached. At this time in an actual BWR, the flooding water would begin to spill out of the jet pump downcomers. The flooding rate in the BWR-FLECHT tests was, therefore, varied to provide sufficient makeup to maintain only a measured static head of eight feet of water. The effect of variation of flooding rate is illustrated by the temperature and heat transfer coefficient profiles at the highest instrumented elevation (10.8 feet) in the bundle, shown in Figure 190. In every case except at 300 psia, the 10.8-foot elevation continued to cool when the flooding rate was decreased at 50 to 60 sec to provide makeup liquid. However, the temperature profile is flattened after 60 sec for the 300-psi test. Apparently the reason for the flattened temperature curve and the decreasing heat transfer coefficient is the lack of sufficient entrained water to continue established cooling. Eventually the thermocouple did indicate quenching at the 10.8-foot elevation.

FIG. 188 TEMPERATURES AND HEAT TRANSFER COEFFICIENTS FOR THE MIDPLANE ELEVATION OF A CORNER ROD AS FUNCTIONS OF PRESSURE.

FIG. 189 TEMPERATURES AND HEAT TRANSFER COEFFICIENTS FOR THE MIDPLANE ELEVATION OF A CENTRAL ROD AS FUNCTIONS OF PRESSURE.

FIG. 190 TEMPERATURES AND HEAT TRANSFER COEFFICIENTS FOR THE HIGHEST INSTRUMENTED ELEVATION OF A ROD ONE ROW FROM THE OUTSIDE AS FUNCTIONS OF PRESSURE.

Figures 191 and 192 compare the absolute heat fluxes corresponding to the temperatures and heat transfer coefficients presented as Figures 188 and 189. The heat flux plots show that energy is removed much earlier for the higher pressures than for the lower pressures.

2.4 BWR-FLECHT Summary

The BWR-FLECHT Tests Project activities during 1970 included analysis of data from top spray cooling tests performed on a stainless steel-clad bundle, feasibility studies and performance of high-temperature zircaloy-clad bundle tests with subsequent data analysis, and parametric testing of a stainless steel-clad bundle at system pressures up to 300 psi with both top spraying and bottom flooding ECC modes.

Analysis of data from the top spray cooling tests (stainless steel-clad bundle SS2N) consisted of evaluating the overall heat transfer process and then the radiation and convective components. The total heat transfer coefficient sensitivity to test parameters was shown to be a function of test parameters as well as location in the bundle. In general, however, the sensitivity of the total coefficient to test parameters was found to be very small at the midplane locations at the center rods.

The radiation heat transfer coefficient was shown to be functionally dependent on rod surface emissivity as well as temperature distribution. The convection heat transfer coefficient was found to be correlatable from test to test if data were normalized at the quenching time of the canister. Convection coefficients were found to be relatively small in comparison to radiation coefficients but are nonetheless significant in the overall cooling of the stainless steel-clad bundles.

An analytical study was performed to determine the effect of temperature dependent power profile shifting when molybdenum-filament heaters were used for the high temperature zircaloy-clad bundle tests. The conclusion reached was that the test results would be interpretable in spite of the temperature-dependent power profile shifting. Unfortunately, a materials incompatibility problem (interaction of Al_2O_3 insulator with the zircaloy cladding) occurred in all the high temperature tests with zircaloy-clad bundles so that the application of the zircaloy-clad bundle experiment results in evaluation of nuclear bundle response under top-spray ECC conditions is marginal.

The high-pressure tests performed on a stainless steel-clad bundle are providing valuable data concerning the influence of pressure on the effectiveness of both top spray and bottom flooding modes of ECC injection. On the basis of present analysis of the data from tests up to 300 psia, increased system pressure improves the heat transfer effectiveness of both ECC modes.

FIG. 191 SURFACE HEAT FLUX AT THE MIDPLANE ELEVATION OF A CORNER ROD AS A FUNCTION OF PRESSURE.

FIG. 192 SURFACE HEAT FLUX AT THE MIDPLANE ELEVATION OF A CENTRAL ROD AS A FUNCTION OF PRESSURE.

3. PRESSURIZED WATER REACTOR - FULL LENGTH EMERGENCY COOLING HEAT TRANSFER (PWR-FLECHT) TESTS PROJECT

(R. W. Shumway)

The purpose of the PWR-FLECHT Test Project^[117] is to provide experimental data concerning the thermal and hydraulic phenomena that occur when a loss-of-coolant accident (LOCA) core thermal transient is being terminated by means of emergency core cooling (ECC) systems in pressurized water reactors. This project provides data from which core reflooding heat transfer coefficients can be obtained and from which heat transfer correlations can be developed to predict the thermal response of PWR cores from the time ECC fills the lower core plenum until the core is reflooded.

The PWR-FLECHT test program^[118] was initiated in late 1968 by Westinghouse Electric Corporation under subcontract to Idaho Nuclear Corporation. By the start of 1970, 65 tests had been completed. An additional 23 tests were performed during the year. All tests were conducted using 12-foot-long electrically heated rods in either 49- or 100-rod bundles. The rods were placed in a square array and were surrounded by a square duct, or housing, which was heated to simulate additional rods.

Detailed descriptions of the PWR-FLECHT test facility and program are available^[111,117-119]. Briefly, the tests were performed by heating the bundle at constant power input to the desired initial test temperature, starting a programmed power decay and simultaneously initiating bottom flooding, and shutting off the water and power after the bundle temperature dropped to the water saturation temperature. Tests were performed with both stainless steel- and zircaloy-clad heater rods at both constant and variable core flooding rates. Table XXXI summarizes the ranges of test parameters investigated.

TABLE XXXI

PWR-FLECHT TEST PARAMETERS

Initial Cladding Surface Temperature, T_i (°F)	800, 1200, 1400, 1600, 1800, 2000, 2200, 2325
Initial Peak Power Density, Q_i (kW/ft of fuel)	0.69, 1.24, 1.40
Coolant Inlet Temperature, T_{water} (°F)	70, 100, 110, 150, 165, 200, 270
Coolant Pressure at Outlet, P (psia)	15, 30, 60, 75, 90
Number of Rods in Test Bundle	100, 49
Flooding Rate, V (in./sec)	0.6, 0.8, 1, 2, 4, 6, 10, 18

In addition to the test parameters indicated in Table XXXI, borated water, water fallback, and flow blockage were considered in the PWR-FLECHT tests. The results of the experimental investigation are summarized in subsequent discussion of the effect of the various test parameters on the thermal behavior as characterized by the transient heat transfer coefficient of the simulated reactor core subjected to LOCA and ECC reflooding conditions.

3.1 PWR-FLECHT Test Results

The principal measured data obtained from the PWR-FLECHT tests consist of temperature histories of the cladding at various bundle radial and axial locations for times after initiation of flooding. In the figures of this section, V is the flooding rate in in./sec, Q_1 is the initial peak power in kW/ft, T_1 is the initial cladding temperature in °F, P is the coolant pressure in psia, and ΔT_s is the water subcooling in °F.

An example of the temperature-time data is shown in Figure 193. These particular data were obtained for a variable-flow test. The temperature exhibits an initial and second peak followed by decay. An abrupt temperature reduction, or quenching, occurs at a rod temperature of about 1000°F as a result of rod surface wetting and greatly increased heat transfer coefficients.

The magnitude of the cladding temperature rise during the ECC reflooding portion of an LOCA is one measure of the ECC system effectiveness. Figure 194 illustrates the cladding temperature rises experienced in PWR-FLECHT stainless steel-clad bundle tests. Although these temperature rises are not directly applicable to pressurized water reactors, the data do make evident one general, and favorable, characteristic of the ECC process -- the higher the initial bundle temperature, the lower the temperature rise. This characteristic behavior is a result of increased steam generation and water entrainment, and of the greater temperature difference between the rod surface and the coolant at higher rod temperatures. The figure also shows that, in general, the temperature rise varies inversely with flood rate. Temperature rises for high flooding rate tests were difficult to determine accurately because temperature turnaround usually occurred within two or three seconds and temperatures were measured only every three to four seconds.

The DATAR code^[111], a transient conduction heat transfer program developed by Westinghouse Electric Corporation, uses the measured temperature-time data to calculate the transient heat flux, $q''(t)$. The transient heat transfer coefficient, $h(t)$ is then determined by

$$h(t) = q''(t) / (T_{\text{clad}}(t) - T_{\text{saturation}})$$

where T is temperature and t is time. Characteristics of bundle thermal behavior for PWR-FLECHT tests can be described by noting the effect of various test parameters on the transient heat transfer coefficient.

Most of the heat transfer coefficient results reported here are based on data taken at the bundle midplane. The heat transfer coefficients at higher and lower elevations are markedly different. Figure 195 presents the heat transfer coefficients at different elevations for a typical stainless steel-clad bundle test. As the distance from the quench front increases, steam superheating increases and the quantity and size of the water droplets entrained in the steam

FIG. 193 EXAMPLE OF PWR-FLECHT TEMPERATURE DATA.

FIG. 194 CLADDING TEMPERATURE RISE IN PWR-FLECHT STAINLESS STEEL-CLAD BUNDLE TESTS.

FIG. 195 PWR-FLECHT HEAT TRANSFER COEFFICIENTS AT DIFFERENT BUNDLE ELEVATIONS.

decreases. Evaporation and superheating reduce the cooling ability of the fluid so that heat transfer coefficients decrease as the elevation increases. Increased evaporation and superheating occurs when the fluid passes high temperature regions so the difference in heat transfer coefficients is larger between the six- and eight-foot elevations than between the eight- and ten-foot elevations, as shown in Figure 195.

The following presentation of the effects of the various test parameters on heat transfer coefficients for stainless steel-clad and zircaloy-clad bundle tests is based on partial analysis of test data. Conclusions reached here may be subject to future revision or qualification.

3.2 Stainless Steel-Clad Bundle Tests

The oxidation characteristics of stainless steel are such that, even under the high temperature test conditions of the PWR-FLECHT experiments, stainless steel-clad bundles could be used for repeated tests. In contrast, zircaloy oxidation at PWR-FLECHT conditions would have imposed the requirement of a new zircaloy-clad bundle for essentially every test. Thus, the bulk of the PWR-FLECHT experimental program was performed with heater bundles clad with stainless steel.

3.21 Initial Cladding Temperature. As illustrated in Figure 194, higher midplane temperatures at the time of flooding initiation resulted in reduced temperature rises. The increased cooling rate results from earlier water entrainment. Water must rise higher in low temperature bundles than in high temperature bundles before violent boiling and water droplet entrainment occurs.

Figure 196 shows the effect of initial temperature on the heat transfer coefficient. The early water entrainment results in initially higher midplane heat transfer coefficients. The water quench front moves up rods in the higher temperature bundles at a slower rate than in the lower temperature bundles because of the additional stored energy which must be removed.

FIG. 196 EFFECT OF INITIAL TEMPERATURE ON HEAT TRANSFER COEFFICIENT AT THE BUNDLE MIDPLANE.

The effect of initial temperature on heat transfer coefficients decreases as the flooding rate decreases, because at lower flooding rates the bundle takes longer to quench such that the energy generated prior to quenching is substantially greater than the initial stored energy. At a flooding rate of 1 in./sec, the effect is negligible and quench times are not strongly dependent on the initial bundle temperature.

3.22 Flooding Rate. The water velocity into the bottom of the bundle (flooding rate) is one of the most important parameters affecting the cladding temperature rise. Figure 197 illustrates the wide range of heat transfer coefficients obtained for constant flooding rates from 0.6 to 6 in./sec. Rod power was shut off during the test for a flooding rate of 0.6 in./sec because temperatures reached 2400°F which was the upper limit set to avoid heater rod failures.

Because of either accumulator depletion or upper plenum steam pressurization, variable flow may occur during ECC injection. Figure 198 shows heat

FIG. 197 EFFECT OF FLOODING RATE ON HEAT TRANSFER COEFFICIENT AT THE BUNDLE MIDPLANE -- CONSTANT FLOODING RATE TESTS.

transfer coefficients from three variable flow tests during which entrained water that reached the upper plenum was allowed to return to the bundle (water fallback). One test (Test 7670) used a ramp-type flooding rate which started at 9 in./sec and was linearly reduced to 6 in./sec over a 17-sec period. Flow was then reduced to 1 in./sec within 1 sec. The other two tests used a step-type flooding rate; flow began at a rate of 6 in./sec and was reduced to 1 in./sec at either 4 or 8 sec. An unexplained spike occurs in the heat transfer coefficient for Test 8975 at about 160 sec.

Constant flooding rate data may be used to estimate variable-flow heat transfer coefficients. During the high-flooding-rate portion of a variable flow test, heat transfer coefficients from comparable constant flooding rate tests can be used. However, difficulty arises in determining which heat transfer coefficient should be used subsequent to the time the flooding rate is reduced. One method, which considers total water mass input, is used to predict heat transfer coefficients for water fallback tests. Heat transfer coefficients for tests which do not include fallback may be estimated by considering the quench front location at the time of flow reduction.

3.23 Initial Power. Figure 199 presents the effect of initial power on heat transfer coefficients at the bundle midplane. The effect of increased power on cooling at the midplane is apparent after the first 30 sec for test conditions of the three tests referred to in the figure. The quench front moves up the rods at

FIG. 198 EFFECT OF FLOODING RATE ON HEAT TRANSFER COEFFICIENT AT THE BUNDLE MIDPLANE -- VARIABLE FLOODING RATE TESTS.

FIG. 199 EFFECT OF INITIAL POWER ON HEAT TRANSFER COEFFICIENT.

a slower rate for higher power generation rates. The lower quench front requires the entrained water droplets to traverse greater lengths of rod between the quench front location and the thermocouple at the bundle midplane. Combinations of fewer, smaller, or hotter water droplets at the bundle midplane thus produce the effect of reduced heat transfer coefficients for higher initial power levels. For a flooding rate of 2 in./sec, increasing the power increases the time to rod quenching at the bundle midplane such that the ratio of the generated energy plus initial stored energy to the quench time is maintained at about 1.1 kW/ft. Since the power decay becomes small after about 100 sec the 1.1 kW/ft ratio can be used to predict bundle midplane quench times at other power levels. Similar ratios may be calculated for other flooding rates or elevations.

Since the initial temperatures of all three tests in Figure 199 were the same, differences in the heat transfer coefficients during the first 5 to 10 sec are probably due to differences in test parameters other than initial power such as pressure, inaccuracies in determining the exact time that flooding occurred, and errors in data reduction.

3.24 Power Decay. Difficulties with rod instrumentation resulted in three different programmed power decays being used at various times in PWR-FLECHT tests. Figure 200 shows the three power decay curves; the central curve is the one which meets test specifications. The low and high power decay values differ by a maximum of about 15% which is not enough to noticeably affect the heat transfer coefficients as is apparent from Figure 201. Larger differences in power decay are expected to affect heat transfer coefficients in the same manner as initial power, as discussed in the previous section.

FIG. 200 RATIO OF DECAY POWER TO INITIAL POWER FOR PWR-FLECHT TESTS 5948, 6848, AND 6948.

FIG. 201 EFFECT OF POWER DECAY ON HEAT TRANSFER COEFFICIENT.

3.25 Pressure. Fluid pressure within the reactor core region following blowdown is influenced by the containment vessel pressure which in turn depends on the effectiveness of the pressure suppression system. PWR-FLECHT tests were performed for system pressures ranging from 15 to 90 psia. Figure 202 illustrates the results of five tests at a subcooling of 140°F . These data indicate a transition in cooling effectiveness between 56 and 30 psia. At 30 psia the heat transfer coefficients are about 30% less and bundle quenching takes about 30% longer than at 56 psia.

The flooding rate for the test data shown in Figure 202 is 6 in./sec. Figure 203 presents additional data at a flooding rate of 4 in./sec. The effect of low pressure on heat transfer coefficients appears to increase with decreasing flooding rate. The net effect of reducing the pressure from 58 to 15 psia at a flooding rate of 4 in./sec is to increase the time to quench by 85%.

3.26 Subcooling. The degree of flooding water subcooling determines how much energy the water can absorb before it vaporizes; increasing the water subcooling aids cooling. Figure 204 shows the transient heat transfer coefficient for tests with different initial subcooling. Test 4225 had a subcooling, ΔT_s , of 139°F ; the water absorbed about 139 Btu/lb_m prior to reaching the saturation temperature. Water in Test 5123 with $\Delta T_s = 24^\circ\text{F}$ could absorb only about 24 Btu/lb_m and thus much more water was required in order to quench the bundle.

FIG. 202 EFFECT OF PRESSURE ON HEAT TRANSFER COEFFICIENT -- V = 6 IN./SEC.

FIG. 203 EFFECT OF PRESSURE ON HEAT TRANSFER COEFFICIENT -- V = 4 IN./SEC.

FIG. 204 EFFECT OF SUBCOOLING ON HEAT TRANSFER COEFFICIENT.

3.27 Water Fallback. During reflooding, the entrained ECC water which passes through the reactor core is not permanently lost for purposes of core cooling. Some of the water droplets may strike upper plenum support structures or other droplets, coalesce, and drop back into the core in areas where the steam velocity drag forces are smaller than the weight of the water droplet. Any water that does fall back into the core adds to the effective coolant inventory and results in increased heat transfer coefficients. The ratio of the upper plenum cross-sectional area to the total rodflow-channel area in PWR-FLECHT is about 20 times larger than it is in a reactor which means that steam drag forces on water droplets in the PWR-FLECHT upper plenum are greatly reduced and water fallback is enhanced compared to that in a reactor.

PWR-FLECHT tests were performed only at the extremes of essentially zero or 100% water fallback. Figure 205 permits comparison between the heat transfer coefficients of tests with and without water fallback. The average increase in the heat transfer coefficient attributable to water fallback is about 20%.

3.28 Flow Blockage. Cladding ballooning during an LOCA causes a reduction (blockage) in the flow channel cross-sectional area. Channel blockage was simulated in 16 PWR-FLECHT tests by placing a perforated plate at the bundle midplane. Hole sizes in the plate were varied to obtain channels which had a 50, 70, 90, and 100% reduction in flow area. As shown in Figure 206, the effect of the blockage plate was to increase the heat transfer coefficients at the midplane. High speed movies taken through a window at the midplane indicate that the reason for increased heat transfer coefficients is that the entrained water droplets are broken up by the plate. More efficient utilization of the entrained water results because the coolant surface area is increased when the droplets are broken up.

FIG 205 EFFECT OF WATER FALLBACK ON HEAT TRANSFER COEFFICIENT.

FIG. 206 EFFECT OF FLOW BLOCKAGE ON HEAT TRANSFER COEFFICIENT.

Obviously, results from PWR-FLECHT channel flow blockage require interpretation and qualification when applied to PWR cores. The flooding rate into the blocked regions of the core will be reduced in proportion to the relative pressure drops in blocked and unblocked flow paths.

3.29 Borated Water. One test was performed with borated water to check the influence of soluble poison. Some improvement in cooling was noted. The temperature rise was reduced through use of borated coolant from 85 to 60°F and the time to quenching initiation was reduced from 70 to 65 seconds.

3.210 Bundle Size. Both 49- and 100-rod bundles were tested to obtain an indication of the effect of test bundle size on test results. Such an indication could aid in determining whether or not data taken from small-bundle tests are applicable to large reactor cores or large segments of cores. The use of 10 x 10 bundle instead of a 7 x 7 rod bundle required only a slightly larger housing and only increased the separation of the central rods and bundle housing by one-half to one inch. Prior to flooding, initial housing temperatures were set such that the initial stored energy in the housing was equal to the stored plus generated energy in the rods during the test. Unfortunately during most tests, inaccuracies in setting housing temperatures masked the effects of bundle size for the otherwise similar tests. One 10 x 10 bundle test did have housing temperature profiles similar to those for a 7 x 7 bundle test; heat transfer coefficients for these two tests agreed within about 10%.

3.3 Zircaloy-Clad Bundle Tests

Four tests were performed with zircaloy-clad rods. The first two tests employed rods with Nichrome heating elements and were conducted from an initial temperature of 2000°F. Midplane temperature rises of 65 and 135°F were obtained at 10 and 4 in./sec flooding rates. Figure 194 indicates that temperature rises of 30 and 72°F would be expected from comparable stainless steel-clad tests. Cladding swelling occurred on a few rods during these tests as was evidenced by irregular quenching behavior indicated by thermocouples.

The third and fourth tests used rods with Kanthal heating elements. Outgassing the boron nitride (heater rod internal insulation material) at 2200°F successfully eliminated irregular quenching behavior indicative of cladding swelling noted in the earlier tests. In the third test (Test 8874, nominal initial temperature of 2325°F, flow rate at 6 in./sec for 8 sec then 1 in./sec) an unexplained rapid cooling rate was observed.

Figure 207 presents a composite of transient cladding temperatures for zircaloy-clad bundle Test 8874, stainless steel-clad bundle Test 8975, and a prediction of Test 8874. The prediction was based on results of lower temperature (2000 to 2200°F) stainless steel-clad bundle tests with constant flow and the assumption that the metal-water reaction energy was zero. The temperature traces were normalized to an initial temperature of 2260°F to facilitate comparison.

A peak temperature of about 2400°F was predicted for zircaloy Test 8874. The actual temperature trace exhibits a maximum of 2070°F. Figure 207 shows that Rod 2D in stainless steel-clad bundle Test 8975 did not exhibit the rapid cooling exhibited by Rod 2D in the zircaloy-clad bundle Test 8874.

FIG. 207 RELATIVE CLADDING THERMAL RESPONSE OF STAINLESS STEEL-AND ZIRCALOY-CLAD BUNDLES -- ZIRCALOY TEST (TEST 8874).

Rod 2D in the stainless steel-clad bundle test had a maximum temperature more than 100°F higher than that of the zircaloy test in spite of the nearly identical housing temperatures. Furthermore, data from Rod 2E in the test employing the stainless steel-clad rods agree closely with predictions based on lower temperatures. Therefore, the conclusion was reached that the unexpected cooling of zircaloy bundle Test 8874 is not attributable to initial rod and housing temperatures.

The fourth zircaloy bundle test, Test 9573, was conducted at initial cladding temperatures of 2000°F and a flooding rate of 1 in./sec. Cladding temperatures were predicted to reach 2400°F at about 30 sec at which time rod failures were expected to occur. Rod failures occurred starting at 18.2 sec; fifteen rods had failed by 30 sec; all but five of 42 heated rods had failed by the time the power was shut off at 58 sec. Cladding temperatures continued to rise until the time of power termination. The peak recorded cladding temperature was 2585°F following power termination. Despite gross rod failure, total bundle power was maintained due to electrical arcing.

The cladding temperature behavior for a central rod (Rod 3D) after flooding in zircaloy Test 9573 is presented in Figure 208 along with results from a stainless steel-clad bundle test of essentially identical conditions (Test 6453) and a prediction for the zircaloy bundle test. The temperature rises for the zircaloy cladding are higher than for stainless steel cladding. The rapid cladding temperature drops exhibited in the third zircaloy bundle test did not occur in this test.

The prediction of zircaloy Test 9573 was made using heat transfer coefficients obtained from the stainless steel cladding test and assumes metal-water reaction energy to be 0.75 times the Baker-Just^[120] prediction. As is apparent in Figure 208, the experimental results are not overpredicted; a factor of 0.75 times the Baker-Just prediction appears to be nonconservative.

Five Pt-13% Rh thermocouples placed on external cladding surfaces failed at about 2200°F. This fact, plus the limitations of internal Chromel-Alumel thermocouples, makes data above about 2450°F questionable. Furthermore, because of the rod failures, data beyond 18 sec require care in interpretation.

3.4 PWR-FLECHT -- Summary and Comments

Analyses to date of data from PWR-FLECHT core reflooding tests show that factors which increase the heat transfer coefficient at bundle midplane are:

- (1) Higher initial cladding temperature
- (2) Higher core reflooding rate
- (3) Lower initial power level
- (4) Higher flow channel pressure
- (5) Greater water subcooling
- (6) Water fallback
- (7) Borated coolant.

Analysis to date indicates that the effects of bundle size (49-rod versus 100-rod bundles) or of small differences in power decay are not important.

FIG. 208 RELATIVE CLADDING THERMAL RESPONSE OF STAINLESS STEEL- AND ZIRCALOY-CLAD BUNDLES -- FOURTH ZIRCALOY TEST (TEST 9573).

Comparison of test results for the stainless steel-clad bundles and zircaloy-clad bundles is expected to continue during 1971. Until this effort is completed the degree of applicability of results presented here to PWR safety evaluations cannot be firmly established. However, the need for additional zircaloy-bundle data is apparent to facilitate prediction of reactor thermal response at cladding temperatures above 2400°F. Certain accident conditions can be postulated which would produce cladding temperatures of 2400°F or higher at upper reactor core elevations (six or eight feet). At temperatures above about 2400°F, chemical reaction of zircaloy and water becomes significant. Removal of the metal-water reaction energy extends the time to quenching and thus delays achieving the improvements in heat transfer evident when rods begin to quench. FLECHT-type high-temperature investigations are currently restricted by the lack of an electrical heater design capable of maintaining integrity of temperatures significantly above 2400°F. A higher temperature design should employ a heating element material with relatively constant resistivity versus temperature characteristics in order to avoid axial power shifting.

4. SCOPING EMERGENCY COOLING HEAT TRANSFER (SECHT) PROJECT

(C. M. Moser and J. L. Plum)

The SECHT Project was an experimental investigation directed at scoping the parameters of bottom and top flooding modes of emergency core cooling (ECC)[121-124]. The investigation was conducted using electric heater rods to simulate the fuel rods of a power reactor. The project investigated that portion of a loss-of-coolant accident (LOCA) from the time of emergency coolant injection to the time at which the rod temperature increase was arrested and rod temperatures were reduced to the saturation temperature. The data were used in planning the FLECHT Projects.

The SECHT Project included tests with three-foot stainless steel-clad bundles (SECHT I and II) and comparable tests of stainless steel-clad six-foot heater rods (SECHT III tests) and zircaloy-2-clad six-foot heater rods (SECHT IV tests). Thus, the project provided information for evaluating the thermal behavior of zircaloy-2-clad heaters compared to that of stainless steel-clad heaters and for determining the behavior of zircaloy-2 cladding subjected to flooding at elevated temperature. In order to evaluate the relative response characteristics of the respective heaters, SECHT IV test conditions were established to be consistent with those of selected tests from SECHT III. Since the respective heaters were operated at comparable test conditions, turnaround temperature and turnaround time at any axial location were considered to be the variables most meaningful for evaluating the thermal response of the respective heaters.

4.1 Test Equipment

The test apparatus consisted of an insulated test vessel, a flood reservoir above the vessel, a remotely controlled pneumatic flooding valve, throttling valves, a flooding and recirculation pump, coolant preheater and temperature controller, heater rods, and test instrumentation and controls.

The test vessel consisted of two identical sections of four-foot-long, six-inch, Type 304, Schedule 40 stainless steel pipe. The vessel was open at the top to allow steam and water to be discharged to the atmosphere after traversing two 90 degree turns. The vessel top was fitted with an adjustable annular ring used to adjust, or shut off, coolant flow from the vessel. The vessel contained eight viewing ports through which high speed cameras recorded events within the vessel.

Two groups of electric heater rods were used. The heater rods were 0.58-inch diameter, commercial Watlow "Firerods" rated for 4000 watts at 220 volts. The heaters contained a magnesium oxide (MgO) electrical insulator surrounding two Nichrome center conductors and multiple Nichrome heater elements wired in parallel. The heated portion of the rod was six feet long. The 37 rods of the bundle for SECHT III tests were clad with stainless steel, and the 37 rods of the bundle for SECHT IV tests were clad with zircaloy-2. Four inner grids in the test vessel provided lateral support for the electric heater rods. Each bundle of rods was arranged in a square matrix with 0.75-inch center-to-center spacing.

The surface temperature of the heater rods was monitored by 68 spot-welded thermocouples positioned at elevations of 0, 9, 18, 27, 36, 45, 54, 63, and 72 inches, relative to the bottom of the heated part of the rods.

4.2 Experimental Conditions

The initial temperatures of the heater rod cladding were limited to between 600 and 2000°F for SECHT III tests, and less than or equal to 1800°F for SECHT IV tests. Other parameters for the two test series are listed in Tables XXXII and XXXIII.

4.3 Experimental Results

A typical response to bottom flooding is given in Figure 209; specific results relating to a constricted vessel, liquid entrainment, turnaround and quenching times, temperatures, and top flooding are presented in Sections 4.31 through 4.36. Section 4.37 compares the results from stainless steel-clad heater rods with those from zircaloy-clad heater rods, and Section 4.38 discuss expansion of the zircaloy-2 cladding.

TABLE XXXII

SECHT III EXPERIMENTAL PARAMETER LIMITS

Parameter	Operation Limits
Heater Heat Flux at Flooding	0 to 15,000 Btu/hr-ft ²
Cladding Temperature	600 to 2000°F
Coolant Temperature	100 or 200°F
Coolant Cold Flooding Rate	0.45 to 7.2 in./sec
Coolant Entry Mode	Bottom and Top Flooding

TABLE XXXIII

SECHT IV CONSTANT EXPERIMENTAL PARAMETERS

Parameter	Set Condition
Heater Heat Flux at Flooding	15,000 Btu/hr-ft ²
Cladding Temperature	1800°F
Coolant Temperature	100°F
Coolant Cold Flooding Rate	1.8 in./sec
Coolant Entry Mode	Bottom Flooding

FIG. 209 TYPICAL PROFILES OF TEMPERATURE VERSUS TIME AFTER FLOODING.

Several characteristics of the cladding temperature response are evident in Figure 209. First, since the thermocouples were attached externally to the rod surface, coolant impinging unevenly on the thermocouple junction probably caused the fluctuations in temperature. Second, all the traces show an abrupt change in slope shortly after initiation of cooling. This change in slope (Point A in Figure 209) defines the turnaround time for the temperature transient. At this time, sufficient coolant has arrived at the location of the thermocouple to generate a net heat flux outward from the cladding surface. Third, after a long period of monotonically decreasing temperature, the thermocouple trace exhibits another abrupt change in slope (Point B in Figure 209) corresponding to the quenching time of the rod, at which a transition is made from transitional or partial film boiling to nucleate boiling. Quenching usually occurred at about 800°F and seemed to be rather unpredictable from the standpoint of time for the same location and experimental conditions. Data from tests using internal thermocouples indicate that quenching occurs at temperatures 200 to 400°F higher than indicated by external thermocouples, but that quenching occurs at about the same time in both cases. Fourth, the quenching for thermocouple D-3-27 was irregular, and appeared to be caused by coolant behavior in the test setup at the 27-inch elevation point.

4.31 Constricted-Vessel Results. Cladding temperature behavior with a constriction over the top of the test vessel is illustrated in Figure 210. The

FIG. 210 SURFACE TEMPERATURE VERSUS TIME AFTER FLOODING BEGINS FOR CONSTRICTED VESSEL.

temperature of the rod cladding appears to drop at a higher rate immediately following turnaround for the constricted case than for the unconstricted case; a temperature drop of approximately 100°F occurred after turnaround for the unconstricted cases, as compared to approximately 200°F for the constricted case. (For locations on the inner cladding surface, the initial temperature drop may not be as sudden as indicated in Figure 210.)

4.32 Liquid Entrainment. That quenching time for the four thermocouple locations illustrated in Figure 209 increases with elevation from the rod bottom is evident. However, the time to arrest the temperature transients for the four elevations shown is nearly equal, implying that a heat transfer coefficient of sufficiently large magnitude occurs over the full length of the rod. Highspeed motion pictures taken through viewing ports in the test vessel show water droplets entrained in the steam flowing up through the test vessel.

To determine the magnitude of liquid entrainment, an experiment was conducted in which the liquid carryover was measured as a function of time. For a given set of test conditions (initial temperature of 1600°F, flooding rate of 3.6 in./sec, and heat flux of 15,000 Btu/hr-ft²) the first entrained liquid appeared approximately 4.5 seconds after the liquid level reached the bottom of the heated portion of the fuel pin simulators. At about 40 seconds, the carryover rate stabilized at 67% of the flooding rate.

4.33 Turnaround Time. The data in Figure 211 show temperature turnaround times as a function of initial temperature. For each initial temperature, some asymptotic value of turnaround time was achieved. The higher the initial temperature, the sooner this asymptotic value was reached. Thus, for a given flooding rate and bundle power, the higher the initial temperature, the sooner the temperature rise of the full length of the rods was terminated, which can

FIG. 211 TIME TO TERMINATE THE TEMPERATURE TRANSIENT AS A FUNCTION OF AXIAL POSITION AND INITIAL BUNDLE TEMPERATURE.

be attributed to a low liquid level and sufficient vapor velocity up through the core for liquid entrainment.

The data in Figure 212 indicate the sensitivity of turnaround time to bundle power level.

The effect on turnaround time of placing a constrictive device over the exit plenum of the SECHT vessel is shown in Figure 213. The net effect of constricting the exit was to decrease turnaround times for the 1.8 in./sec flooding rate, and to increase turnaround times for the 3.6 and 7.2 in./sec flooding rates.

The constriction raised the average pressure in the vessel, and thus the density of the steam, and lowered the mass flow rate out of the bundle, and thus lowered the average steam velocity up through the vessel. The maximum pressure difference recorded during a test with no constriction was 3.8 psid, and for a test with a constriction the maximum pressure difference was 8.8 psid.

The differences between the test data for the constricted and unconstricted vessel are smaller for the higher initial temperature of 1600°F. However, the differences at the low flooding rate of 1.8 in./sec are larger at the higher initial

FIG. 212 TIME TO TERMINATE THE TEMPERATURE TRANSIENT AS A FUNCTION OF AXIAL POSITION AND POWER DENSITY.

FIG. 213 TIME TO TERMINATE THE TEMPERATURE TRANSIENT -- CONSTRICTED AND UNCONSTRICTED VESSEL.

bundle temperature. The increase in convective heat transfer and entrainment capability for the low flooding rate helps generate conditions for turnaround; whereas the relative decrease in mass flux through the bundle for the high flooding rates generates conditions which retard turnaround.

4.34 Quenching Time. Differences in quenching times for the same elevation but at different radial positions in the bundle were observed, and are due to the random water entrainment and subsequent cooling.

For the 1.8 in./sec flooding rate at the three initial bundle temperatures of 800, 1200, and 1600°F, the rate that the quenching front moves up the core decreases as the initial temperatures increases. The higher the initial temperature, the larger the amount of stored energy in the rod that must be removed before the surface temperature can be reduced to approximately 800°F.

The effect of the constriction on quenching time is similar to that for turnaround time. At the 1.8 in./sec flooding rate, quenching occurred earlier for the constricted experiment than for the open vessel. At the higher flooding rate of 7.2 in./sec, the effect of constriction was to increase the time to quenching.

4.35 Temperature Profiles. To obtain the axial temperature profiles shown in Figure 214 several thermocouples at each elevation were used wherever possible. The axial temperature profiles show the quenching front as it progresses up the bundle, and the subsequent effect on the temperature redistribution as time progresses.

The radial temperature profiles in Figure 215 were taken at the mid-elevation of the bundle. At time zero, a definite gradient with the lower temperatures on the outer row of rods can be seen. The gradient existed initially because of radiated heat loss to the cooler vessel walls during heatup. As time progresses, this radial gradient becomes more pronounced, the outer course of rods cooling much more quickly than the center region. Some channeling of the coolant toward the colder vessel wall may have been the cause for the preferential cooling that the radial profiles indicate. Consequently, in comparisons for stainless-steel- and zircaloy-2-clad rods, only data from the inner rods were used.

4.36 Top Flooding. Examination of SECHT III data shows that top flooding is more effective in removing stored heat in the upper one-third of the bundle, and bottom flooding is more effective in removing stored heat in the lower two-thirds of the bundle, as measured by quenching time. Quenching occurred earlier for both cases when the vessel top was constricted, although the time reduction was greater for bottom flooding.

FIG. 214 AXIAL TEMPERATURE PROFILES AS FUNCTIONS OF TIME FOR CENTER OF BUNDLE.

FIG. 215 RADIAL TEMPERATURE PROFILES AT MIDELEVATION AS FUNCTIONS OF TIME.

4.37 Stainless Steel Compared to Zircaloy-2. SECHT IV tests were run at conditions comparable to selected SECHT III tests. In each case, the initial cladding temperature for SECHT IV tests was controlled at the 45-inch elevation so as to duplicate the initial cladding temperature of the selected SECHT III test at the 45-inch elevation. However, because of differences in the physical characteristics of the heaters, the average bundle temperatures for SECHT IV tests at each elevation were lower than for the comparable SECHT III test. Consequently, the SECHT IV temperature data were normalized to SECHT III data in order to correct for the effect of physical differences in the two heaters. The procedure for normalizing the temperature data was as follows:

- (1) For each elevation, the average initial temperature for SECHT IV was subtracted from that of SECHT III. (Zircaloy-2-clad rods tended to operate cooler.)
- (2) The difference in the average initial temperature at any elevation was added to the average initial, turnaround, and quenching temperatures measured for SECHT IV. This procedure provided SECHT IV normalized temperature profiles

that could be compared with the temperature profiles of SECHT III.

The typical thermal response of stainless-steel- and zircaloy-2-clad heaters is illustrated in Figure 216. For comparable test conditions, characteristics of the temperature turnaround times for SECHT IV were quite similar to SECHT III results. In both cases the time decreased as a function of increasing initial cladding temperature, and the average turnaround times as a function of length were approximately constant above the 36-inch elevation. However, the SECHT IV turnaround time tended to be about two seconds less due to the approximate 25% less heat storage capacity of the zircaloy-2 cladding.

4.38 Cladding Expansion. For the zircaloy-2-clad heater rods used, permanent expansion of the cladding was noted after high temperature tests (1800°F). This expansion is believed to be caused by differing coefficients of thermal expansion for the rod materials. The coefficient for magnesium oxide is lower than that of stainless steel, but is a factor of approximately two greater than that for zircaloy-2. Thus, the stainless steel cladding would tend to expand and contract with the MgO electric insulator of the heater as the temperature changes. However, the zircaloy would tend to expand considerably during heatup and then be cooled faster than the dielectric so that it remained in this expanded state. Sectioning of some heaters after high-temperature operation showed that an annulus existed between the MgO electric insulator and the zircaloy-2 cladding.

4.4 Conclusions

The specific conclusions derived from this project are as follows:

- (1) Bottom flooding is an adequate technique for cooling electrically simulated nuclear fuel rods in small arrays. The effect of radial flow could not be evaluated in the small SECHT array.

FIG. 216 CLADDING TURNAROUND TEMPERATURE AS A FUNCTION OF LENGTH -- STAINLESS STEEL AND ZIRCALOY.

- (2) At all initial bundle temperatures investigated, the temperature transient in the bundle was arrested or turned around at elevations of nine inches or higher, before the liquid level would have reached that elevation had the bundle been cold. The cause for this phenomenon was shown to be liquid entrainment in the steam flow generated by the heated rods.
- (3) For the temperature range investigated, the time to terminate the temperature transient was found to be inversely proportional to the initial bundle temperature (that is, the higher the initial bundle temperature, the shorter the time to terminate the temperature transient for any given elevation).
- (4) For a given cold flooding rate, the higher the initial bundle temperature, the smaller the effect of bundle power density on turnaround times.
- (5) The effect of placing a constriction over the exit of the test vessel was to aid cooling at a low flooding rate and to retard cooling at a high flooding rate.
- (6) The stainless steel-clad and zircaloy-clad heaters exhibited similar thermal behavior for the same test conditions up to 1800°F. Differences that existed in thermal behavior were due to inherent physical characteristics of the respective heaters.
- (7) The magnitudes of the axial turnaround and quenching temperatures for stainless steel-clad heaters were equivalent to those achieved with zircaloy-clad heaters subjected to the same test condition.
- (8) The major difference in behavior was associated with temperature turnaround times. An appropriate correction factor may be required under certain test sequence conditions in order to apply temperature-turnaround-time data from stainless steel cladding to zircaloy cladding.
- (9) The turnaround time for zircaloy-clad heaters decreased in tests performed after an elevated temperature test because of permanent expansion of the cladding. Comparable tests using stainless steel-clad and zircaloy-clad heaters that are otherwise identical require a test sequence for zircaloy-clad heaters that proceeds sequentially from the lowest to highest test temperature of interest. At any particular temperature, consecutive tests can be conducted without the introduction of error due to changes in physical characteristics of the heater rods.

VIII. INCIPIENT FAILURE DETECTION BY ACOUSTIC EMISSION

Acoustic signals are known to be generated in metals under stress. Although these signals emanate from both flawed and unflawed materials, those signals emanating from flawed materials have certain characteristics that are indicative of material flaw growth and are thus distinguishable from those signals emanating from unflawed materials.

The technology of detecting, analyzing, and interpreting acoustic emissions emanating from materials is being developed and applied as a new nondestructive testing technique for determining and monitoring the integrity of stressed structures.

The specific goal of this project has been to develop a nondestructive test system that would provide integrity analysis of the assembled reactor pressure vessel and primary pressure system components of large nuclear power plants by detecting, locating, and describing incipient failure conditions. The resulting incipient failure detection system (IFDS) is described in this section. This section also describes the testing, evaluation, and application of the IFDS.

1. PROJECT DEFINITION AND OBJECTIVES

The overall problem to which the development of the IFDS is directed, is the detection of significant incipient flaws in the primary system pressure envelope (both pressure vessel and piping) of large pressurized- and boiling-water reactors before such flaws can lead to catastrophic failure of the pressure envelope.

Information in this section is based, in part, on an AEC-sponsored review of current practices (as of 1966 when IFDS development was initiated) in design, analysis, materials, fabrication, inspection, and testing of steel pressure vessels for water-cooled nuclear reactors[125]. It is also based in part on accounts of AEC-sponsored research on pipe rupture at Battelle Memorial Institute[126, 127] and General Electric Company[128, 129].

1.1 Characterization of Incipient Flaws

Failures of load-bearing structures, such as pressure vessels and piping, can be either of the yielding-dominant or of the fracture-dominant type (fracture prior to general yielding of the section). At temperatures near or above the nilductility transition (NDT) temperature, fracture will not occur prior to general yielding unless the pressure vessel or piping contains at least one crack-like flaw of substantial size. Cracks may extend in area (propagate) when the surrounding material is under tension. The initial critical size leading to crack propagation is a function of toughness and average tensile stress and can be estimated by the methods of fracture mechanics, as substantiated by experimental data[126, 127]. Other shapes and types of flaws, such as stringers, inclusions, and holes, become significant only when in sufficient clusters to affect appreciably the resistance of the metal section to crack extension.

Depending on the size and shape characteristics of a structural component and on its stress-environment history during service use, a critical condition for crack propagation may develop gradually from fatigue, stress-corrosion (or hydrogen embrittlement), or reduction in material toughness, as by radiation damage.

The rate of growth of cracks in materials under stress is a function of several parameters, including the stress level and the size of the initial crack. Once a threshold stress is reached, slow, stable crack growth occurs in incremental steps or spurts with bursts of mechanical energy being subsequently released. This incremental growth may be explained by the crack propagating and, subsequently, being arrested by an area of plastic deformation which grows ahead of the crack tip. At a sufficiently high stress and large crack size, unstable crack growth at growth rates up to several hundred feet per second leads to fracture.

The size at which incipient flaws become significant is important in determining the required capabilities of potential detection methods. Criteria regarding permissible flaw sizes in the primary systems of water-cooled reactors have not been established. In rocket applications, materials have been selected on the basis that a through-wall crack of twice the wall thickness in length is stable at a stress equal to the yield strength. Completed rocket chambers made from such materials would generally withstand an average hoop stress in the cylindrical section nearly as large as the yield strength of the material without fracturing.

For pressure vessels and piping tested and used at a hoop stress much less than the yield strength of the material, less material toughness or greater stable crack length can be accommodated. In ductile ruptures of carbon steel piping, crack toughness measurements yielded values for k_c (critical stress intensity factor of 150 to 300 kpsi $\sqrt{\text{in.}}$ [126], and axial through-wall cracks longer than twice the wall thickness were found to be stable. At the allowable stress in the ASME piping code, through-wall cracks having lengths of the order of 10 to 20 times the wall thickness of the pipe were found to be stable. When the crack penetrated 70% or less of the wall thickness, failures did not occur at the ASME code stress levels.

These results suggest that a method capable of detecting surface cracks equal in length to the wall thickness would be quite useful in detecting flaws before they reached a critical size for uncontrolled propagation.

The project objectives and descriptions of the three project phases follow.

1.11 Phase I -- Development, Testing, and Application of IFDS Type A. The IFDS Type A system is to provide decisive integrity analysis capability for assembled nuclear power plant primary system components during hydrostatic testing conditions. Its primary use is for the detection and location of all significant flaws in the pressure vessel and primary pressure system components of nuclear plants. It is to be applicable during manufacturing and acceptance testing prior to plant installation, as well as applicable for integrity analysis of a nuclear plant primary system under plant shutdown conditions.

1.12 Phase II -- Development, Testing, and Application of IFDS Type B.

The IFDS Type B is to be applicable for the integrity analysis of the primary vessel and pressure system of an operating nuclear power plant. Under the conditions imposed by the primary system fluid flow noise of an operating plant, the system is to be capable of detecting and locating significant growing flaws.

1.13 Phase III -- Development, Testing, and Application of IFDS Type C.

The IFDS Type C is to have the additional capability of describing growing flaws originating in the pressure vessel or primary pressure system components of operating nuclear power installations. In order to describe flaws, the system is to have the capability of identifying the flaw size, orientation, and location, in addition to providing an assessment of flaw significance to the integrity of the operating plant.

2. DESCRIPTION OF ACOUSTIC EMISSION TECHNIQUE

This section describes the phenomena on which the acoustic emission technique is based, presents the principles on which the design and operation of the IFDS is based, and summarizes the performance of the acoustic system.

2.1 Description of Acoustic Phenomena

The theory of acoustic emission source origin set forth in this section has resulted in large measure from the investigations performed and the resulting information obtained in the conduct of tests. However, similar conclusions have been reached by other investigators in the field[130, 131].

When a metal is stressed, two distinct characteristic types of acoustic signals are generated: the burst signal and the deformation emission (sometimes called continuous emission). The burst signal has been found to be indicative of flaw or crack growth. The signals are generated by the elastic energy released by material fracture, by reorientation of the leading edge of the crack, and by the dislocation movement (avalanching) created in the plastic zone associated with the leading edge of every crack. The deformation emission has been attributed to the dislocation movement of material deformation.

2.2 Design and Operating Principles

This section deals with the principles of signal detection, frequency selection, amplification, and analysis.

2.21 Method of Detection. The nature of the mechanics causing the acoustic emission indicates that the emission resembles an event with a rise time in the nanosecond time domain. To build acoustic detection transducers that are sensitive only to the acoustic emission is impractical, if not impossible; therefore, the currently accepted means of detecting the emission is the use of the resonant frequency of the mounted detecting transducer. The energy content of the acoustic emission appears to be broadband in nature, as is expected of an event with a very rapid rise time. This observation is validated by the fact that detection transducers have been excited at mounted resonant frequencies from 10 kHz to 30 MHz by acoustic emissions.

The acoustic stress wave is normally detected by its compressional or shear wave component, although other, less prominent, transmission modes are known to exist. The shear wave component of the acoustic emission was chosen early in the project as the component for detection because of its sharp, distinct, leading edge. This leading edge permits more precise determinations of signal arrival times and thus more accurate location of the emission source,

The transducer used in the IFDS is a piezoelectric crystal (lead-zirconate-titanate) that is attached to the surface of the material to be tested. It is cut and mounted for use in the shear mode and thus has a high sensitivity to the shear component of the acoustic emission signal. The piezoelectric transducer normally has much higher sensitivity to material stress waves than other standard detection devices.

2.22 Selection of Frequency. The IFDS piezoelectric shear transducer has a principal resonance frequency in the 100 kHz region. This specific frequency is high enough to avoid mechanical noise interference from operating machinery, as well as signals induced by such sources as flange and bolt movement, yet low enough to avoid the high signal attenuation rates observed at the higher frequencies and most of the background electronics noise problems inherent in the high-frequency electronic systems.

In general, the frequency region from 90 to 300 kHz appears to provide a detection band which allows the detectors to be placed long distances apart because of relatively low material signal attenuation rates without encountering the mechanical noise interference under typical plant shutdown conditions.

For application to IFDS Type B and C systems, this frequency range does not, with the present state-of-the-art, circumvent the interference to acoustic signal detection encountered in an operating nuclear plant where turbulent fluid flow and cavitation generate a broadband high amplitude noise spectrum which is often higher in magnitude than the acoustic signals.

2.23 Signal Amplification. The electrical output signal from the transducer is amplified through high gain, low noise, band-pass preamplifiers, specifically designed and constructed for this function. The high signal amplification of the acoustic analysis technique requires that the electronics noise of the signal conditioning system be kept to a minimum to achieve the greatest possible signal-to-noise ratio.

The signal from the preamplifiers is additionally amplified by use of a second set of special purpose frequency band-pass amplifiers with which the necessary additional gain is obtained prior to further processing of the detected signals.

2.24 Signal Analysis. System electronic capabilities were designed to enhance signal detection sensitivities and a time analysis computer (TAC) was developed to compute and store differences in the arrival time of data from different transducers.

- (a) Time Analysis Computer. To achieve maximum accuracy, an acoustic analysis system was required to utilize all detected signals. The TAC was therefore designed to compute the differences in arrival time (Δt) of each detected acoustic signal at any three

transducers and to accumulate the resulting data for visual monitoring or computation of the emitting source location at any desired time. The TAC utilizes all detected acoustic signals, rejects invalid data points by use of coincidence techniques, and accumulates valid data points.

The TAC also reduces the effect of such problems as those due to mode conversion of the acoustic signals as they travel through the structural material. (Mode conversion produces inaccuracies in the time-of-arrival data because of the velocity changes associated with different acoustic signal transmission modes.) The TAC provides the most accurate average time-of-arrival (Δt) data derived from thousands of acoustic signals from all emitting sources.

- (b) Triangulation Computer. The time analysis computer provides processed data to a triangulation computer which calculates the geometric location of the detected sources. The triangulation computer (normally a digital multiple purpose computer) utilizes the TAC data, which are read out at the discretion of the operator, along with data concerning each transducer location on the structure under test, to calculate the geometric location of the detected flaws or cracks.

Computer programs that use hyperbolic triangulation techniques[132, 133] are applicable to acoustic sources on cylindrical vessels with and without end heads, spherical vessels, and vessels on which the transducers are located on connecting appendages.

- (c) Acoustic Signature Circuitry. In addition to the TAC and triangulation computers, the IFDS contains acoustic energy release signature circuitry which functions on a composite signal from the acoustic detection and signal conditioning channels. The circuitry displays the acoustic energy release signature on a strip chart recorder along with structural stress. The characteristics of this display, which are monitored continually during a test, yield information concerning the significance of the emitting flaws in the structure under test. The number and occurrence rate of the bursts (created by avalanches of acoustic burst signals), coupled with the increase or decrease of the total acoustic energy being released from the structure, indicate whether any monitored flaw is significant to the integrity of the structure. Figure 217 is a signature indicating a flaw (or flaws) that is growing significantly, and may endanger the integrity of the structure under test.
- (d) Data Interpretation. Data interpretation techniques have been developed and evaluated through both laboratory and field testing, and have proven to be valid for the recognition of structural flaws that represent a potential hazard to the integrity of the structure that is being evaluated. The visual display of the TAC is simultaneously monitored, along with the energy release signature, to determine the rate of accumulation of Δt data in any specific computer time region. An increase in the rate of data accumulation at specific locations in the TAC memory, coupled with an increase in the energy release signature, indicates which

FIG. 217 ACOUSTIC ENERGY RELEASE SIGNATURE SHOWING SIGNIFICANT FLAW GROWTH.

flaw (or flaws) is significant to the structural integrity. The readout of the specific Δt data and subsequent computation by the triangulation computer, geometrically locates the source.

3. TEST PROGRAM AND RESULTS

The test program consisted of the following tests: (a) feasibility investigations on the PM-2A reactor vessel, (b) laboratory tests on tensile specimens and small pressure vessels, (c) engineering-scale tests on pressure vessels at the National Reactor Testing Station, and (d) in-plant tests of complex chemical and nuclear pressure vessels at various manufacturing and operating facilities.

3.1 PM-2A Feasibility Tests

The initial phase of the acoustic test program involved feasibility investigations to determine whether the acoustic signals generated by stress-induced flaw growth in a complex nuclear reactor pressure vessel could be detected and utilized for flaw location. For these tests, a simple stress wave detection, signal conditioning, and recording system was assembled. This system was applied to the PM-2A reactor pressure vessel undergoing embrittlement studies at the National Reactor Testing Station. The test program involved the hydrostatic stressing of the vessel under conditions of varying temperature and flaw size.

The conclusions from the PM-2A feasibility test results were as follows:

- (1) Acoustic signals emanating from the flaw region of a complex pressure vessel can be detected
- (2) The burst signals allow location of the flaw through triangulation techniques
- (3) The detection threshold for the burst-type signal was 0.1 to 30% of normal vessel operating stress and 0.02 to 5% of the nominal material yield stress.

3.2 Laboratory Tests

After completion of the feasibility studies, a laboratory-scale program was directed at development, fabrication, and testing of an acoustics data acquisition system capable of detecting and recording all acoustic emissions emanating from the test specimen.

3.21 General. The data analysis system was constructed to display the acoustic data in a form that could be easily analyzed. Emphasis was placed on the development of the capability for interpreting the display data for flaw detection, location, and possible metal condition indications.

The tests were implemented with a series of tensile specimens and small pressure vessels or jugs. Test specimens, both with and without flaws, were stressed. One disadvantage of using tensile specimens was that noise interference was induced by the grips of the tensile specimen machine and by misalignment of the specimen in the holder. These tests were conducted essentially to obtain information concerning the origin of the burst signal and the deformation emission. A typical tensile specimen is shown in Figure 218 and a typical pressure vessel is shown in Figure 219. The testing of jugs in the small hydrostatic loop shown in Figure 220 circumvented the noise interference problem encountered in testing tensile specimens. In one series of tests, the jugs were conditioned to various degrees of annealing and embrittlement. The resulting data were analyzed for acoustic energy release and acoustic signature parameters that could indicate metal condition. These tests indicated differences in the total acoustic energy release from the annealed and embrittled vessels and confirmed the potential of acoustic emission data for metal condition analysis. Figure 221 shows the acoustic signatures derived from tests of identical vessels in the annealed and embrittled state.

FIG. 218 TENSILE SPECIMEN USED FOR LABORATORY TESTS.

FIG. 219 SMALL PRESSURE VESSEL USED FOR LABORATORY TESTS.

Both detectors and instrumentation were modified during the test series to achieve a system with high sensitivity and high signal-to-noise ratio. When the total acoustic signal was utilized, the acoustic energy release signature was found to be statistically more significant than when only the burst signals were used.

FIG. 220 SMALL HYDROSTATIC TEST LOOP FOR LABORATORY TESTS.

FIG. 221 ACOUSTIC SIGNATURES FOR ANNEALED AND EMBRITTLED TEST SPECIMENS (AS-212-STEEL).

3.22 LOFT Support. A series of special tensile specimen tests was conducted in support of the LOFT Integral Test Program[1].

Thirteen tensile specimens of Type A-36 steel, as shown in Figure 222, were hydraulically stressed while instrumented with four channels of the IFDS. The objective of these tests was to determine the stress level at which material cracking is initiated.

The test series showed that the IFDS could detect one-micron voids forming around material inclusions and could identify the stress levels at which ductile rupture was initiated and terminated. Figure 223 is an acoustic energy release signature showing at Point A the initiation of void formation around the material inclusions and the initiation and termination of ductile rupture at Points B and C and also at Points D and E. The IFDS also provided source location information accurate to within 0.1 inch.

These tests provided an indication of the degree of sensitivity that can be obtained by use of acoustic emission technology under controlled conditions. Although the effect of specimen size on detection sensitivity has not been completely defined, the high sensitivity shown in these tests is expected to be attainable in tests of small vessels and structures, in which signal transmission distances from source to transducer are less than three feet.

FIG. 222 TYPE A-36 LOFT TENSILE SPECIMEN WITH PIEZOELECTRIC ACOUSTIC TRANSDUCERS.

FIG. 223 ACOUSTIC ENERGY RELEASE SIGNATURE FOR LOFT TYPE A-36 STEEL TENSILE SPECIMEN.

3.3 Engineering Tests

Engineering-scale tests were needed to obtain data pertaining to signal mode conversion and attenuation suffered by acoustic signals as they travel through structural material. These tests were needed for the development and assessment of the capability of the IFDS for multiple flaw detection and location. Information was required on the sensitivity and resolution necessary for the IFDS to be usable for crack detection and location in large structures. Engineering-scale tests were also needed for the development of criteria both for optimum transducer location and for data interpretation.

3.31 Facilities and Equipment. On completion of the SM-1A tests described in Section VIII-3.4, the acoustic system was modified and expanded. Special solid state amplifiers with high signal-to-noise ratio, good stability, and high-gain capability were added. The on-line time analysis computer (TAC) for automatic reduction of data was designed and constructed. A computer program for the automatic location of acoustic emission sources was implemented.

The Engineering Scale Acoustic Test Facility was built to allow the testing of large scale vessels. This test facility contains a tensile specimen tester, a small pressure vessel test loop, a large pressure vessel test loop, a wide-band mechanical noise generator, an acoustic analysis system, and equipment for data processing and analysis.

The small pressure vessel test loop shown in Figure 220 provided the capability for stressing test specimens or small pressure vessels at variable rates as well as controlling the rate in a predetermined manner (either linear, exponential, or cyclic). The system is capable of 3000-psi pressure at ambient temperature, and with slight modification it can stress a vessel to 8000 psi. The large hydrostatic test loop, shown in Figure 224, is designed to test larger pressure vessels at pressures up to 10,000 psi over a temperature range of -20 to 300°F.

3.32 Tests with Artificial Acoustic Source -- Carbon Steel and Stainless Steel Vessels. A series of tests was conducted using an artificial acoustic source generator, or pinger, positioned at various locations on the surface of a 965-cubic-foot, carbon steel vessel having a 3/4-inch wall thickness and on a 20-cubic-foot, stainless steel vessel having a 2-inch wall thickness.

The carbon steel vessel was selected specifically for the development of the source location computer codes and as a test vehicle for evaluation of the TAC. Transducers were arranged in various configurations on the vessel surface to study the effect of transducer location on the IFDS source location accuracy.

The pinger-generated acoustic burst signals contained several acoustic signal transmission modes useful for development and evaluation of the acoustic system, TAC, and associated source location computer codes.

FIG. 224 DIAGRAM OF LARGE PRESSURE VESSEL HYDROSTATIC TEST LOOP.

On completion of these tests, a hydrostatic test of the carbon steel vessel was conducted. This hydrostatic test resulted in the detection and location of 15 previously unidentified acoustic sources in the vessel. The majority of the sources were identified as weld defects by the use of other nondestructive test techniques. The IFDS also detected and located one known flaw which had been created in the vessel by cyclic fatigue processes.

The tests with the 20-cubic-foot stainless steel pressure vessel, shown in Figure 225, were conducted in the same manner as those with the carbon steel vessel. These tests were directed at determining the acoustic signal transmission characteristics of stainless steel. The test series was concluded with a hydrostatic test of the vessel. During this test, three cracks were detected and located in vessel weld regions.

No significant difference between carbon steel and stainless steel was noted by the IFDS in the generation or transmission of acoustic signals. Both the tests of the carbon steel vessel and those of the stainless steel vessel indicated that the IFDS, with associated TAC and source location computer programs, could locate structural flaws to within a few inches.

3.33 Detection of Multiple Flaws. A group of vessels, one of which is shown in Figure 226, was constructed from three-foot sections of five-inch diameter Schedule 80, carbon steel pipe to determine the crack location capability of the IFDS when the vessel under test contained large numbers of emitting sources. End caps were attached to the vessel with intentionally poor welds to create a multitude of acoustic signal sources; in addition, a flaw was created in each vessel by using cyclic stress and hydrogen embrittlement techniques.

The tests confirmed that the IFDS could locate a crack on a vessel of this size to within one-half of an inch despite the interaction of many acoustic signal generating sources. Failure signatures were also obtained for cracks that penetrated through the wall thickness, thereby creating leaks. These signatures later aided in identifying a leak source in a large pressure vessel undergoing field acoustic evaluation (Section VIII-3.4).

3.34 Detection of Failure in Support Structures. A test with the dual purpose of (a) evaluating a simplified computer code for locating emission sources on a spherical structure and (b) obtaining acoustic energy release signatures on intentionally created cracks in vessel walls was conducted on a two-inch-thick, three-foot-diameter, carbon steel sphere. Figure 227 shows the vessel used in this test, and Figure 228 shows the acoustic energy release signature created in the initial hydrostatic test of the vessel. The rapid increase in slope in the signatures shows the typical signature of a moving crack; however, the series of rollovers noted at A, B, C, and D was not typical of any failure signatures observed up to that time.

During the performance of this test, the TAC indicated that several cracks were present in the vessel, and were located within a few inches of each other. The area of cracking was located through use of the TAC information and the resulting digital computer location data. Subsequent visual examination of the area indicated that the welds holding the 3/4-inch-thick identification plate shown in Figure 229 to the vessel wall had been cracking. As the vessel expanded

FIG. 225 STAINLESS STEEL TEST VESSEL.

slightly during the hydrostatic test, the plate welds were stressed and the poor quality welds, which contained numerous flaws, began to crack. The energy release signature indicated that, as a crack ran through a weld and fractured, it created the small "rollover" in signature as indicated in Figure 223. As

FIG. 226 SCHEDULE 80 CARBON-STEEL-PIPE VESSEL.

FIG. 227 THREE-FOOT-DIAMETER CARBON STEEL SPHERE.

FIG. 228 ACOUSTIC ENERGY RELEASE SIGNATURE FOR THREE-FOOT-DIAMETER CARBON STEEL SPHERE.

the first weld (the corner weld) fractured, the stress was shifted to another weld which fractured thereby shifting the stress concentration to other welds. When sufficient stress had been relieved from the plate because of weld fracture, the acoustic signature showed an acoustic energy decrease.

This test produced information concerning the failure of external structures attached to vessels. This information was used subsequently in identifying cracks in the support structure of a large chemical pressure vessel undergoing field testing (Section VIII-3.4).

3.4 In-Plant Tests

As discussed previously, the application of the acoustic analysis technique to operating nuclear power installations presents a variety of problems imposed by such environmental conditions as unpredicted mechanical and electrical noise, radiation, heat, and the usual inaccessibility of a typical reactor primary loop.

In-plant field tests of the IFDS, in conjunction with engineering-scale acoustic test series, were required to fully evaluate the system. The in-plant tests had the overall objective of testing, evaluating, modifying, and improving IFDS circuitry, application techniques, and data interpretation criteria.

3.41 SM-1A Tests. In 1967, the IFDS was tested on the pressure vessel of the SM-1A PWR reactor plant at Fort Greeley, Alaska during an in-place annealing heat treatment. This heat treatment was to provide significant recovery of notch

FIG. 229 IDENTIFICATION PLATE ON THREE-FOOT-DIAMETER CARBON STEEL SPHERE.

ductility, lost through radiation damage. The vessel was treated for about one week at about 600°F with the reactor core as the primary source of heat. The objectives of applying the acoustic system to the SM-1A plant were threefold:

- (1) To evaluate the feasibility of using the acoustic phenomena under the environmental conditions imposed by an operative nuclear power installation
- (2) To evaluate in-plant system installation techniques
- (3) To evaluate the feasibility of determining the change in embrittlement of the primary pressure vessel by means of the recorded acoustic data.

The data accumulated during the preanneal and postanneal hydrostatic tests included clean, clear signals of both the low-amplitude deformation emission and the high-amplitude burst emission. However, the reactor system operational noise interfered with acoustic data acquisition when primary system fluid flow was initiated. Accordingly, acoustic data could not be accumulated during the annealing phase with the SM-1A primary pumps operating.

3.42 Elk River Tests. In August of 1968, the IFDS was applied to the detection and location of leaks in the primary system of the Elk River nuclear power plant. These tests have been reported in greater detail elsewhere^[134]. The Elk River reactor vessel is a stainless steel-clad carbon steel vessel about 25 feet in height with an inside diameter of about seven feet. The wall thickness of the vessel is three inches. The vessel has two steam outlet lines and two feed water return lines.

Following scoping laboratory tests, 21 transducers were installed on the primary system piping of the Elk River nuclear power plant.

The acoustic leak detection tests conducted on the primary system of the Elk River nuclear power plant located the leaking cracks previously found by helium leak detection techniques. A leak had been detected in the Elk River nuclear power plant by the primary leak monitoring system (a radioactive sampling system) during a shutdown period in June of 1968. The acoustic system (IFDS) utilized the signals generated by the two-phase flow; that is, those signals generated by steam void formation and collapse in the crack interior.

3.43 Wyatt Tests. In May 1969, ESSO Research and Engineering Corporation made available for acoustic analysis a thick-walled carbon steel vessel manufactured by Wyatt Industries. This vessel, shown in Figure 230 was the first to be evaluated in a series of field tests of the IFDS jointly sponsored by Idaho Nuclear Corporation and industry. The IFDS was applied to this vessel during the hydrostatic acceptance test.

The programmatic objective of the test was to evaluate the IFDS performance on a newly constructed thick-walled pressure vessel.

The Wyatt vessel was a high-pressure steel vessel that measured approximately 4 feet 4 inches in diameter, about 17 feet in overall length, and had a thickness of 4 inches. This vessel had nine penetrations, one of which had an outside diameter of 3 feet 4 inches.

FIG. 230 WYATT INDUSTRIES VESSEL.

Eighteen acoustic channels were used to monitor the vessel and its appendages.

The accumulation of acoustic data was severely hampered during the test by fluid flow noise from the pressurization line. The pressurizing fluid was pumped into the vessel through a one-fourth-inch line by an air-driven piston pump. A line restriction resulted in flow noise which was picked up by the acoustic system as wide-band noise pulsations. The amplitude of these pulsations was sufficiently great to prevent efficient use of the TAC.

Due to the flow noise interference, posttest analysis of the acoustic data was performed by individual burst evaluation involving data slowdown for Δt measurements. The acoustic signals detected were generally from weld regions; however, their low emission rate indicated a vessel of high integrity.

As a result of this test, acoustic testing procedures were changed such that interference from the hydrostatic pressurizing hardware could be detected before the test and the interference noise eliminated. Also, as a result of this test, the IFDS preamplifiers were modified to permit mounting at any location or orientation on a steel vessel. Considerable knowledge was gained during this test concerning the prevention of interference from random transient electrical noises.

3.44 Enjay Tests. A joint test program was established with ESSO Research and Engineering Corporation following completion of the Wyatt vessel test. The joint test program provided the opportunity for evaluating the IFDS on selected large chemical pressure vessels, while at the same time providing industry a

means of preventing vessel failure during the hydrostatic acceptance test. Two vessels were provided by the Enjay Chemical Company. The vessels, one of which is shown in Figure 231, were 44-foot diameter pressure spheres constructed of 2-5/8-inch-thick, -SA 516 carbon steel. The spheres were designed

FIG. 231 ENJAY CHEMICAL 44-FOOT-DIAMETER SPHERE.

for use at 300 psig at a design temperature of 130°F. The vessels were acoustically tested during a hydrostatic test to 475 psig.

The programmatic objectives of these acoustic tests were as follows:

- (1) To determine acoustic signal degradation and attenuation through long transmission distances from the signal source to the detector
- (2) To investigate the distortion and mode conversion of acoustic signals through welds and weld interfaces
- (3) To field test and evaluate the triangulation computer code for large spherical vessels
- (4) To evaluate the IFDS signal source (flaw) location resolution on vessels of large dimensions
- (5) To develop, test, and evaluate system field application techniques
- (6) To evaluate data interpretation criteria.

The hydrostatic test of Vessel D-41 was initiated immediately following the background noise test, and acoustic data were detected with the first increment of vessel stress. The sources of these signals were random with no single source region becoming predominant, until a pressure of 160 psi was reached. At this pressure, large-amplitude, slow rise time, burst signals typical of flange and bolt movement were detected. The TAC data indicated an ill-defined region on the bottom of the vessel as being the emission source location (ill-defined due to slow rise time of the signals). The occurrence rate of these signals gradually decreased as the vessel stress increased, but never completely stopped.

Previous experience allowed identification of the signal as a vessel leak. The minute size of the leak confirmed the sensitivity of the IFDS for the detection of leaks in large pressure systems. However, this leak sensitivity can also be interpreted as a disadvantage to acoustic flaw detection in vessel integrity analysis, in that a leak signal as observed during this test can obscure acoustic flaw-generated signals.

Since significant discontinuities were not identifiable by on-line analysis, the acoustic analysis of Vessel D-41, as conducted prior to the leak, indicated the vessel to be of high integrity.

The test conditions, instrumentation, and test procedures used for the acoustic emission analysis of Vessel D-42 were essentially identical to those described for Vessel D-41, with the exception of new signal conditioning amplifiers. These amplifiers provided a narrow bandpass, a variable gain control, and an improved signal-to-noise ratio.

The hydrostatic test of Vessel D-42, was initiated upon completion of the IFDS check-out, calibration, and background noise evaluation sequence. Acoustic data were detected with the first increment of stress placed on the vessel. However, in this test the data were emitted from discrete sources on the vessel with

only a small portion being of a random nature. Sufficient data were accumulated in the TAC during the first 100 psig of stressing to allow acoustic source locations to be computed. The data from the TAC memory were punched out and fed to the Enjay IBM-360 computer. The hydrostatic test proceeded without delay because the energy release signature circuitry identified the sources as being insignificant to the integrity of the vessel.

3.45 LAGO Tests. Tests were conducted for ESSO Research and Engineering Corporation and their affiliate, LAGO Oil and Transport Company, in conjunction with the hydrostatic acceptance testing of two large chemical reactor vessels constructed by Babcock Atlantique in St. Nazaire, France. These vessels are designated as Vessel 2601 and Vessel 2602.

The major objectives of Idaho Nuclear Corporation were to demonstrate the application of acoustic technology and to evaluate the performance of the acoustic system as applied to large, thick-walled, carbon steel pressure vessels containing stainless steel weld overlay cladding, internal structures, and numerous nozzles.

The vessels were constructed of four-inch thick, A-302 Grade B carbon steel. They were internally clad with Type 347 stainless steel weld overlay, and contained internal structures welded at various levels inside the vessel.

Vessel 2602 shown in Figure 232, is 16 feet in diameter and 120 feet 6.4 inches in length. Vessel 2601 is 16 feet in diameter and 66 feet 6.4 inches in length. Both vessels have the following specifications: (a) capability to withstand a Charpy test of 45 foot-pounds at 75°F, (b) a tensile strength of 80,000

FIG. 232 LAGO VESSEL 2602.

to 100,000 psi, and (c) a yield strength of 50,000 psi. Design pressure is specified as 935 psi at 790°F. Both vessels were given 100% ultrasonic and radiographic analysis as well as a dye penetrant test.

A summary of results from the confirmation tests for Vessel 2602 is as follows:

- (1) Two sources, identified during the acoustic test as internal vessel support structure defects, were visually identified as cracks in tack welds that were used to attach internal vessel structure support legs temporarily to the vessel inner walls.
- (2) Three sources in vessel weld seams gave 30 to 60% of full scale deflection on the ultrasonic equipment with the sensitivity set at ASME specifications. These flaws were identified as weld porosity voids, 0.1 inch or less in size.
- (3) Nine sources in vessel weld seams gave 30 to 100% deflection on the ultrasonic equipment with the sensitivity set at ASME specifications plus 4 to 10 dB. These sources resulted from porosity voids less than 0.05 inch in size.
- (4) Four sources were visually identified as being located in external bracket welds.
- (5) One source was identified by ultrasonics to be a small cladding crack. Four other sources were tentatively identified by visual observation to be hairline cracks in the cladding but scheduling did not permit ultrasonic testing of these sources.
- (6) Six of the porosity voids confirmed by ultrasonic techniques were also identified on X-ray plates taken during the vessel construction. All voids were insignificant to the integrity of the vessel.

Eleven insignificant sources were identified and geometrically located by data analysis conducted during and immediately following the test of Vessel 2601.

All of the sources that were confirmed by other nondestructive testing techniques were found to be within less than four inches of the geometric location identified by the acoustic analysis test.

Only the most active sources were identified during the tests. Vessel integrity and the lack of significant flaws were established by interpretation of the acoustic data on an immediate basis as the tests proceeded. Detailed posttest analysis of the acoustic data accumulated on magnetic tape during the tests is expected to identify locations for additional insignificant sources. This information will be useful in the sensitivity and reliability evaluation of the acoustic analysis system.

3.5 Noise Analysis Tests

The long range goal of the acoustic program has been to develop an incipient failure detection system capable of continuously monitoring an operating power reactor to assess the primary system integrity.

Early in the program, tests conducted with an acoustic analysis system suggested that the broadband reactor plant noise generated by fluid flow in the primary system of a nuclear power installation may be a limiting factor in applying acoustic analysis techniques for flaw detection and location in an operating nuclear power system.

The objectives of the noise analysis tests were to: (a) measure the typical noise amplitude and spectrum of operating nuclear power plants and (b) determine whether regions occurred in the noise frequency spectra in which the amplitude of the noise was low enough to allow operation of an incipient failure detection system.

3.51 System Development. The noise analysis system, shown in Figure 233, consists of broad frequency band transducers mounted on the piping of the reactor systems, amplifiers, an active filter used to suppress lower frequency mechanical noises, and an oscilloscope with plug-in frequency spectrum analyzers. Photographic techniques and a strip chart recorder were used to record the oscilloscope display and a magnetic tape unit was used to record data up to the limit of its frequency response.

In order to interpret accurately the noise data obtained in an operating reactor plant the characteristics of the noise analysis system must be known; specifically the frequency response of the system and the temperature response characteristics of the transducers must be known.

FIG. 233 BLOCK DIAGRAM OF NOISE ANALYSIS SYSTEM.

The response characteristics and the temperature behavior were obtained by four broadband transducers, including transducers made from lead-zirconate-titanate or lead metaniobate crystals assembled with Mithra or Echo Bond 104 bonding materials. The transducers which proved most applicable and which were used for in-plant noise analysis were made from lead-zirconate-titanate assembled with Echo Bond 104.

Since mounting techniques affect transducer response, two basic types of transducer mounts were tested. The first type was a magnetic base mount with a flexible locking arm and the second type was a precision machined block mount. The first mount proved to be vulnerable to high-amplitude, low-frequency plant vibrations that resulted in constantly changing transducer position and alignment. The second mount had much more stable characteristics when subjected to low-frequency vibrations. The second mount also had the advantage of rapid alignment. This mount had a spring which was used to hold the transducer in place and a nut which permitted transducer removal without disturbing mount alignment. Figure 234 shows the block mounts and broadband transducers.

The preamplifiers, which were designed specifically for broadband noise measurements, were charge-sensitive units with an input impedance of 20 ohms and an output impedance of 50 ohms. The equivalent voltage gain of these units is 40 dB. The frequency response of these preamplifiers is flat, ± 3 dB from 10 kHz to 10 MHz.

The filter used in the system was a commercially available active filter usable from 2 kHz to 2 MHz, with an adjustable passband having a roll-off rate of 24 dB/octave.

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FIG. 234 BROADBAND NOISE ANALYSIS TRANSDUCERS AND MOUNTS.

The spectrum analyzer consisted of a Tektronix Model 556 oscilloscope with two plug-in analyzer units; a Tektronix Model 1L5 usable from dc to 1 MHz, and a Tektronix Model 1L10 usable from 1 to 36 MHz.

The results from the 1L5 plug-in analyzer were photographed by using a Tektronix Model C-12 oscilloscope camera. The results from the 1L10 plug-in analyzer were recorded on an X-Y plotter.

Analog data to 1.5 MHz were recorded directly on a Hewlett-Packard Model 3527A magnetic tape recorder for posttest evaluation.

3.52 Laboratory Evaluation. The broadband noise analysis system was subjected to several laboratory tests to determine the frequency and temperature response characteristics. Two independent means of determining frequency response were used. The electrical characteristics of the transducers were determined by using vector impedance meters. The actual frequency response of the system was determined by clamping two transducers face-to-face and driving one with an oscillator while the output from the other transducer was plotted. A final frequency response verification was achieved through use of a broadband mechanical noise generator. The resultant system frequency response curve is presented in Figure 235 and is used for the noise data evaluation. Temperature response was determined by placing the transducer on the mechanical noise generator and monitoring the system output while the transducer was heated in stages up to 600°F. The results of this evaluation, shown in Figure 236, indicate that the response is constant up to a temperature of about 400°F.

FIG. 235 SYSTEM FREQUENCY RESPONSE.

FIG. 236 TRANSDUCER TEMPERATURE RESPONSE.

3.53 In-Plant Tests. Tests were performed on Dresden I, the Engineering Test Reactor (ETR), the Advanced Test Reactor (ATR), the San Onofre Reactor, and Yankee Rowe. Data obtained from these operating nuclear power plants indicate broadband noise, which extends to, and in some instances exceeds, 1.3 MHz. These data indicate that acoustic data acquisition at frequencies up to and exceeding 1 MHz would be impossible under the plant noise conditions measured in both the San Onofre and Yankee Rowe nuclear power plants using current signal recognition techniques.

4. CONCLUSIONS

The Idaho Nuclear Corporation acoustic project has resulted in the development and evaluation of a new nondestructive test tool utilizing the phenomena of acoustic emission analysis. The acoustic system (IFDS) has been shown to be applicable to the analysis of structures ranging in size from small tensile specimens to large nuclear pressure vessels. It has analysis capabilities ranging from the detection of voids initiated around inclusions to the identification and location of significant crack growth. Acoustic emission analysis coupled with other standard nondestructive test techniques will provide a previously unattainable level of capability for the integrity analysis of large complex industrial structures.

The specific conclusions to be derived from the acoustics project as presented in the report are summarized as follows:

- (1) Through a variety of in-plant tests of complex, thick-walled pressure vessels, the acoustic emission technique, as incorporated into the incipient failure detection system, has proven to be potentially capable of rapidly detecting and locating both significant and insignificant flaws during hydrostatic acceptance and requalification testing of nuclear reactor pressure vessels.
- (2) The acoustic emission technique has proven extremely sensitive for detecting minute leaks in pressure systems.
- (3) Preliminary tests indicate that the acoustic emission technique has considerable promise for determining the state of embrittlement of in-service nuclear reactor pressure vessels.
- (4) The investigation of fluid flow noise in a nuclear power plant and the associated preliminary analysis has shown the need for further work to demonstrate the feasibility of developing analytical techniques that will allow application of the present acoustic technology to operating plants.
- (5) During the development program described in this report, satisfactory techniques and equipment have been developed to provide the following specific capabilities of flaw detection by the acoustic emission technique:
 - (a) The necessary sensitivity and resolution for detecting and locating the origin of acoustic signals to within about 1% of the vessel dimensions.
 - (b) The capability of excluding typical in-plant electrical and mechanical noise interference (with the exception of fluid cavitation noise).
 - (c) The capability of distinguishing between significant and insignificant flaws, and between flaws and other pertinent sources of acoustic emission, such as leaks and material deformation emission.
 - (d) The capability of detecting any acoustic emission source originating within the structure under test by proper system application and data analysis techniques.
 - (e) A method of producing continuous records suitable for comparison with those from future inspections.
 - (f) Timely on-line display and interpretation of data.
 - (g) The ability to compensate for complicating features of the pressure vessel, such as nozzles and other penetrations, stainless steel cladding, internal structures, large wall thicknesses, and inaccessibility of the vessel shell.

IX. IN-PLANT TESTING -- THE CAROLINAS VIRGINIA TUBE REACTOR CONTAINMENT TESTING PROJECT

The Carolinas Virginia Tube Reactor Containment Testing Project^[135] was performed by the Atomic Energy Division of Phillips Petroleum Company^[a] and the Carolinas Virginia Nuclear Power Associates, Inc. as part of the Atomic Energy Commission's safety program. The project consisted of three major series of tests:

- (1) Vibration tests by two independent techniques to determine the vibrational characteristics of the containment system
- (2) Leakage rate tests to evaluate containment leakage as a function of pressure and temperature
- (3) Steam injection tests to simulate design basis accident (DBA) conditions^[b].

The purpose of this project was to perform experiments in a representative PWR containment that would provide information applicable to the safety evaluation of power reactors. The vibration test results have been reported^[136-139]. Both preliminary and final reports covering the leakage rate testing^[140-141] and the DBA testing^[142-143] were issued. Only test descriptions and major findings from the leakage rate and DBA work are reported in Sections IX-1 and IX-2.

The CVTR vapor container, shown in Figure 237, is a reinforced concrete, right vertical cylindrical structure with a flat base and hemispherical dome, having an internal diameter of 57 feet 11-1/2 inches and an inside height of about 114 feet. The vessel has a 2-foot-thick reinforced concrete cylindrical wall that supports a 1/2-inch-thick steel dome covered by 20-1/2 inches of concrete for 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 walls are lined with 1/4-inch-thick steel plates to make the containment vessel vapor tight. The basement floor consists of 4 foot 6 inches of concrete over the steel plates.

The containment structure was provided as a safeguard against the possibility of release of fission products to the surrounding area in the event of an accident, and was designed to withstand the CVTR postulated DBA for which a containment pressure of 35 psia and a temperature of 215°F were calculated. The structure, which housed the reactor and primary system, the steam generator, and various auxiliary systems and components, has a free volume of about 227,000 ft³, and is physically divided by floors and equipment into three regions -- operating, intermediate, and basement.

[a] Now part of Idaho Nuclear Corporation.

[b] As used in this report, DBA conditions are containment environmental conditions resulting from a postulated loss-of-coolant accident, such as a primary system pipe break. The usual condition is an air-steam mixture with a pressure and temperature dependent on the particular containment. No attempt was made to model any actual postulated CVTR accident.

FIG. 237 CVTR CONTAINMENT STRUCTURE.

1. CVTR LEAKAGE RATE TESTS

Leakage rate tests were performed on the Carolinas Virginia Tube Reactor containment system to investigate the effects of pressure, temperature, and design basis accident (DBA) conditions on containment leakage rate.

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 basepoint 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 solutions to those problems.

Three distinct types of leakage rate tests were performed:

- (1) Ambient temperature, integrated leakage rate tests performed 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
- (2) Elevated temperature (150 and 200°F), 21-psig integrated leakage rate tests to determine the influence of temperature on containment leakage rate
- (3) An ambient temperature, 20-psig integrated leakage rate test following the DBA tests to evaluate the effect of DBA conditions on the containment vessel leakage rate.

During some of the tests, a known leak was superimposed upon the normal containment leakage to permit the accuracy and relative sensitivity of the leakage rate measuring systems to be determined. In addition, at the completion of several of the tests, a make-up air system was used to return the containment pressure to that which existed at the start of the tests. The amount of air required was used to calculate containment leakage and to evaluate the leakage rates measured by absolute and reference vessel methods.

The integrated leakage rate tests were performed similarly to previous CVTR tests performed for AEC compliance requirements[144] and to tests performed for evaluation of the CVTR continuous leakage rate system[145].

The leakage rates were determined by both the absolute method and the reference vessel method using data from seven independent leakage rate measuring systems: three absolute method systems and four reference vessel method systems.

1.1 Leakage Rate Test Instrumentation and Method of Analysis

During the CVTR leakage rate tests, measurements were made of containment temperature, pressure, and humidity, and differential pressure which developed between the containment vessel and each reference vessel.

The average containment temperature was obtained from two independent temperature measuring systems; 15 volume-weighted platinum resistance thermometers and 29 volume-weighted Chromel-Alumel thermocouples. The containment vessel pressure was obtained from three independent instruments: one absolute precision pressure gage and two manometers. The differential pressure between the containment vessel and the reference vessel was measured by four instruments (one for each reference vessel) including a precision differential pressure gage, a micromanometer, and two U-type manometers. The outside atmospheric pressure was obtained from a mercury barometer.

Data from the measurement systems permitted 14 determinations of the containment vessel leakage rate to be made for each leakage rate test, that is, three absolute and four reference vessel calculations for each temperature measuring system. These calculations established a basis of comparison from which recommendations were made concerning leakage rate measuring methods and systems.

Three computer programs were used to reduce the CVTR leakage rate test data, calculate the containment vessel leakage rate for each test, and obtain the statistical error associated with each leakage rate determination [140].

The containment leakage rate was calculated for each hour of leakage through use of leakage rate equations. From the calculated leakage rates, the amount of containment air remaining, which was normalized to 1.0 at time zero, was calculated and plotted with respect to time. A least-squares fit of these calculated data points was then performed. The leakage rate for the test period is simply the slope of the least-squares lines.

1.2 Results and Analysis of Leakage Rate Tests

Nineteen leakage rate tests were performed on the CVTR containment vessel at various pressure and temperature conditions. The initial conditions of each test are shown in Table XXXIV.

The CVTR containment leakage rates were calculated for each test by using the leakage equations and the computer codes developed to calculate leakage rate and data from both the absolute and reference vessel methods. The leakage rates were calculated using data from the precision pressure gages and both containment temperature measuring systems. In addition, for Test 8, a 21-psig ambient temperature test, the average containment temperature was calculated several times using data from a decreased number

TABLE XXXIV

INITIAL CONDITIONS OF CVTR LEAKAGE RATE TESTS

Test	Test Time (hr)	Test Pressure (psia)	Test Temperature (°F)
1	31	20.33	72
2	48	27.25	70
3	48	35.54	73
4	48	27.27	70
5	48	20.21	69
6	39	20.80	62
7	18	27.37	65
8	20	35.45	67
9	20	27.28	67
10	23	20.47	66
11	25	20.60	65
11A	24	20.52	64
12	18	27.4	66
13	24	35.41	70
16	24	20.48	76
17	18	35.43	77
18	40	35.47	146
19	42	34.28 to 35.04	195
20	38	34.85	92

of containment temperature sensors for each calculation. For each average temperature, the containment leakage rate was then calculated to determine the effect upon leakage rate of varying the number of temperature sensors used to obtain the average containment temperature.

Data from all seven pressure measuring systems were used to calculate the leakage rate of Tests 6 through 10, a complete series of tests. The data of Tests 6 through 10 were used to calculate leakage rates on an hourly basis. The following section discusses and compares the results of these various calculations.

1.21 Comparison of Leakage Rate Measurement Methods. Table XXXV lists the CVTR containment leakage rates determined for each leakage rate test by the absolute and reference vessel methods using pressure data obtained from the precision pressure gages, and temperature data obtained

TABLE XXXV

CVTR LEAKAGE RATES (%/DAY) FROM PRECISION PRESSURE GAGE

Test Conditions			Absolute Method		3/4-inch Reference Vessels					
			Thermocouples		Resistance Thermometers		Thermocouples		Resistance Thermometers	
Test	Pressure (psia)	Temperature (°F)	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak
1	20.33	72	0.859 ± 0.476[a]	-0.038 ± 0.232[a]	0.838 ± 0.18[a]	-0.004 ± 0.25[a]	0.199 ± 0.006[b]	0.228 ± 0.026[b]	0.199 ± 0.006[b]	-0.229 ± 0.026[b]
2	27.25	70	0.264 ± 0.011	0.287 ± 0.037	0.257 ± 0.006	0.286 ± 0.016	0.232 ± 0.005	0.275 ± 0.008	0.232 ± 0.005	0.272 ± 0.008
3	35.54	73	0.302 ± 0.011	0.176 ± 0.029	0.249 ± 0.005	0.261 ± 0.040	0.234 ± 0.004	0.235 ± 0.013	0.235 ± 0.004	0.235 ± 0.013
4	27.27	70	0.158 ± 0.009	0.227 ± 0.032	0.162 ± 0.005	0.229 ± 0.032	0.181 ± 0.004	0.229 ± 0.006	0.181 ± 0.004	0.222 ± 0.006
5	20.21	69	0.073 ± 0.013	0.094 ± 0.029	0.096 ± 0.007	0.119 ± 0.027	0.102 ± 0.003	0.164 ± 0.029	0.102 ± 0.003	0.163 ± 0.029
6	20.8	62	0.332 ± 0.019	---	0.284 ± 0.008	---	0.267 ± 0.006	---	0.268 ± 0.006	---
7	27.37	65	0.352 ± 0.018	0.402 ± 0.043	0.333 ± 0.012	0.379 ± 0.033	0.315 ± 0.009	0.351 ± 0.050	0.316 ± 0.009	0.352 ± 0.05
8	35.45	67	0.395 ± 0.024	0.430 ± 0.052	0.381 ± 0.019	0.502 ± 0.035	0.370 ± 0.009	0.413 ± 0.092	0.371 ± 0.009	0.413 ± 0.092
9	27.28	67	0.286 ± 0.020	---	0.283 ± 0.018	---	0.297 ± 0.011	---	0.297 ± 0.011	---
10	20.47	66	0.209 ± 0.013	---	0.204 ± 0.015	---	0.231 ± 0.011	---	0.231 ± 0.011	---
11	20.60	65	0.297 ± 0.052	---	0.291 ± 0.052	---	0.239 ± 0.009	---	0.239 ± 0.009	---
11A	20.52	64	0.119 ± 0.015	---	0.115 ± 0.013	---	0.108 ± 0.011	---	0.108 ± 0.011	---
12	27.4	66	0.209 ± 0.015	---	0.173 ± 0.015	---	0.178 ± 0.008	---	0.178 ± 0.008	---
13	35.41	70	0.226 ± 0.015	---	0.218 ± 0.011	---	0.230 ± 0.008	---	0.230 ± 0.008	---
16	20.48	76	0.073 ± 0.069	---	0.031 ± 0.024	---	0.080 ± 0.022	---	0.081 ± 0.022	---
17	35.43	77	0.326 ± 0.091	---	0.303 ± 0.032	---	0.314 ± 0.015	---	0.315 ± 0.016	---
18	35.47	146	0.160 ± 0.035	0.283 ± 0.063	0.149 ± 0.028	0.387 ± 0.046	0.122 ± 0.029[c]	0.374 ± 0.038	0.122 ± 0.029	0.372 ± 0.038
19	34.28	195	---	0.137 ± 0.018[c]	---	0.141 ± 0.017[c]	---	0.133 ± 0.016	---	0.133 ± 0.016
20	34.85	92	0.299 ± 0.040	0.420 ± 0.034	0.204 ± 0.056	0.455 ± 0.059	0.264 ± 0.039[b]	0.429 ± 0.048[b]	0.270 ± 0.038[b]	0.455 ± 0.059[b]

[a] Data from mercury manometer.

[b] Data from micromanometer.
Precision pressure gages were not available for Test 1.[c] The known leak was in operation throughout Test 19. The leakage was not measurable without the known leak in operation.
Test 19 results do not include data when dew point measurement was in doubt.

from both containment temperature measuring systems. The precision pressure gage data were used for these calculations because they were the most accurate and reliable pressure data obtained at CVTR.

Figures 238 through 241 are plots of containment air remaining as a function of time and are typical of leakage rate test results. These figures are for leakage rate Test 8 and include plots for each method of calculating leakage rate (absolute and reference vessel) and for each method of obtaining the average containment temperature (thermocouples and resistance thermometers). Similar plots for all the leakage rate tests are included in Reference 140.

FIG. 238 LEAKAGE RATE TEST 8 AT 21 PSIG; ABSOLUTE METHOD, PRECISION PRESSURE GAGE, THERMOCOUPLES.

FIG. 239 LEAKAGE RATE TEST 8 AT 21 PSIG; ABSOLUTE METHOD, PRECISION PRESSURE GAGE, RESISTANCE THERMOMETERS.

FIG. 240 LEAKAGE RATE TEST 8 AT 21 PSIG; 3/4-INCH REFERENCE VESSEL, PRECISION PRESSURE GAGE, THERMOCOUPLES.

FIG. 241 LEAKAGE RATE TEST 8 AT 21 PSIG; 3/4-INCH REFERENCE VESSEL, PRECISION PRESSURE GAGE, RESISTANCE THERMOMETERS.

As can be seen, both methods of calculating leakage rate and both systems of obtaining the average containment temperature provide comparable results. Generally, however, the reference vessel results show slightly less scatter than the absolute method results. In addition, for the absolute method, the results from the resistance thermometer data exhibited slightly less scatter than the results from the thermocouple data. The temperature measuring method had no effect upon the reference vessel results. Thus, data obtained from the reference vessel (3/4-inch) using either temperature measuring system exhibited the least scatter; slightly more scatter was present in the absolute method, resistance thermometer data; the most scatter occurred in the absolute method, thermocouple data.

In all cases, the observed scatter was negligible, and results from the absolute method and the reference vessel method were comparable. The leakage rate measurement method selected for a leakage rate test in a containment similar to the CVTR containment appears to be optional.

- (a) Various Temperature Sensor Combinations. The number and placement of the sensors used to obtain the average containment temperature can have significant effect on the calculated leakage rate. To determine the effect in CVTR, the leakage rate of Test 8 was calculated several times, using data with each calculation from a different combination of containment temperature sensors to obtain the average containment temperature. The result was that the number of temperature sensors used to determine the average containment volume temperature had essentially no effect on the results of the reference vessel leakage rate measurement method, and only a minor effect on the results of the absolute leakage rate measurement method.

The insensitivity of the CVTR leakage rate measurement to the number of temperature sensors used to obtain the average containment air temperature is probably typical of similar leakage rate measurements for insulated thick-walled concrete containment structures having well-mixed air volumes. However, if large heat sources or sinks exist within such structures, air temperature differences could occur and those differences must be accounted for in order to perform accurate containment leakage rate measurements.

The contained air volumes of uninsulated steel containments are greatly affected by atmospheric diurnal temperature cycles. The accuracy of the leakage rates determined by the absolute measurement method for this type containment structure will thus be quite dependent upon the number and placement of the temperature sensors used to determine the average containment air temperature. However, even for uninsulated steel containments, the accuracy of the leakage rate measurement by the reference vessel method would be expected to be relatively insensitive to the number of temperature sensors because a well-designed reference vessel automatically compensates for containment volume temperature changes.

Regardless of the containment system tested, or the testing method used, the containment temperature is one of the parameters which directly affects determination of the containment leakage rate. Therefore, to determine accurately a containment leakage rate, the containment atmospheric temperature must be measured with accurate, well-placed temperature devices, or compensated for through use of a well-designed, reference vessel.

- (b) Alternate Basis of Calculation. The ANS Standard [146] recommends that containment leakage rate be calculated for each hour of the test period. A conclusion from CVTR was that a better procedure is to calculate leakage from an initial data set to each successive data set. This procedure reduces the effect instrument errors have upon the leakage rate results, because the effect of instrument error decreases rapidly as the total test time increases as discussed in Section 1.3.

To determine the differences in the two procedures, the leakage rates of Tests 6 through 10 were calculated by both methods using resistance thermometer temperature data and the following four pressure measuring systems: (a) the precision absolute pressure gage, (b) the 3/4-inch reference vessel precision differential pressure gage, (c) the mercury manometer and barometer, and (d)

the 3/4-inch reference vessel micromanometer. The results of these calculations are shown in Table XXXVI. As can be seen, for the precision absolute pressure gage and for the two 3/4-inch reference vessels essentially no difference occurs in results obtained by the two methods of calculation. For the mercury manometer system, however, because of the errors which occurred in reading this manometer, a considerable difference in results between the two calculational methods was obtained. The reasons for the differences can be seen in Figures 242 through 245. When a data error occurs, as at 9 and 10 hours and 18 and 19 hours during Tests 6 and 10, respectively, the results from the time-from-start-of-test procedure are affected only at the time of the error. In the hourly procedure, however, because each hourly calculation is based upon the data of the previous hour instead of on an initial set of conditions, an error influences the test results for several hours (Figure 245). Because of this continuing influence, the results of an entire test can be compromised, as shown by the negative leakage rates obtained from the mercury manometer system for Tests 9 and 10. Even when the erroneous data points are not included in the leakage rate calculations, their influence inherently affects succeeding points and thus compromises the test results in the hourly procedure of calculating containment leakage rate.

TABLE XXXVI

CVTR LEAKAGE RATE CALCULATIONAL PROCEDURE COMPARISONS
(Resistance Thermometers)

Test	Leakage Rate (%/day)							
	Absolute Precision Pressure Gage		3/4-in. Reference Vessel Precision Pressure Gage		Absolute Mercury Manometer		3/4-in. Reference Vessel Micromanometer	
	Hourly	From Start of Test	Hourly	From Start of Test	Hourly	From Start of Test	Hourly	From Start of Test
6	0.285 ± 0.008	0.284 ± 0.008	0.272 ± 0.006	0.268 ± 0.006	0.214 ± 0.030	0.251 ± 0.026	0.275 ± 0.006	0.271 ± 0.006
7	0.334 ± 0.012	0.333 ± 0.012	0.319 ± 0.009	0.316 ± 0.009	0.344 ± 0.138	0.344 ± 0.137	0.319 ± 0.009	0.315 ± 0.009
8	0.382 ± 0.019	0.381 ± 0.019	0.373 ± 0.009	0.371 ± 0.009	0.384 ± 0.062	0.384 ± 0.062	0.373 ± 0.009	0.371 ± 0.009
9	0.283 ± 0.018	0.283 ± 0.018	0.300 ± 0.011	0.297 ± 0.011	-0.147 ± 0.113	0.147 ± 0.113	0.302 ± 0.012	0.298 ± 0.012
10	0.204 ± 0.015	0.204 ± 0.015	0.235 ± 0.011	0.231 ± 0.011	-0.096 ± 0.055	0.279 ± 0.330	0.238 ± 0.011	0.234 ± 0.011

FIG. 242 LEAKAGE RATE TEST 6 AT 6 PSIG; ABSOLUTE METHOD, MERCURY MANOMETER, RESISTANCE THERMOMETERS, FROM START OF TEST.

FIG. 243 LEAKAGE RATE TEST 6 AT 6 PSIG; ABSOLUTE METHOD, MERCURY MANOMETER, RESISTANCE THERMOMETERS, HOURLY.

FIG. 244 LEAKAGE RATE TEST 10 AT 6 PSIG; ABSOLUTE METHOD, MERCURY MANOMETER, RESISTANCE THERMOMETERS, FROM START OF TEST.

Tests 7, 8, and 9 also had data errors. However, in each case the error occurred in the last data point of the test, but because the erroneous data were not included in the leakage rate calculation the hourly calculated test result was not affected.

Based on these findings, the conclusion is reached that leakage rate calculations should be made from an initial set of test conditions to each succeeding data point rather than on an hourly basis as specified in the ANS Standard [146].

FIG. 245 LEAKAGE RATE TEST 10 AT 6 PSIG; ABSOLUTE METHOD, MERCURY MANOMETER, RESISTANCE THERMOMETERS, HOURLY.

- (c) Reduced Number of Data Points. In an attempt to determine how frequently data must be recorded during leakage rate testing, the results of Test 4, a 48-hour test with an additional 24 hours of known leak operation, were calculated using every data point, every other data point, every fourth data point, and every eighth data point. The results of these calculations are shown in Table XXXVII.

As can be seen, as the number of readings used to obtain data were reduced, no significant difference in leakage rate was obtained. However, the error associated with each result began to increase significantly. In calculations of error by regression analysis, a t value is used (Section 1.3) which is dependent upon the confidence level desired and the degrees of freedom, that is, the number of data points. Thus, as the number of data points used to calculate the leakage rate decreases, the value of t increases and the error associated with the calculation increases. Therefore, in leakage rate testing, if an 18- to 24-hour test is being run, data should be recorded hourly. On long-duration tests, data every 4 or 8 hours will probably be sufficient. The frequency with which data are taken depends upon the length of tests to be run and the allowable error in the test results.

1.22 Comparison of Instrumentation Systems Used for Leakage Rate Determination. In addition to the two precision pressure gages, five other pressure instruments, two for absolute method measurements and three for reference vessel method measurements, were used to obtain pressure data for containment leakage rate determinations. The instruments are as follows:

TABLE XXXVII

CVTR TEST 4 LEAKAGE RATE FROM VARIOUS NUMBERS OF DATA POINTS

<u>Calculational Method</u>	<u>Frequency of Data (hr)</u>	<u>Total Readings</u>	<u>Leakage Rate (%/day)</u>
Absolute	1	49	0.158 ± 0.009
	2	25	0.151 ± 0.015
	4	13	0.161 ± 0.018
	8	7	0.161 ± 0.022
Reference vessel, 3/4-inch	1	49	0.181 ± 0.004
	2	25	0.179 ± 0.006
Precision pressure gage	4	13	0.180 ± 0.008
	8	7	0.180 ± 0.014

(1) Absolute method:

- (a) A 100-inch Meriam well-type manometer filled with mercury
- (b) A 120-inch Meriam well-type manometer filled with Meriam No. 3 indicating fluid[a].

(2) Reference vessel method:

- (a) A 6-inch Meriam U-type manometer filled with red oil and used with the 1/4-inch reference vessel
- (b) A 60-inch Meriam U-type manometer filled with red oil and used with the 1-inch reference vessel
- (c) A Meriam micromanometer filled with green indicating fluid and used with a 3/4-inch reference vessel.

The results of Tests 6 through 10 for all seven pressure instrument systems used for measuring containment leakage rate are presented in Table XXXVIII. The leakage rates were calculated using data from the resistance thermometer temperature measuring system and do not include obvious data errors. Tests 6 through 10 were used for these comparisons because no known changes in leak paths occurred.

For the absolute method, the precision pressure gage provided the best results because of its greater accuracy; that is the data were less scattered

[a] Because of the limited range of this manometer (13 psig) this system was not used for any 21-psig tests.

TABLE XXXVIII
CVTR LEAKAGE RATE INSTRUMENTATION COMPARISONS,
TESTS 6 THROUGH 10^[a]
(Resistance Thermometers)

		Leakage Rate (#/day)							
Absolute Method		Mercury Manometer		120-inch Manometer		Precision Pressure Gage			
Test	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	
6	0.251 ± 0.026	---	0.245 ± 0.029	---	0.204 ± 0.000	---	---	---	
7	0.344 ± 0.157	0.363 ± 0.475	0.328 ± 0.040	0.246 ± 0.260	0.333 ± 0.012	0.379 ± 0.033	---	---	
8	0.384 ± 0.062	0.550 ± 0.515	insufficient range		0.381 ± 0.019	0.502 ± 0.035	---	---	
9	0.147 ± 0.113	---	0.286 ± 0.034	---	0.283 ± 0.018	---	---	---	
10	0.279 ± 0.330	---	0.183 ± 0.039	---	0.204 ± 0.015	---	---	---	

Reference Vessel Method		1/4-inch Vessel, U-tube Manometer		1-inch Vessel, U-tube Manometer		3/4-inch Vessel, Micromanometer		3/4-inch Vessel, Precision Pressure Gage	
Test	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	Without Known Leak	With Known Leak	
6	0.220 ± 0.007	---	0.259 ± 0.006	---	0.271 ± 0.006	---	0.268 ± 0.006	---	
7	0.273 ± 0.011	0.290 ± 0.062	0.344 ± 0.016	0.374 ± 0.043	0.351 ± 0.009	0.352 ± 0.031	0.316 ± 0.009	0.352 ± 0.050	
8	0.226 ± 0.014	0.362 ± 0.034	0.293 ± 0.015	0.457 ± 0.056	0.371 ± 0.009	0.435 ± 0.068	0.371 ± 0.009	0.413 ± 0.092	
9	0.264 ± 0.015	---	0.315 ± 0.015	---	0.298 ± 0.012	---	0.297 ± 0.011	---	
10	0.219 ± 0.013	---	0.262 ± 0.012	---	0.234 ± 0.011	---	0.231 ± 0.011	---	

[a] Tabulated leakage rates do not include circumous data.

and agreed with the 3/4-inch reference vessel results. Of the reference vessel methods, the two 3/4-inch vessels provided the best results and the results were approximately equivalent.

Based on the results of the comparisons, the absolute and reference vessel methods of leakage rate measurement are comparable when comparable instrument systems are used. Both methods provide good results and both methods have advantages and disadvantages. For example, precise temperature and humidity instrumentation must be used with the absolute method; for the reference vessel method, the vessel should sample all regions of the containment and must be leak-tight.

One item clearly pointed out by the detailed comparison of the instrumentation systems used with the absolute method is the distinct advantage of an absolute pressure instrument, particularly one which had digital readout. Throughout the leakage rate tests, obvious errors occurred in obtaining instrument readings. During Tests 6 through 10, at least one error occurred in each test. In Tests 6, 7, and 10 the errors involved the barometer reading and affected the results of all the absolute method leakage rate calculations except those obtained with the precision pressure gage. In Tests 8 and 9, the errors occurred in reading or recording manometer level. An absolute pressure gage with digital readout eliminates the possibility of a barometer error and greatly reduces the possibility of an instrument reading error.

1.23 Pressure, Temperature, and DBA Effects. A primary objective of the CVTR leakage rate tests was to determine the influence of pressure, temperature, and DBA conditions on leakage rate. Preliminary evaluations of the results of these effects have been reported[141].

- (a) Pressure Effects. Leakage rate tests of the CVTR containment were performed at several pressures to determine the effects

of test pressure on containment leakage rates. Table XXXV presents the results of all the leakage rate test performed on the CVTR containment vessel. Table XXXIX presents the results of specific leakage rate tests that illustrate the effect of pressure on containment leakage rate.

As expected, the containment leakage rate increases with pressure, as shown in Figure 246. Further, the process by which leakage increases appears to be a function of the predominant type(s) of leakage flow paths. For Tests 6 through 10 (Table XXXIX), for example, the leakage rate closely followed the turbulent flow, smooth path extrapolation equation[141]. For Tests 3 through 5 and 11A through 13, however, the leakage rate closely followed the molecular flow extrapolation equation[141]. During other tests (Tests 16 and 17) the flow paths apparently changed and no extrapolation equation fits the data from those tests. Thus, to determine an applicable extrapolation equation which accurately predicts containment leakage rate may be impossible. Even if a series of leakage rate tests is performed to obtain an applicable extrapolation equation, the equation may be valid only as long as the leak paths remain constant. As shown by the CVTR tests, changes in leak paths do occur, often for no apparent reason.

FIG. 246 CVTR LEAKAGE RATE AT VARIOUS PRESSURES.

Even though several of the leakage rate tests closely followed the molecular flow extrapolation equation, molecular flow is not of significant consequence for the size of leaks of interest (300 to 500 ft³/day) in containment vessels. Therefore, the prevailing type of flow from the containment during tests when the molecular flow extrapolation equation applied was probably not molecular but a combination of laminar and turbulent types of flow.

As can be seen in Table XXXIX, for tests during which the leak paths remained essentially constant, very little hysteresis effect was noted in the leakage rate pressure dependence. That is, the leakage rate was about the same for a given pressure, regardless of whether the previous test pressure was higher or lower.

The average rate of change of the CVTR containment leakage rate was $0.008 \pm 0.002\%/day/psi$ over the pressure range tested. Thus, the type of leak flow paths did not affect the average pressure-dependent extrapolation, provided the leak paths remained essentially constant, that is, provided no significant changes occurred during a test series.

TABLE XXXIX

EFFECTS OF PRESSURE, TEMPERATURE, AND DBA CONDITIONS
ON CONTAINMENT LEAKAGE RATE

<u>PRESSURE EFFECT</u>				
<u>Containment Conditions</u>				
<u>Test</u>	<u>Pressure (psig)</u>	<u>Temperature (°F)</u>	<u>Leakage Rate (%/day)</u>	<u>Best Fit Extrapolation Equation[141]</u>
6	6	62	0.268 ± 0.006	Turbulent Flow, Smooth Path
7	13	65	0.316 ± 0.009	
8	21	67	0.371 ± 0.009	
9	13	67	0.297 ± 0.011	
10	6	66	0.231 ± 0.011	
11A	6	64	0.108 ± 0.011	Molecular Flow
12	13	66	0.178 ± 0.008	Molecular Flow
13	21	70	0.230 ± 0.008	Molecular Flow
3	21	72	0.235 ± 0.004	Molecular Flow
4	13	70	0.181 ± 0.004	Molecular Flow
5	6	69	0.102 ± 0.003	Molecular Flow
16	6	76	0.081 ± 0.022	Flow Paths Changed
17	21	77	0.315 ± 0.015	
<u>TEMPERATURE EFFECT</u>				
17	21	77	0.315 ± 0.016	
18	21	146	0.122 ± 0.029	
19	21	195	not measurable ^[a]	
<u>DBA EFFECT</u>				
20	21	92	0.260 ± 0.040	

[a] To substantiate that the leakage during Test 19 was negligible, throughout Test 19, a known leak of approximately $0.16 \pm 0.02\%/day$ was imposed upon normal containment leakage. The measured total containment leakage rate was approximately $0.14 \pm 0.02\%/day$.

- (b) Temperature Effects. Three tests (Tests 17, 18, and 19) were performed at CVTR to investigate the effect of temperature on containment leakage rate. As shown in Table XXXIX and Figure 247, increasing the containment temperature resulted in a decreased containment leakage rate at the same pressure condition, 21 psig. The leakage rate decreased from 0.315%/day at 77°F to 0.122%/day at 146°F to a negligible amount of 195°F. (Since no normal containment leakage could be detected during Test 19, a known leak of 0.16%/day was superimposed upon the normal containment leakage to check the leakage rate instrumentation and to confirm the leakage rate measurement.) The total containment leakage measured during Test 19 with the known leak was about 0.14%/day. As can be seen in Figure 247, the data scatter during Test 19 was quite large and the exact value for the containment leakage during this test is somewhat uncertain. However, the leakage rate is believed to be small [a].

FIG. 247 CVTR LEAKAGE RATE AT 21 PSIG AND VARIOUS TEMPERATURES.

The average decrease in containment leakage rate per °F increase in containment temperature was about 0.0028%/day/°F between 77 and 146°F. If the containment leakage rate is assumed to be 0.015%/day at 195°F, the rate of decrease in leakage rate per °F temperature increase is also 0.0028%/day/°F. These results would indicate that the rate of change in the CVTR containment leakage rate with temperature may be approximately linear over the range of temperatures investigated.

- (c) DBA Conditions Effects. Test 20 was the final CVTR leakage rate test and was performed following the design basis accident test series. The test conditions and leakage rate for Test 20 are shown in Table XXXIX. As can be seen, the leakage rate during

[a] A discussion of the problems associated with performing the hot air test series is given in Reference 141.

Test 20 was approximately 0.260%/day, a decrease in leakage of about 0.055%/day from that of Test 17, a preceding 21-psig ambient temperature test. Most of this decrease in leakage appears to be the result of a higher average containment temperature (about 15°F) during Test 20. If the average decrease in leakage rate of 0.0028%/day/°F is assumed to be valid, the decrease in leakage because of an increased containment temperature is about 0.042%/day, or nearly all of the observed decrease in leakage. Therefore, DBA conditions had negligible effect upon containment leakage.

(d) General Discussion. The CVTR pressure and temperature results generally agree with the results reported by others [147,148]. The temperature effect upon leakage rate could have been the result of several possible causes:

- (1) Leak paths through valves, pipes, and fittings closed with increasing temperature as the metal expanded
- (2) Resilient seals deformed with temperature, pressure, and humidity to fill small leak paths and form a better seal
- (3) Nonmetallic gaskets became more flexible, deformed, and formed a better and tighter seal
- (4) Resilient seals and nonmetallic gaskets swelled because of high temperatures and formed better seals.

One important unknown in the reduction of leakage rate by high temperature conditions is time. During the elevated temperature tests, several days were required to heat the containment to test conditions. The rate at which the containment leakage decreased is not known. Thus, although high temperatures do decrease containment leakage rate, the time required to effect a reduction may be minutes, hours, or even days.

1.24 Secondary Methods of Verifying Containment Leakage Rates. A known leak system, a fixed leak system, and a make-up air system were utilized respectively at CVTR to (a) indicate the accuracy and sensitivity of the leakage rate systems, (b) determine the influence of pressure, temperature, and DBA conditions on leakage rate, and (c) serve as another method of calculating containment leakage rate.

- (a) Known Leak. During several of the CVTR leakage rate tests, a known leak was superimposed upon normal containment leakage for the final 6 to 24 hours of the test period. A comparison of the measured and calculated effect of the known leak for Tests 5, 7, and 8 is shown in Table XL. The plotted data for Test 8, which is typical of all the tests, are shown in Figures 239 and 241. As can be seen, the 3/4-inch reference vessel method and the absolute method rapidly indicated that the known leak had been placed in operation. The reference vessel method, however, provided a much more accurate measurement of the size of the leak than did the absolute method.

TABLE XL

KNOWN LEAK COMPARISONS

Test	Pressure (psig)	Measured Known Leak (%/day)		Calculated Known Leak (%/day)
		[a]	[b]	
5	6	0.061	0.024	0.085
7	13	0.036	0.046	0.058
8	21	0.042	0.121	0.045

[a] Three-fourth-inch reference vessel, precision pressure gage, resistance thermometers.

[b] Absolute method, precision pressure gage, resistance thermometers.

In all cases except one, the calculated value of the known leak is larger than the measured value. The most probable reason for the greater calculated value is that it is based upon an estimated value of containment-free volume, and since the estimated volume for CVTR (227,000 ft³) is slightly low, a larger-than-actual calculated known leak results. In addition to containment volume, other factors which can influence the measurement of the known leak are the accuracy of the known leak rotameter and the care with which flow is controlled while the known leak is in operation.

Generally, calculated and measured values for the known leak are good, particularly as measured by the reference vessel method. Even though the two measurements of the known leak for these tests are not in exact agreement, the use of a known leak is a good practice because it can provide a reliable indication of the accuracy and sensitivity of the leakage rate test instrumentation.

- (b) Make-up Air. At the conclusion of several of the CVTR leakage rate tests, a make-up air system consisting of a positive displacement gas meter, a temperature indicator, and a pressure indicator was used to return the containment pressure to that which existed at the beginning of the test. By measuring the amount of air required to return the containment to the original test condition, a further check on containment leakage was obtained. The results of calculations for Tests 4 and 12 are shown in Table XLI. The results show that the amount of make-up air required to return the containment to the initial test pressure agrees very well with the amount of air which leaked from the containment ($\pm 10\%$) and illustrates that a make-up air system can be used advantageously to check the measured leakage.

1.3 Leakage Rate Instrumentation Error Analysis^[a]

An error analysis was performed on the CVTR leakage rate instrumentation to determine the expected instrumentation error associated with each

[a] The propagation of error formulas are derived in Reference 140.

TABLE XLI
MAKE-UP AIR RESULTS

<u>Test</u>	<u>Test Pressure (psig)</u>	<u>Leakage (standard cubic feet)</u>	<u>Make-Up Air (standard cubic feet)</u>
4	13	2300	2530
12	13	563	540

leakage rate measuring system. The results of the instrumentation error analysis have been compared with the results of the regression error analysis to obtain an indication of the relationship between calculated and measured errors.

An instrumentation error analysis can be important if performed prior to purchasing equipment for a leakage rate test. Such an analysis can provide a basis for the selection of leakage rate instrumentation. If the containment leakage rate range and measurement accuracy have been established, an instrumentation error analysis can be useful in determining the instrumentation requirements that must be met to measure the leakage with the specified accuracy.

1.31 Classification of Errors[149]. Two types of errors are generally encountered in the measurement process: random and systematic. Both types of errors occur in the measurement of a given parameter and together determine the total measurement error.

In measurements, random errors are the sum total of all the small independent errors over which no control can be exerted, such as interpolation in reading scales and slight fluctuations in environmental conditions. For a stable measurement process, the measured values follow a normal distribution with small errors occurring more frequently than large errors, and with positive and negative errors approximately balancing one another. Thus, no obvious trend or pattern exists in the measurement process as a result of random errors.

A systematic error is a fixed deviation inherent in each and every measurement of X in a particular set of measurements and is often referred to as bias. Corrections can be made to account for the systematic error if the magnitude and the sign of the error (bias) are known. Generally, the absolute magnitude and sign of the systematic error are unknown.

The error in the measurement of each parameter used in determining leakage rate will, therefore, have two components: (a) a systematic error or bias that is assumed to have the same sign and magnitude for each measurement and (b) a random error which will consist of the measurement system repeatability for each parameter and will define the precision of the measurement[149].

1.32 Determination of Standard Deviations. Before a standard deviation can be calculated for the leakage rate measurement, a value for the standard deviation of each leakage rate parameter must be determined either experimentally, from vendor data, or calculated. Table XLII shows the standard deviation associated with each type of error and the method used to determine the standard deviation for each parameter applicable to the CVTR leakage rate requirements.

TABLE XLII
STANDARD DEVIATION VALUES

Parameter	System	Absolute Method	Reference Vessel Method		Method of Determination
		S_r [a]	S_s [a]	S_r [a]	
Temperature	Thermocouples [b]	0.184°F	Not Applicable	0.184°F	Experimentally determined for complete measuring system.
	Resistance Thermometers [c]	0.1°F	Not Applicable	0.1°F	
Absolute Pressure	Precision Pressure Gage	2.46×10^{-4} psi	7.22×10^{-3} psi	2.46×10^{-4} psi	Mechanical repeatability as quoted in the instruction manual.
	Red Oil Manometer	5.4×10^{-3} psi			
	Mercury Manometer	2.45×10^{-2} psi			Estimated at one-half of the minor scale division.
Differential Pressure	Precision Pressure Gage		Not Applicable	2.55×10^{-6} psi	
	Micromanometer		Not Applicable	1.8×10^{-5} psi	
	U-Tube Manometer		Not Applicable	1.8×10^{-3} psi	
Vapor	Cambridge Dew Point Hygrometer [d]	2.1×10^{-3} psi	Not Applicable	2.1×10^{-3} psi	As quoted in instruction manual.

[a] S is the standard deviation, the subscripts r and s refer to the random and systematic errors, respectively.

[b] The values for the thermocouples include a long-term cycle inherent in the 150°F reference junction box.

[c] The resistance thermometer value was determined by the Idaho Nuclear Corporation's Standards Laboratory.

[d] The humidity values must be determined for each test, based on a hygrometer accuracy of 0.5°F. The values shown are for Test 7.

For the absolute method, the systematic error or bias was considered negligible in the analysis of the leakage rate error and the random error was the only error considered. The random error is determined by the inability of a measurement system to indicate the same temperature or pressure at any time ($t_1, t_2, t_3, \dots, t_n$) if the conditions have not changed.

For the reference vessel method, both the systematic errors and the random errors were considered in determining the standard deviation values. In the case of pressure (absolute and differential, however, the contribution from the random error was insignificant.

1.33 Calculation of Estimated Error. The estimated standard deviation of each parameter from Table XLIII, the variance of leakage rate, $V(L)$, and the standard deviation of leakage rate, $S(L) = \sqrt{V(L)}$, were calculated by using the

TABLE XLIII

LEAKAGE RATE VARIANCE AND STANDARD DEVIATION

	Leakage Rate (%/day)	Error By Regression Analysis	Estimated Standard Deviation $S(L) = \sqrt{V(L)}$	Estimated Variance $V(L) = \sum V(X)$	Contribution to $V(L)$ From Various Systems				Time (t)
					Temperature $V(T)$	Pressure $V(P)$	Δ Pressure $V(\Delta P)$	Vapor Pressure $V(P_v)$	
Absolute Method	0.352	± 0.018	± 0.046	2.119×10^{-3}	Thermocouples 2×10^{-3}	Precision Pressure Gage 1.6×10^{-6}		1.17×10^{-4}	8×10^{-9}
	0.333	± 0.012	± 0.029	8.42×10^{-4}	Resistance Thermometers 7.24×10^{-4}	Precision Pressure Gage 1.6×10^{-6}		1.17×10^{-4}	8×10^{-9}
	0.334	± 0.137	± 0.129	1.68×10^{-2}	Resistance Thermometers 7.24×10^{-4}	Mercury Manometer 1.6×10^{-2}		1.17×10^{-4}	8×10^{-9}
	0.328	± 0.040	± 0.04	1.62×10^{-3}	Resistance Thermometers 7.24×10^{-4}	No. 3 Fluid Manometer 7.8×10^{-4}		1.17×10^{-4}	8×10^{-9}
Reference Vessel Method	0.316	± 0.009	± 0.0108	1.17×10^{-4}	Resistance Thermometers 4×10^{-8}	Precision Pressure Gage 7×10^{-9}	Precision Pressure Gage 1.73×10^{-10}	1.17×10^{-4}	8×10^{-9}
	0.315	± 0.009	± 0.0108	1.17×10^{-4}	Thermocouples 1.3×10^{-7}	Precision Pressure Gage 7×10^{-9}	Precision Pressure Gage 1.73×10^{-10}	1.17×10^{-4}	8×10^{-9}
	0.344	± 0.016	± 0.0142	2.02×10^{-4}	Resistance Thermometers 4×10^{-8}	Precision Pressure Gage 7×10^{-9}	U-Tube Manometer 8.5×10^{-5}	1.17×10^{-4}	8×10^{-9}
	0.315	± 0.009	± 0.0108	1.17×10^{-4}	Resistance Thermometers 4×10^{-8}	Precision Pressure Gage 7×10^{-9}	Micromanometer 9×10^{-9}	1.17×10^{-4}	8×10^{-9}

propagation of error formulas. The calculations were made for Test 7 for several leakage rate measuring systems. The time error in the calculations was assumed to be ten minutes for a 24-hour test period.

For the absolute method, the major contributors to the total errors were the temperature and vapor pressure measurements. For the reference vessel method, the differential pressure and the vapor pressure were the major contributors.

The error in the leakage rate results as calculated by regression analysis is also included in Table XLIII. This value was included for comparison of the error obtained from the test data with the error calculated from the instrumentation. In every case, except where the mercury manometer was used, the regression analysis error is less than the instrument error as was expected, because the instrument error calculation assumes that all possible instrument errors occur at the same time, in the same direction, and in the maximum possible amount. In testing, however, errors do not occur in this manner. Rather, the errors are random and cancelling effects occur.

Both the instrument error analysis and the regression analysis indicate that the errors in the absolute method are larger than those in the reference vessel method.

Test parameters are not the same for all leakage rate tests. As the parameters change, the standard deviation in leakage rate, $S(L)$, also changes. Figures 248, 249, and 250 show the effect upon $S(L)$ of changes in test time, pressure, and dew point. The curves were calculated by varying the parameter of interest while holding all other parameters in the propagation of error equation constant.

Although the effect of a small error in test time is negligible, the total test time has a significant effect on the standard deviation. Figure 248 shows the effect of total test time upon $S(L)$, and as can be seen the standard deviation in leakage rate decreases rapidly as test time increases, particularly for the first 25 hours of a test.

Figure 249 shows the effect of test pressure upon $S(L)$. The calculations were made for three different pressure measuring instruments, the mercury manometer, the manometer filled with No. 3 indicating fluid, and the precision pressure gage, and are based on the assumption that the error associated with each pressure instrument is constant over the instrument range. As can be seen as test pressure increased, the standard deviation $S(L)$ decreased. Also, the more accurate the pressure instrument, the less the effect of test pressure upon $S(L)$.

Figure 250 illustrates the effect of dew point upon $S(L)$. As can be seen, as the dew point increases, $S(L)$ increases, if a constant error over the range of the dew point instrument is assumed. The standard deviation increases with dew point even though a constant 1°F instrument error is assumed. This increase in standard deviation can be explained by the fact that as the dew point increases the vapor pressure increases, and a 1°F error at a low dew point is not as significant as a 1°F error at a high dew point.

FIG. 248 EFFECT OF TEST TIME UPON STANDARD DEVIATION.

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FIG. 249 EFFECT OF TEST PRESSURE UPON STANDARD DEVIATION.

FIG. 250 EFFECT OF DEW POINT UPON STANDARD DEVIATION.

On the basis of the preceding analysis, if the leakage rate error (SL) is to be small, the test instrumentation must have high precision, or the tests should be performed at the highest practical pressure for the longest practical time and at the lowest practical dew points.

1.4 Leakage Rate Test Conclusions

The following conclusions were reached from the results of the leakage rate tests performed on the CVTR containment. Included are conclusions from the preliminary evaluation of the test results[141] which were not stressed in the text of this report.

- (1) Over the pressure range investigated and for a particular leakage path, the CVTR containment leakage rate increased and decreased in a continuous manner, as predicted by established extrapolation equations. However, leakage paths and leakage rates changed during and between some of the test series. Thus, experimentally establishing a particular extrapolation equation may be unreliable for accurately extrapolating containment leakage from subsequent lower pressure tests to that expected at DBA pressures.

Over the pressure range investigated for the CVTR containment and for a given series of tests during which the leak paths remained constant, the following simple extrapolation equation predicted containment leakage within $\pm 10\%$

$$L_e = L_t + 0.008 (P_e - P_t)$$

where

L_e = Leakage at extrapolated pressure (%/day)

L_t = Leakage at test pressure (%/day)

P_e = Extrapolated pressure (psia)

P_t = Test pressure (psia).

These results indicate that for CVTR, regardless of the leakage rate, the effect of pressure on leakage rate was essentially linear.

- (2) A continuous leakage rate measurement system which provides constant knowledge of containment leak tightness is probably the most meaningful method of monitoring containment leakage because of uncertainties associated with changing leak paths.
- (3) The CVTR containment leakage rate decreased with increasing test temperature at an average rate of about 0.0028%/day/°F. This relationship is undoubtedly dependent upon the particular leak paths, and these results may not be applicable to

all containments. However, the relationship is expected to be representative of that for many of the newer containments which are similar in construction to CVTR.

- (4) Simulated DBA conditions appear to have had negligible effect on the containment leak tightness. DBA conditions did not increase containment leakage, and may have decreased leakage slightly.
- (5) The absolute method and reference vessel method of measuring containment leakage rate are comparable when equivalent instrumentation is used. For the CVTR tests, the reference vessel method did, however, provide data with slightly less scatter, and thus greater precision.
- (6) Both the absolute method and reference vessel method were able to detect quickly a superimposed known leak and indicate its relative size. Use of a known leak system is a good test practice because the system indicates the accuracy and sensitivity of the leakage rate measuring systems. A make-up air system is also a valuable asset for a leakage rate test, because a make-up air system provides an additional means of checking the measured containment leakage.
- (7) Test pressure had insignificant influence on the accuracy of the leakage rate measurement when an accurate pressure measuring instrumentation, such as the precision pressure gages, was used. The accuracy of the 6-psig test results are comparable to the 21-psig test results, particularly as measured by the reference vessel method. For less precise pressure instruments, however, test pressure does influence test results, and the higher the test pressure, the more reliable the test results.
- (8) The number of temperature sensors used in CVTR to obtain the average containment atmosphere temperature had no effect on the leakage rate as determined by the reference vessel method and only a minor effect on the absolute method results. One precision temperature measurement would have been sufficient for the CVTR leakage rate tests. Similar results could be expected for other well-insulated containments (thick concrete walls) in which no large heat sources or sinks exist and for which the atmosphere is well mixed during testing.
- (9) Eighteen hours was the minimum time for a reliable leakage rate test of the CVTR containment. As test time decreased, the uncertainty in the measurement rapidly decreased. If short-term leakage rates are desired, the test should be run at a higher pressure and with more accurate leakage rate instrumentation.

- (10) The two methods of calculating leakage rate (time from start of test and the time between successive data points) provide comparable results until data reading or recording errors occurs. The time-between-successive-data-points calculational methods is then inferior because an error has a continuing influence upon this method of calculating leakage rate and the results of an entire test can be compromised.

2. CVTR SIMULATED 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 to:

- (1) Obtain experimental data on containment pressure and temperature response to assess the capability of typical computer codes for predicting the response of a containment atmosphere subjected to DBA conditions
- (2) Obtain time-dependent heat transfer data at various locations throughout the containment structure to assess currently accepted heat transfer correlations used in containment response computations and to provide a basis for recommendations to improve heat transfer models
- (3) Determine the capability of a pressure-reduction containment spray system and the gross effectiveness of the spray
- (4) Determine the gross effects of DBA conditions, exclusive of radiation, on the CVTR containment integrity.

Five steam tests were performed during the CVTR project: two system checkout tests and three simulated DBA tests:

- (1) A steam line test for checkout of the steam plant and steam line at operating conditions without steam injection into the containment.
- (2) A semi-DBA test for which steam injection was terminated at a containment pressure of about 8 psig and following which the pressure reduction spray system was operated for about three minutes. This test served as a basis from which the DBA steam injection time and spray operation time were calculated.
- (3) A natural decay test in which, (following steam injection) the containment atmosphere was allowed to return to ambient conditions by natural decay. The objectives of this test were to provide accurate pressure-temperature histories of the

containment atmosphere for comparison with predictions calculated by the computer code CONTEMPT^[85] and to provide base conditions for evaluating the effectiveness of the containment pressure reduction spray.

- (4) A steam-spray test during which (following steam injection) the pressure reduction spray system was activated at a flow rate of 290 gpm. The objectives of this test were to demonstrate the capability of the spray system and the effectiveness of the spray and to provide experimental containment response data for comparison with that predicted by computer codes.
- (5) A second steam-spray test identical to the first, except that the spray flow rate was increased to 500 gpm.

To perform a DBA test, two of the Parr Station boilers were brought to full capacity by venting steam to the atmosphere. When the boilers reached full capacity, the desuperheater was brought on-line by increasing desuperheating water flow until the temperature of the venting steam had been reduced to about 10°F above the saturation temperature. Once desuperheating water flow for desuperheating the superheated steam had been established, and the steam flow to the atmosphere had been stabilized, the cylinder-operated charge valve was opened and the vent valve was closed to direct the total steam flow into the containment.

2.1 Simulated DBA Data Measurement and Acquisition Systems^[142,143]

In addition to the process data measurements associated with the steam injection system and the pressure reduction spray system, a number of measurements were made to determine the containment response to rapid steam injection and to relate the experimental response to that calculated by current analytical techniques. The following instrument systems were used to obtain the containment response measurements:

(1) Containment Atmospheric Pressure

The containment atmospheric pressure was measured by use of seven 0 to 25 psig fast-response pressure transducers and a 0 to 30 psig Heise gage.

(2) Containment Atmospheric Temperature

The containment atmospheric temperature was measured by 34 Chromel-Alumel thermocouples and 15 platinum resistance thermometers located throughout the containment volume. Two additional atmospheric thermocouples were located in the reactor header cavity and the refueling canal.

(3) Containment Surface Temperature

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

(4) Temperature Profile Through the Containment Liner and Wall

Two heat transfer assemblies were installed in the wall of the operating region of the containment. Each assembly consisted of a 10-inch-diameter, 1/4-inch-thick, steel plate with special thermocouples embedded across the thickness of the plate.

(5) Condensation

The total condensate formed during each test was determined through use of containers that collected condensate from selected areas of the containment liner walls. The rate of condensate formation was obtained by suspending five of the containers from strain gage load cells.

(6) Containment Liner Stress

Eighty-three rosette strain gages, each having three components, were installed on the containment liner.

(7) Containment Convective Currents

Three ultrasonic type anemometers were installed inside the containment vessel to measure air velocities during the DBA tests.

(8) Data Acquisition Systems

The data from the various instrumentation systems were recorded on one or more of four data acquisition systems: (a) a digital data acquisition system (slow scan), (b) an analog data multiplexing system (fast scan), (c) an oscillograph system, and (d) a digital voltmeter with a digital printer.

2.2 Simulated DBA Test Results

Three simulated DBA steam injection tests were performed in the CVTR containment system. Steam flow rates and injection conditions are summarized in Table XLIV.

2.21 Pressure History. The containment pressures for the three DBA tests as obtained from the Heise gage are shown in Figure 251. Figure 252 presents the pressure of the main containment volume as a function of time for Test 3 as obtained from four of the five pressure transducers and the Heise gage. Temperature corrections have been applied to these data, and as can be seen, excellent agreement among the results from various pressure measuring devices was obtained. Also, as expected, no pressure gradients or other special pressure dependence was observed for this relatively slow containment pressurization of the DBA tests. Pressures in the partially isolated reactor header canal and fuel transfer canal were measured to determine whether differential pressures or differences in the times of peak pressure existed between the partially isolated volumes and the main containment

TABLE XLIV

STEAM DATA SUMMARY

Test [a]	Steam Injection Time (sec)[b]	Superheated Steam Flow Rate (lb/hr)	Average Flow Rate of Desuperheating Water (lb/hr)	Saturated Steam Flow Rate (lb/hr)	Total Flow (lb)	Average Enthalpy (Btu/lb)	Total Energy (Btu)
3	166.4	320,000	60,000	380,000	16,525	1195	1.97×10^7
4	174.8	310,000	65,000	375,000	17,341	1196	2.07×10^7
5	173.1	320,000	40,000	360,000	16,376	1197	1.96×10^7

[a] Test 1 was a system checkout test and Test 2 was a semi-DBA test for establishing the steam injection time and spray operation time for succeeding tests.

[b] Injection time was measured from the time the charge valve started to open until it was closed again.

FIG. 251 CVTR CONTAINMENT PRESSURE RESPONSE -- HEISE GAGE.

volume. The measurements for the header cavity, which had an almost unrestricted flow area to the main containment volume of about 50 in.², showed essentially no difference in either peak pressure or time of peak pressure. In the fuel transfer canal, however, which had a connecting flow area of about 3 in.², a definite lag in time of peak pressure of about five seconds occurred during all three tests, which resulted in a pressure lag of approximately 1 psi. No differences in the magnitudes of the peak pressures were observed. The pressure lag indicates that for a highly compartmented containment with small connecting areas and a rapid blowdown situation, large pressure differences might result in structural damage.

2.22 Temperature History. Temperatures were measured at various locations in the CVTR containment. The atmospheric vertical temperature, being the most important, is discussed here. Containment atmosphere vertical temperature profiles were determined from temperature measurements obtained as nearly as possible at the same radius and angle from the containment center, but at different elevations. The vertical temperature profiles for Steam Tests 3 and 5, as determined by four thermocouples, are shown in Figures 253 and 254, respectively. As can be seen, during steam injection of both tests the data exhibited essentially the same trend; that is, they exhibited rapid temperature rise with the operating region having the highest temperature and the basement region having the lowest temperature. Following steam injection, however,

FIG. 252 CVTR CONTAINMENT PRESSURE RESPONSE -- PRESSURE TRANSDUCERS, TEST 3.

FIG. 253 ATMOSPHERIC VERTICAL TEMPERATURE PROFILE, TEST 3.

FIG. 254 ATMOSPHERIC VERTICAL TEMPERATURE PROFILE, TEST 5.

the temperature profiles become significantly different because of the activation of the containment spray system during Test 5. Without the spray, large containment temperature gradients existed for as long as data were taken (about two hours). The spray, however, tended to bring the entire containment to a uniform temperature^[a], with the rate at which the temperature approached uniformity dependent upon the spray rate.

2.23 Pressure Reduction Spray. Objectives of the CVTR DBA tests were to determine the containment response to, and the effectiveness of, a containment pressure reduction spray system.

The approximate spray pattern was determined by arranging spray catch cans on two quadrants of the reactor operating floor, and by measuring the amount of water in each catch can following each test. The spray distribution for Test 4 is shown in Figure 255. As can be seen, the nozzle pattern provided good coverage of the containment. Isolated regions (generally adjacent to the containment wall) had less water coverage than average; however, the least coverage measured was still about 0.022 gal/ft²-min.

Figure 256 illustrates the effect of the spray on the containment atmosphere temperature for the three DBA tests. Data from a single thermocouple located in the upper region of the containment volume were used for this figure and, as can be seen, the spray action significantly reduced the containment atmosphere temperature in this region with rate of decrease almost directly proportional to the spray flow rate.

Figure 257 shows the effect of spray on the containment pressure decay for the three DBA tests. The spray was effective almost immediately in reducing the containment pressure, and continued to be effective throughout the operation of the spray system.

The efficiency of the spray was determined by measuring the temperature of the spray at several heights in the containment operating region. Each temperature measurement was obtained from a thermocouple junction installed in the throat of a four-inch funnel that was insulated with polyurethane to reduce the influence of atmospheric temperature. During spray system operation, as spray water was collected by each funnel, the temperature of the spray was measured by the thermocouple in the throat of the funnel.

The temperature results of both spray tests indicate that the spray water was at thermal equilibrium with the containment atmosphere when it reached the nearest funnel, a distance of 31 feet. These results agree with reported heat transfer calculations^[150], indicating that for 1000-micron droplets, 99% reach thermal equilibrium within a travel distance of 12 feet.

Because the CVTR spray system was temporary and was required to operate for only two tests, it was constructed of black-iron pipe. Extensive nozzle plugging was experienced during the semi-DBA test, and plugging of

[a] The spray tended to equalize the overall containment temperature by decreasing the temperature of the operating region and increasing the temperature of the basement and intermediate regions.

- (a) A 3/4-inch Nozzle and a 1-1/2-inch Nozzle were Located at each A Nozzle Position
- (b) A 3/4-inch Nozzle and a 1-inch Nozzle were Located at each B Nozzle Position

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FIG. 255 SPRAY PATTERN, TEST 4.

FIG. 256 CVTR SPRAY EFFECTIVENESS, TEMPERATURE.

FIG. 257 CVTR SPRAY EFFECTIVENESS, PRESSURE.

two nozzles was experienced during Test 4. For permanent spray system installations, which may remain passive for years, the systems should be constructed of noncorrodable materials and should be kept dry during inactive periods.

2.24 Containment Liner Strain. Containment liner strain measurements were obtained at 83 locations with three-component rosette strain gages to provide backup data in the unlikely event of liner or containment failure.

- (a) Pressure Loading Analysis. Two separate analyses were performed to investigate the effects of pressure loading on the containment liner. The first method was a finite element axisymmetric stress analysis using the computer code SAAS II[151]. For this analysis, the liner, reinforced concrete, and surrounding soil were assumed to act as a continuum (no gaps between materials or cracks in the concrete). The reinforcing was modeled as thin bonded plates located at radii corresponding to the rebar positions.

The second analysis assumed that the liner acted as a separate thin-walled cylinder. That is, sufficient gaps were postulated to exist between the liner and concrete for the liner to carry the entire pressure loading. The actual conditions of the structure varied between these two extremes, and thus, the analysis provided upper and lower bounds for the actual stress responses.

Figure 258 is a comparison of the analytical stress curves and experimental data representative of the stress on the cylinder liner plates. Experimental strain measurements near containment penetrations and joints were not considered in determining the indicated average stresses. As can be seen, the slope of the average experimental stress curve decreases with increasing pressure, indicating that the known gap between the central portions of the steel liner plates and the concrete walls is gradually closing as the pressure increases. Therefore, the CVTR strain measurements indicate that the liner initially assumed essentially the entire pressure loading for the structure. As the containment pressure increased, and the gap closed, the reinforced concrete began to assume a larger portion of the load. The large spread in data of Figure 258 indicates the relative nonuniformity of the gap and other localized stress conditions.

- (b) Dome-Cylinder Transition Loading. A theoretical analysis of the containment liner strain in the intermediate region of the transition zone between the dome and the cylinder was performed through the use of the SAAS II computer program. No gap was assumed to exist between the liner and the concrete. The results of this analysis and the measured stress for a 21-psig containment leakage rate test[140] are shown in Table XLV. The lack of agreement between the two values is attributed to gaps between the liner (dome and cylinder) and the concrete.

Because of the method of construction of the cylindrical section of the containment, and as observed when two holes were drilled through the liner for the heat plugs, gaps were known to exist in this section of the containment. Therefore, the assumption of gaps in this region of the containment to explain the discrepancy of Table XLV is considered valid. The transition region plate

FIG. 258 COMPARISON OF ANALYTICAL AND EXPERIMENTAL MAXIMUM STRESSES IN THE CVTR LINER.

was also installed with a known air gap between it and the liner. Thus, the assumption is also valid for this region. The dome liner, however, was installed before the 20-inch concrete cover was poured, and any gap would be a result of concrete shrinkage upon curing. If a 1- to 20-mil gap is assumed to exist between the dome liner and concrete, the discrepancies of Table XLV can be explained. A gap of this size is considered not only feasible but probable. Thus, gaps are postulated to be the major reason for the discrepancies of Table XLV.

Calculations of the dome loading for a 21-psig pressure condition in which no concrete support was assumed resulted in a stress level of about 7,500 psi. Thus, the measured values again fall between the two boundary calculations.

TABLE XLV

COMPARISON OF ANALYTICAL AND EXPERIMENTAL STRESSES FOR THE
DOME-LINER TRANSITION ZONE FOR A CONTAINMENT PRESSURE OF 21 PSIG

Strain Gage	Strain Gage Location	Experimental Stress (psi)	SAAS II Analysis (psi)
45	Dome	3815	1712
46	Dome	3379	1763
47	Transition region	3756	1335
48	Liner	7399	1656
49	Liner	7288	1689
52	Transition region	-2519	1335
53	Liner	3603	1656

Liner stresses were found to be higher during DBA testing than the stresses measured at comparable pressures during the ambient temperature or the hot air leakage rate tests [140]. These higher stresses would be expected, however, since during DBA testing, the rapidly applied heat and pressure caused significant liner temperature gradients that added thermal stress effects to the loading. An examination of the stresses based on a Tresca yield criterion indicates that the higher stress levels produced yielding at all of the strain gage locations examined except one.

Current reinforced concrete type containment structures are generally constructed by using the steel liner as the internal concrete form. (The CVTR liner, except for the dome, was welded in place after the concrete structure was completed.) Use of the liner as a concrete form is expected to prevent large gaps between the liner and the concrete.

2.3 Containment Heat Transfer Properties

A primary purpose of the CVTR simulated DBA tests was to examine heat transfer processes within a representative power reactor containment under conditions approaching those that might be encountered during a DBA occurrence. No attempt was made to duplicate any particular postulated CVTR accident; instead, the tests were designed to result in conditions that would be within those which the containment structure was designed to withstand (215°F, 21 psig). The steam injection time and enthalpy were noted to be about 170 seconds and 1200 Btu/lb, respectively. Thus, the CVTR tests were representative of a 10- to 12-inch pipe break, but were not necessarily representative of the large break in a PWR for which primary system blowdown times and fluid enthalpies of about 15 seconds and 550 Btu/lb, respectively, are often considered.

The principal information used to determine the heat transfer behavior in the CVTR containment consisted of the following:

- (1) Containment wall interior temperature response (heat transfer assemblies)
- (2) Steel liner surface - bulk atmosphere temperature differences
- (3) Other heat sink surface temperatures
- (4) Condensation rates
- (5) Heat fluxes
- (6) Energy addition versus pressure and temperature response (energy balance)
- (7) Convective currents
- (8) Pressure reduction spray efficiency and spray effectiveness (temperature and pressure response measurements)
- (9) Steam migration times and stream distribution (high speed photography).

Detailed information related to the type and number of sensors and sensor locations is provided in Reference 142. Some of the more significant heat transfer measurements and results are discussed in following sections.

2.31 Wall Interior Temperature Response. Measurements of temperature gradients within the liner and concrete provide a means for analytically determining the effective heat transfer coefficient and heat flux at the containment liner surface. This section presents a discussion of the measuring system, experimental data, and analytical approaches used to determine surface heat transfer from interior wall temperature data.

- (a) Experimental Data from Heat Transfer Assemblies (Heat Plugs). The heat transfer process at the surface of the containment wall is due to dependent phenomena; both dropwise and filmwise condensation processes are possible but filmwise condensation is probably predominant. Measurements sufficiently detailed to identify and determine all of the localized heat transfer behavior were not practicable. However, since the overall coefficient of heat transfer across the containment boundary is of primary interest for containment response analysis, two special assemblies (heat plugs) were installed to obtain temperature data from which to determine this coefficient. A section of the containment wall with the heat transfer assembly installed is shown in Figure 259. Thermocouples were precisely located on the surfaces of the steel plug and in the atmosphere adjacent to the heat plug, as well as embedded in the steel disc and concrete plug. The steel disc was welded in place and the surface was finished to match closely the adjoining

FIG. 259 HEAT TRANSFER ASSEMBLY.

section of the liner. Both installations were in the operating region with Heat Plug 1 being about 4 feet above the operating floor (elevation 348 feet, 4 inches) and nearer the level of major steam injection.

Figure 260 illustrates part of the temperature history for Heat Plug 1 during Test 5. The temperatures of the surface of the steel disc, the first point within the disc, and the first point in the concrete are compared with the average bulk atmosphere temperature.

- (b) TAEH Heat Conduction Program and Calculation Results. An inverse heat conduction code, TAEH, was developed in the CVTR program for analysis of the data from the heat plugs [142]. The code solves the inverse problem of heat conduction for the calculation of unknown surface conditions from known internal temperature behavior. In particular, Duhamel's theorem is solved by a numerical inversion technique. The code calculates surface heat fluxes, temperatures, and effective heat transfer coefficients.

Heat transfer coefficient curves, calculated by TAEH are shown for Test 3, Heat Plugs 1 and 2, and for Test 5, Heat Plugs 1 and 2 in Figures 261 and 262, respectively. As can be seen, the heat transfer coefficients obtained from Heat Plug 2 reach a maximum earlier in both cases than those from Heat Plug 1; and, the maximum occurs prior to termination of steam injection into the containment. This difference in behavior is attributed to the difference in heat plug locations. Heat transfer at Heat Plug 2 apparently reaches a

FIG. 260 TEMPERATURES DURING STEAM TEST 5, HEAT PLUG 1.

FIG. 261 TAEH-CALCULATED HEAT TRANSFER COEFFICIENT, STEAM TEST 3.

FIG. 262 TAEH-CALCULATED HEAT TRANSFER COEFFICIENT, STEAM TEST 5.

limiting process prior to steam shut off. This limiting process may have been a result of either a conduction-limited heat transfer process occurring in the concrete or air gap behind the liner or a localized diffusion-limited mass transfer process occurring in the boundary layer. Also, the different times of formation of a condensate film and the different film thickness due to the difference in wall height influence the observed differences in the temperature behavior for the two locations. In any event the results indicate differences in heat transfer behavior for different locations even within the region of steam turbulence. Plug 2 was located about 20 feet higher on the liner wall of the operating region than Plug 1, and was nearly at the level of major steam exhaust from the diffuser section and apparently in a much more turbulent region of the steam-aid atmosphere. Higher atmosphere and liner temperatures (approximately 20°F) were measured in the region of high elevation near Heat Plug 2, the bulk atmosphere temperature reached a maximum value sooner, liner temperatures increased more rapidly, and temperature differences between the bulk atmosphere and the liner decreased more rapidly.

The TAEH code was designed to analyze the temperature history at one or several internal slab spatial points, and the calculation can be performed using slab, cylindrical, or spherical geometry. The results in Figures 261 and 262 are for cylindrical geometry and one average internal temperature. For Heat Plug 1, the internal temperature was the average of the data from the first five liner internal thermocouples, whereas generally for Heat Plug 2, an average of the data from only the first two internal thermocouples was used because of known uncertainties in the data from the other thermocouples. In all cases, an average of the bulk measurements near the face of the plugs was used for the bulk atmosphere temperatures. No significant change in results was noted from the calculation using a different code geometry, but the results are dependent on the meshpoint assignment for the average temperature.

In general, the heat transfer coefficients will be larger by up to about 10% if the assignment is near the face of the liner or if the temperature history from the first thermocouple inside the liner is used.

The TAEH results are for point measurements at two locations in the containment. Heat transfer rate differences were noted and these differences appear dependent upon measurement locations relative to the steam exhaust. The dashed lines of Figures 261 and 262 are an attempt to form an average coefficient for the containment wall in the operating region based on these data. The solid line of Figure 261 is the pretest best estimate heat transfer coefficient based on the best available assumptions and common usage within the industry. This pretest estimate was used for pretest CVTR containment response predictions.

2.32 Liner Surface-Bulk Atmosphere Temperature Differences. Temperatures of the liner surfaces were measured at various locations throughout the containment. Also, several bulk atmosphere temperatures were recorded in each region of the containment. These temperature measurements were used to determine heat transfer coefficients at locations in the containment. The basis for determining these coefficients was comparison of both the time dependent differences of the liner and bulk atmosphere temperatures with corresponding differences from the heat plugs and calculations described in the following section.

To use the temperature data for calculations, approximations such as assignment of an average atmosphere temperature to the bulk atmosphere of the basement region or atmosphere temperatures measured in the proximity of a liner temperature were required. Because small changes in temperature differences heavily influence heat transfer calculations, these approximations should be recognized as potentially capable of leading to serious inaccuracies. For the previously discussed heat plug data and the TAEH calculations, this type of inaccuracy was reduced by using several bulk temperature measurements close to the face of the plug.

Results of Steam Test 3 are compared in Figures 263 and 264 with the TAEH calculations for the heat plugs. The heat transfer coefficients of Figure 263 are a sample of a large number of such determinations for the operating region. Variations in rates of liner temperature increases within the operating region are indicative of significantly different heat transfer rates from point to point even within relatively open volumes. Some of these differences between heat transfer rates for this region can be correlated with impediments to steam distribution such as the refueling machine, or crane structure, or distance from point of injection. The heat transfer coefficients for the basement region (Figure 264) are observed to be much smaller than those for the upper intermediate and operating regions and are in keeping with the restrictions to the flow of steam to the lower regions. In the intermediate region, the apparent discrepancy of higher heat transfer rates for the thermocouple located nine feet under the operating floor compared with the rates for the thermocouple located two feet under the floor is due to the lower thermocouple being exposed more directly to steam through the annulus whereas the higher thermocouple was located near conduit, piping, and other structures.

FIG. 263 OPERATING REGION HEAT TRANSFER COEFFICIENTS FROM LINER SURFACE-BULK ATMOSPHERE TEMPERATURE DIFFERENCES, TEST 3.

FIG. 264 BASEMENT AND INTERMEDIATE REGIONS HEAT TRANSFER COEFFICIENTS FROM LINER SURFACE-BULK ATMOSPHERE TEMPERATURE DIFFERENCES, TEST 3.

2.33 Condensation Rates. During the DBA tests, measurements were made to determine the rate at which condensation formed on the containment liner surfaces. The condensate collecting system consisted of one-foot-wide troughs welded to the containment wall at selected locations. An area one foot wide extending from each trough location to the bend line of the containment was bordered with a bead of Dow Corning Compound III. Condensate forming and running down the outlined area was routed through plastic tubing to catch cans.

The condensation rate measurement was provided by suspending the catch cans from specially designed load cells. As the condensate was collected, the output of the load cell was recorded continuously. Also, at the completion of each test, the collected condensate was weighed.

The condensation rate measurements are distributed measurements over large surface areas. An accurate time correlation between the condensation rate data and the actual test time was not obtained because the test time basically involved the unknown time for the formation of a film, flow down the wall, and flow through the trough and tubing of the collection system. Typical condensation rate results are shown in Figure 265 for Steam Test 3 with a best estimate for timing. Reasonably high condensation rates are observed. At 15 minutes, the total condensate collected is 10,000 ml (22 lb) of water or well over 2-1/2 gallons for this 31-ft² strip. About 10,600 ft² of liner area for the containment cylindrical section is above the operating floor; therefore, on the basis of the data obtained from the areas sampled, about 850 gallons (7120 lb) of condensate formed on this area of the liner in the 15-minute test period. The total amount of water weighed following test completion for this strip was 12,700 ml which extrapolates to 9565 pounds of condensate for the upper liner surface area. A CONTEMPT^[85] calculation with the TAEH average heat transfer coefficient predicted the mass of condensate at 18 minutes to be about 12,000 pounds which agrees reasonably well with the experimental data. The cylindrical section of the containment on which Figure 265 is based is only a part of the total surface in the containment available for condensation. The rates elsewhere in the containment are dependent on orientation of the surfaces on which condensate forms. Photographic evidence of the behavior of condensation on the glass windows of camera boxes indicates that dropwise condensation may have occurred during the initial part of the CVTR tests. However, the special surface conditions, such as an oil film, necessary for promoting and maintaining this type of condensation, and the fact that these surface conditions may have been eliminated by the first presence of steam, strongly suggest that the predominant behavior was filmwise condensation.

2.34 Heat Transfer Coefficient Estimate. From the results and considerations of previous sections, an estimate of the CVTR heat transfer coefficients is formed primarily for the purpose of providing input data for the containment response analysis to follow. The reader is cautioned that the features of heat transfer discussed here are modified by the results of the analysis, and the best estimate for the heat transfer coefficient should be concluded following that discussion.

The heat transfer behavior obviously varied from location to location throughout the containment, apparently even in relatively open volumes. This behavior can be related to steam distribution and flow obstructions in many cases, and although the behavior may have been exaggerated by the CVTR

FIG. 265 CONDENSATION RESULTS FOR STEAM TEST 3.

geometry and steam injection system, it should be expected in varying degrees for any primary system blowdown into large containment volumes. Two approaches to a suitable heat transfer estimate can be taken: (a) an average coefficient may be assigned uniformly as is done in common usage or (b) a more realistic model of the actual heat transfer distribution may be used. Both approaches are examined by the analysis in Section IX-2.42. From former sections, the magnitude of the heat transfer coefficient has also been observed to differ among the different calculational methods. In most cases, the techniques gave values somewhat larger than the TAEH calculated values. However, the TAEH values are considered the most reliable, even though deficient, in that sampling at only two points was possible. For the operating region, the TAEH average coefficient is adopted as a first estimate of the behavior for the region. From the temperature data and heat transfer results, the heat transfer in lower regions is considerably reduced from that of the operating region. Time dependent coefficients of reduced magnitude for the intermediate and basement regions would be applicable but, as will be discussed, cannot be incorporated into the CONTEMPT containment code (Section 2.42). Consequently, constant values for heat transfer coefficients for lower containment regions are assumed.

2.35 Comparison of Heat Transfer Coefficient Results with Those Used by Industry. A review of heat transfer coefficients for condensing steam in a containment building following a loss-of-coolant accident has been completed [87]. The uncertainty that exists in the selection of an accurate empirical correlation for the heat transfer coefficients in large power reactor containments was underlined by this review. The Tagami correlation [95,152] was recommended for use until improved data are available because of its apparent trend to conservatism.

For containment analysis, two separate time periods following a loss-of-coolant accident are often considered during which two different condensing steam heat transfer coefficient correlations are required. The first period is characterized by high turbulence caused by decompression of the primary coolant system and is often identified as the forced convection, turbulent or blowdown period; that is, the transient state. The second period is the natural convection, postblowdown, or steady state period during which turbulence is considerably reduced.

Current industry usage indicates a preference for a heat transfer model based on the Tagami correlation for the maximum coefficient on steel surfaces. Various assumptions are used for a smooth transition from the pre-accident steady state coefficient to the maximum value predicted from the Tagami correlation. The natural convection data of Uchida [96], which are based on a weight ratio of air to steam is often used following the peak value from the Tagami correlation. Little information regarding heat transfer coefficients on concrete surfaces exists, and a value of 40% of the steel values and other estimates believed to be conservative are often used.

The Tagami correlation can be expressed as

$$h_{\max} = 72.5 \left(\frac{Q}{V t_p} \right)^{0.62}$$

where

$h_{\max}$ = the maximum heat transfer coefficient during blowdown
(Btu/hr-ft²-°F)

Q = the total energy released from primary system during
blowdown (Btu)

V = the free volume of the containment vessel (ft³)

t_p = the time interval until peak pressure (sec).

For Steam Test 3, $Q \cong 1.97 \times 10^7$ Btu, $V \cong 141 \times 10^3$ ft³ (above the operating floor), and $t_p \cong 167$ sec such that the Tagami correlation predicts a maximum heat transfer coefficient of about 65 Btu/hr-ft²-°F. The choice of volume in this case corresponds more closely with the blowdown or turbulent region. The normal assumption of total free containment volume would result in a lower predicted maximum heat transfer.

The Uchida coefficients can be determined from estimates of the air-to-vapor weight ratios. The weight of air initially in the containment was approximately 1.64×10^4 lb and leakage of air during the test period was assumed to be negligible. The amount of vapor in the containment is calculated by CONTEMPT. The amount of vapor air-to-steam ratio, and corresponding predicted coefficients from Uchida data are given in Table XLVI for a few time points from a typical calculation from Steam Test 3. The mass of vapor from CONTEMPT depends heavily on assumed heat transfer coefficients and Table XLVI reflects calculations using the TAEH average heat transfer from the previous discussion.

TABLE XLVI

UCHIDA HEAT TRANSFER COEFFICIENTS

<u>Time (sec)</u>	<u>Steam (lb)</u>	<u>Ratio of Air to Steam [a]</u>	<u>Uchida Coefficients (Btu/hr-ft²-°F)</u>
3.6	240	68.3	2
10.8	718	22.8	7
18.0	1395	11.7	12
25.2	2011	8.2	16
39.6	3036	5.4	20
61.2	4120	4.0	24
90.0	5248	3.1	29
118.8	6469	2.5	34
165.6	8805	1.9	44
327.6	6597	2.5	34
471.6	5854	2.8	31
615.6	5520	3.0	29
759.6	5207	3.1	28.5
903.6	4888	3.4	27
1047.6	4574	3.6	26

[a] Basis - CONTEMPT calculation for Test 3; TAEH average heat transfer coefficient with uniform assignment and initial conditions of 1.642×10^4 pounds of air, 1.79×10^2 pounds of water vapor, and no containment leakage.

The heat transfer coefficients determined from the Tagami correlation and Uchida data for CVTR are representative in magnitude of values which would be predicted by common practice in the industry. Clearly, the predicted coefficients are smaller than those derived from the CVTR measurements.

2.4 Containment Response Analysis

Three containment response codes were employed for the CVTR calculations: CONTEMPT^[85], CONDRU I, and CONDRU II^[a].

The CONTEMPT computer program was initially developed to predict the pressure-temperature response of a dry containment building to a loss-of-coolant accident (current versions of CONTEMPT also include the option of calculations for a pressure suppression containment). The model is concerned only with the pressure and temperature response of the containment volume and the temperature throughout the containment structure. The computer program input has provision for the description of several heat absorption slabs and for the description of the discharge of coolant, the boiling of residual water by decay heat, the superheating of steam passing through the core, and metal-water reactions. The program also calculates the effect of building leakage and the effects of engineered safety devices such as pressure reduction spray. CONTEMPT uses a single-node analytical model such that a single bulk atmosphere temperature and pressure prediction representative of the average response behavior is provided at each calculation time point.

In comparison with CONTEMPT, CONDRU I is a fairly simple single-node model that uses the more conventional assumption of a homogeneous mixture and equal temperatures of water, vapor, and air throughout the containment volume. Similarly to CONTEMPT, CONDRU I considers heat exchange between the containment atmosphere and walls or internal structures and calculates pressure and temperature after each time step from a mass and energy balance for the total system.

Both CONTEMPT and CONDRU I are basically one-volume codes. The CVTR is divided by an operating floor into two distinct volumes or compartments with an interconnecting flow path. The upper and lower volumes are 141,328 ft³ and 85,590 ft³, respectively, and the flow path is 329 ft². Thus, the CVTR geometry is representative of containment compartmentation into large volumes. The CONDRU II code was developed as part of the CVTR work to represent more realistically the CVTR containment and examine compartmentation response. Specifically CONDRU II is a two-node modification of CONDRU I with the capability of calculating the pressures and temperatures in a containment made up of two volumes (compartments) connected by a flow path. Pressure and temperature in both compartments are evaluated in the same way as in CONDRU I. In the first compartment, an energy addition from the coolant blow-down, the chemical reaction, or the spray water addition can be considered by input tables. In either compartment, heat exchange among an arbitrary number of structures is allowed. To account for separate vapor-to-air ratios, each compartment can have a different table of heat transfer coefficients. The flow area between the compartments can be changed either as a function of time or pressure. Either an isentropic or quasi-steady state flow calculation is made for the vapor, water, and air vent flow between the two compartments. Thus, CONDRU II can evaluate pressure differentials in a two-compartment system and simultaneously consider energy addition, water spray addition, and heat exchange among internal structures.

[a] The CONDRU codes were written by H. G. Seipel, a West German National on assignment to Phillips Petroleum Company through an AEC training agreement, and are an extension of earlier work by Mr. Seipel and others.

2.41 Posttest CONTEMPT Calculations. The CONTEMPT code was the principal analytical model evaluated during the CVTR work because of its widespread usage. For evaluation of CONTEMPT, necessary code input data, such as heat transfer coefficients or steam injection rates, were derived from measured data and used in obtaining response predictions. The predicted responses, such as pressure and temperature, were compared with corresponding measured response data. In some cases in which obvious differences occurred between and calculated and measured response, such differences were evaluated in terms of whether the fault was in the code or in the input data. Thus, to a limited degree, CONTEMPT was also used as an investigative tool to aid in defining the heat transfer properties. The discussion of representative results is centered around heat transfer considerations (Section IX-2.3).

- (a) Pressure Calculations. The containment pressure for Steam Test 3 is given in Figure 266 for various heat transfer coefficients. The upper trace represents a calculation in which the pretest heat transfer coefficient used was that illustrated in Figure 261. The steam injection rate and steam conditions used in this calculation are the measured values. As compared to the measured pressure history (also included on the figure), the calculation with the pretest heat transfer coefficient overpredicts the pressure peak by about 8 pounds, or 45%.

The pressures calculated for the time dependent TAEH average heat transfer coefficient in the operating region and constant values for heat transfer in lower regions are also shown on the figure. From Section IX-2.3, the heat transfer to the lower regions was found to be time dependent but of reduced magnitude as compared with that of the operating region. A deficiency of the CONTEMPT code is the inability to include more than one time dependent heat transfer coefficient. The restriction is in keeping with the single-volume model assumption. With this restriction, the assignment of constant values for the heat transfer coefficient of 20, 40, and 60 Btu/hr-ft²-°F to the concrete in all lower regions, steel in basement, and steel in intermediate region, respectively, was an attempt to more accurately model the heat transfer distribution of the containment. As seen from Figure 266, the calculated pressure still exceeds the measured data by about 3 pounds or 17% at the peak.

Finally, Figure 266 presents calculated results for uniform assignment of the TAEH average heat transfer coefficient to all heat absorbing structures. This heat transfer assumption is identical to common practice in containment analysis but is surely erroneous for the CVTR case because of the known reduced heat transfer in the lower regions. Nevertheless, the level of agreement between calculated and measured results is considerably improved.

To illustrate another feature of the calculation with the average TAEH heat transfer coefficient assigned uniformly, the results are extended in time to 18 minutes and are presented in Figure 267. For the lower curve, the heat transfer coefficient has been held at a constant value of 30 Btu/hr-ft²-°F commencing at a time of about 200 seconds. From Table XLVI, this assumption would seem to correspond fairly well with the Uchida data for the air-to-steam ratios of the test. However, apparently, such an assumption is incorrect for the case of CVTR and will result in the actual pressure being

FIG. 266 STEAM TEST 3, PRESSURE CALCULATIONS -- 0 TO 6 MINUTES.

underestimated over the long time period. For the top trace of Figure 267, the heat transfer coefficient has been reduced in the pressure decay period as indicated. For example, at a time of 9 minutes, the heat transfer coefficient was assigned a value of 5 Btu/hr-ft²-°F until termination of the calculation. The shape of the curve indicates that this heat transfer coefficient is more nearly correct; however, it possibly should have been assigned at a time later than 9 minutes.

The TAEH average heat transfer coefficient with uniform assignment has been shown (Figure 266) to give a reasonable approximation to the measured pressure curve. However, as discussed, the heat transfer in lower regions for the CVTR tests obviously should be reduced in magnitude compared to that of the upper region. Also from Figures 266 and 267, a suitably large heat transfer coefficient for calculated and measured peak pressure agreement has not been achieved. Two possibilities are that the heat transfer in the upper region is somewhat larger on the average than that given by the TAEH average data, or, that the amount of steel in the containment has been underestimated. This latter possibility is a very real one in that the large amount of piping, ventilation ducts, gratings, tanks, and miscellaneous gear are difficult to estimate or incorporate as heat slab code input data in an accurate fashion. The procedure was simply to incorporate only major components and to use

FIG. 267 STEAM TEST 3, PRESSURE CALCULATIONS -- 0 TO 18 MINUTES.

the added steel as a parameter. Additional calculations, the results of which are shown in Figure 268, were performed for Test 3 to investigate these possibilities. The upper curve is from a calculation in which the TAEH-generated heat transfer coefficient is from Heat Plug 2 and corresponds to an increase in the heat transfer in the upper region. The constant heat transfer coefficient assignment to the lower regions is identical to that of the earlier discussion. Some improvement in the agreement between the calculated and measured peak pressures is observed by comparison of results with those of the similar run with the TAEH average coefficient of Figure 267. However, the improvement is small such that increases in the heat transfer coefficient of the upper region would appear to be an unlikely solution. An additional observation is that the constant heat transfer coefficient assignment for the lower regions is a poor assumption in the long time sense as indicated by the incorrect calculated pressure decay curve.

To investigate the effect of underestimating the amount of steel in the containment, the quantity of steel was increased by 20% in the operating regions and by 50% in the lower regions. These increases are considered to be the upper limit of possible inaccuracy in determining the quantity of steel in the containment. For both the operating and lower regions, the steel increase was assigned as an increase in the area of an existing heat absorbing steel slab and given a thickness of 1/2 in. Calculated results for the case of increased steel are shown in Figure 268.

FIG. 268 STEAM TEST 3, PRESSURE CALCULATIONS WITH STEEL CONSIDERED.

The peak pressure agreement is improved but the pressure during the decay period is obviously in error. The erroneous pressure during this period is believed to be primarily due to the constant heat transfer assignment in the lower regions. For the case in which the TAEH average heat transfer coefficient was assigned uniformly to all regions along with the increase in steel heat slabs, too much heat transfer is exhibited.

Figure 269 shows the results of two calculations compared to the experimental data for Steam Test 4. For Test 4, only Heat Plug 2 was functioning; thus, the calculation uses the TAEH determined heat transfer coefficient from Plug 2 with uniform assignment to all regions. The upper trace was obtained by using the measured steam flow rate data. The lower trace shows the results obtained by assuming a 2-1/2% inaccuracy in the steam flow rate measurements; the inaccuracy of the steam flow measurements could be as great as 6%.

Again, from the results of Test 4, very little heat transfer in the decay period is indicated by the values at the 6- and 7-1/2-minute times. For this calculation, the coefficient was about 4 Btu/hr-ft²-°F at 18 minutes and was reduced to 3 Btu/hr-ft²-°F at 30 minutes. The code performed a tabular interpolation for needed values at other times. Even at these low values the

FIG. 269 STEAM TEST 4, PRESSURE CALCULATIONS.

heat transfer coefficients are too large in the decay period for this test. Apparently, the pressure suppression spray water employed for this test acted as an effective heat sink such that heat transfer to structures was reduced nearly to zero.

- (b) Temperature Calculations. CONTEMPT-calculated temperature results are compared with experimental data from Steam Test 3 in Figure 270. The experimental bulk atmosphere and surface temperatures for the operating region shown in this figure are from Heat Plug 2 and are nearly representative of the average behavior for the operating region. Temperatures at two locations in the intermediate region are shown on the figure to illustrate the large differences noted in this region. The temperature of the basement region is representative of the average behavior in this region. CONTEMPT, being a single volume code, calculates a single temperature, shown by the dashed line in Figure 270, which in this case is approximately a volume-weighted average of the temperature behavior throughout the containment. As can be seen, the experimental maximum temperatures are underestimated by CONTEMPT. This underestimation was not particularly serious in the case of CVTR because the temperature of the injected steam

FIG. 270 STEAM TEST 3, TEMPERATURE RESULTS.

was relatively low (about 380°F), the injection rates were low, the volumes relatively open, and interconnecting flow paths large. However, for fast blowdowns of high temperature water (approximately 600°F) and blowdowns in smaller volumes, or blowdowns for which more serious flow restrictions exist, temperature predictions may be seriously in error and localized heating exceeding design limits may result.

2.42 CONDRU II Calculations. As noted in the previous discussions, actual steam injection conditions result in special temperature (and pressure) gradients that need to be taken into account in the plant design. The two-node code, CONDRU II, was developed to predict pressure and temperature gradients in containment systems. An example of CONDRU II posttest calculations is shown in Figure 271. For this calculation, the TAEH average heat transfer coefficient was assigned to the operating region (Compartment 1) and a second time-dependent heat transfer coefficient was assigned to the lower regions (Compartment 2). The heat transfer coefficient for Compartment 2, which was nearly the average of the two curves shown for the intermediate region in Figure 264, had a peak value of 115 Btu/hr-ft²-°F at about 135 seconds. Good temperature agreement is noted for the operating region whereas poor agreement is evidenced for the lower regions. Again, the one node assigned to the lower region was found to be insufficient for accurate representation of the CVTR temperature distribution and the need for development of a several-node model is indicated.

FIG. 271 CONDUR II TEMPERATURE CALCULATION RESULTS FOR STEAM TEST 3.

2.5 Miscellaneous Results

Experimental information concerning containment insulation, containment coatings, a fixed leak system, containment convective currents, and the gross effects of DBA testing on the containment was obtained during the DBA tests.

Reinforced-concrete containment vessels used for nuclear power reactors frequently are provided with thermal insulation on the bottom 20 feet of the inside surface of the liner to reduce the stress contribution from thermal effects at the junction of the wall and base slab in the unlikely event of an accident. To examine the effectiveness of this technique, an 8-foot high by 30-foot long section of the CVTR containment liner in the basement region was insulated with a two-inch-thick sheet of polyurethane (U-200, $\rho=2.8 \text{ lb/ft}^3$). Thermocouples and strain gages were placed on the liner surface under the insulation and also on an adjacent uninsulated section of the liner.

The results from the thermocouples were that the insulation reduced the temperature. The results from the strain gages located beneath the polyurethane also indicated a reduced strain as compared with strain results for the bare liner. Thus, insulation provides protection to the liner from DBA effects. The more rapid the atmospheric temperature increase and the higher the peak temperature value, the greater the potential protective effect of the insulation.

The insulation will reduce the total energy absorbed by the liner and thus cause a higher peak containment pressure during a DBA. A trade-off evaluation is thus required to determine the optimum design.

Visual observations and photographic records were made of the containment to evaluate degradation effects associated with DBA testing. Two major effects were noted, neither of which compromised the containment integrity and both of which are primarily associated with subjection of the containment to elevated temperature.

Hair-line cracking on the outside concrete surface which had been observed during the 21-psig, 200°F hot-air leakage rate tests [145] became noticeably larger during the DBA tests. The more extensive cracking at this time is attributed to the higher temperature (~240°F) associated with the DBA tests.

External visual measurements with a theodolite, internal mechanical deflection measurements and external containment wall surface expansion measurements with strain gages were made to determine containment growth during the leakage and DBA testing. Indications from the measurements were that the vessel cross section elongated in the east-west direction with little or no growth in the north-south direction and that the maximum elongation was less than 0.5 inch.

2.6 Conclusions

The principal conclusions derived from the CVTR containment DBA tests involve the condensing-steam heat transfer behavior, containment compartmentation, pressure suppression spray results, containment integrity, and the evaluation of current analytical practices. The need for additional analytical and experimental efforts was concluded if an improved assessment of the safety margin of reactor containment systems is required.

2.61 Heat Transfer Behavior. Maximum values for the condensing steam heat transfer coefficients were determined experimentally from the DBA tests to be a factor of three to five larger than would be predicted from the often-used Tagami correlation [95]. These coefficients were subsequently substituted into CONTEMPT and the CONTEMPT response calculations obtained using the coefficients agreed with the measured response thereby verifying that the coefficients were of the right magnitude. Following steam shutoff and during pressure decay, the heat transfer coefficient rapidly decreased to values significantly less than those predicted for these conditions from the commonly-used Uchida data [96]. Otherwise, the heat transfer behavior was consistent with common assumptions used in the nuclear industry (that is, the heat transfer coefficient is initially small and increases rapidly to a maximum value during blowdown as mixing and turbulence develops, followed by a rapid decrease to low values after completion of blowdown).

2.62 Containment Compartmentation. The potential importance of compartmentation in containment response analysis was re-emphasized by this work (that is, containment response may not only be dependent on the total energy addition and addition rates, but also on blowdown locations and orientations). Temperature differences between regions and even within regions were recorded during the CVTR tests. This phenomenon occurred even though the

CVTR containment volume is relatively open with reasonably large connecting flow paths. These experimental results indicate that fast blowdowns in relatively isolated volumes within a containment may lead to localized heating. Another potential concern in compartmentation is that fast blowdowns in enclosed volumes or volumes with restricted flow paths may lead to differential pressures. However, no significant differential pressure behavior was noted in the CVTR results.

2.63 Pressure Reduction Spray. The pressure reduction spray was demonstrated to effectively lower the pressure and temperature of the containment atmosphere following the simulated DBA steam injection. Experimental temperature behavior showed that the spray effectively promoted atmosphere mixing and temperature equalization throughout the containment. From comparisons between CONTEMPT calculated response behavior and experimental behavior, and from spray efficiency temperature measurements, spray efficiencies were determined to be very large initially (nearly 100%). Although spray efficiency probably decreases as a function of time and temperature reduction in the containment, such decreases were not detected by the CVTR temperature measurements.

2.64 Containment Integrity. Containment integrity was not compromised by these tests. In fact, leakage rate tests performed at the conclusion of DBA testing indicated that the leakage rate may have been slightly reduced by the DBA tests. However, strain gage measurements of the containment liner during the DBA tests indicated that the liner yield stress was approached. This behavior is thought to be associated with the manner in which the liner was erected (concrete poured first), and the consequent air gaps existing behind the liner. Thus, portions of the liner carried a significant amount of the load associated with containment pressurization instead of the reinforced concrete as intended. The current practice in constructing reinforced concrete containment is to first erect sections of the liner and then to pour the concrete. A smaller air gap between the liner and concrete would be expected through use of this procedure such that the liner loading, and stress, would be reduced.

2.65 Evaluation of Current Analysis. The evaluation of current analytical practices was based on the results of CONTEMPT containment response calculations. CONTEMPT is considered to be representative of the state-of-the-art in the industry.

The CVTR DBA tests and analysis have demonstrated that the CONTEMPT containment response analytical model will predict reasonably accurately the containment atmosphere pressure-temperature response, provided correct assignment of condensing steam heat transfer coefficients and other key parameters is made. But, when common assumptions currently used for the heat transfer coefficient (Tagami or similar) were employed, the CVTR DBA test results indicated that the peak pressure prediction obtained from CONTEMPT was conservative by about 45%. However, the magnitude of the overprediction of pressure depends on the blowdown conditions and other factors such that for a large pipe rupture (or fast blowdown), underestimating the heat transfer coefficient probably has a lesser effect on the peak pressure predictions (15% or less from Reference 86).

CONTEMPT, being a single-node model, provides an average temperature and pressure response prediction. For calculations of the CVTR test temperature, CONTEMPT significantly underestimated the actual peak temperature in the operating region but overestimated the temperatures in the lower region. No pressure differential behavior was noted for the CVTR steam injection conditions and the single pressure prediction capability of CONTEMPT was sufficient to accurately describe the measured results.

2.66 Additional Analytical and Experimental Requirements. The CVTR DBA tests provided information related to containment response, heat transfer behavior, and response analysis generally applicable to a DBA resulting from a medium-sized pipe rupture. However, similar representative containment response and heat transfer data are also needed for the large pipe rupture or fast blowdown case if an improved assessment of response predictions is required. Heat transfer behavior different from that of CVTR tests could be encountered for fast blowdown conditions such that a strict extrapolation of CVTR data to analysis of containment response for a large pipe rupture case is difficult. Also, the potential problem area of differential temperature and pressure conditions under fast blowdown conditions may be significant. Pressure differentials may be limited by engineering good flow communication between volumes but localized heating effects may be unavoidable.

The CVTR DBA tests have pointed to the possible improvements a multi-node analytical containment response model could provide for accurate assessment of containment response and margin of safety in containment design. The success of the CONDRU II model that was developed and utilized with the CVTR work, has indicated the feasibility of the multinode model approach. However, to adequately describe all complex features of containment response may require many nodes, and a model incorporating such nodes could become undesirably complex. Thus, some limitation on this approach is implied.

Uncertainties in containment response analysis surely exist as has been pointed out. Furthermore, these uncertainties exist even though much of the input information needed for CVTR analysis, such as the steam injection history, were relatively well known from experimental measurements. Also, the reported analytical results for the CVTR response are a summary of many calculations investigating the influence of all applicable parameters. As a result of these observations, similar analyses done for other facilities are expected to have comparable (or possibly greater) uncertainties.

Finally, these tests indicate probable conservatism in the practices currently employed for containment response. Localized behavior exceeding design limits is a possible exception. Some indication of the possibility of excessively high localized temperatures was provided by CVTR results. Additional effort in containment response investigation could be expected to benefit the designer in terms of relaxed design restrictions or increases in margin of safety.

X. RELIABILITY AND FAULT TREE APPLICATION

(W. E. Vesely)

Fault tree methods are used at the National Reactor Testing Station (NRTS) to obtain both qualitative and quantitative information regarding the safety and reliability of reactor safety and control systems. The fault tree analysis, when performed with the proper input information, depicts all the primary causes of control or safeguard system failure. The particular analytical technique used to obtain the quantitative, probabilistic information is the kinetic tree theory technique.

This technique has been described in other publications^[153,154]; therefore, only a brief description of this methodology is discussed in this section. In essence, once the fault tree has been drawn, the kinetic tree theory technique obtains not only the minimal cut sets (critical paths) of the fault tree, but also the following probability characteristics:

- (1) The probability of no system failure to time t
- (2) The probability of the system being in a failed state at time t
- (3) The failure intensity at time t
- (4) The expected number of failures to time t (integral of failure intensity)
- (5) The failure rate at time t .

These probability characteristics are obtained for any number of time points t , and hence time-dependent probability curves are obtained. Examples of the information that can be obtained from these curves are the degradation of availability with time or the decrease of the probability of no failure as total operating time increases. If only a particular total operating time, t_0 , or the state of the system at a particular time t_0 is of interest then the information at the particular time t_0 is used. The probabilistic information versus time curves thus yield not only the quantitative information at a particular time of interest, but also the complete time-dependent behavior of the reliability or safety characteristics.

The time-dependent information is obtained for each component of the fault tree, for each minimal cut set of the fault tree, and for the top event of the fault tree.

The time-dependent information obtained for the minimal cut set yields the most probable modes in which the system will fail (that is, the most probable modes by which the top event occurs)^[a]. A minimal cut set or critical path

[a] The "system failing" and "top event on the fault tree occurring" are equivalent expressions.

is defined as a smallest set of components that must all fail in order for the system to fail. A minimal cut set is thus a particular mode by which the system failure occurs. The unique minimal cut sets of the fault tree are consequently all the unique modes by which system failure occurs.

The information obtained for each of the minimal cut sets of the fault tree thus yields the minimal cut sets of most importance -- those which will most likely cause system failure. System redesign, maintenance, or repair would then be directed toward these few, most important minimal cut sets.

For the system or top event of the fault tree, the characteristics obtained from kinetic tree theory completely quantify the safety or reliability of the system.

The characteristics obtained for the components, minimal cut sets, and the system are straightforward and physical in interpretation. The interpretations corresponding to the probability characteristics are as follows:

- (1) The system reliability corresponds to the probability of successful operation during the interval from $t = 0$ (the beginning of operation) to some time t
- (2) The unavailability of the system at time t corresponds to the probability of the system being in a failed state at that time
- (3) The expected number of times the system will fail in the interval from $t = 0$ to some time t corresponds to the actual number of failures, including failures whose cause has been repaired
- (4) The system failure intensity, which when integrated gives the expected number of failures. The failure intensity when plotted against time t corresponds to the most probable time at which system failure will occur
- (5) The inverse of the system failure rate corresponds to the mean time to failure (MTTF).

Additional probability quantities, such as the mean time between repairs, are also obtained from the probability characteristics.

To obtain the described time-dependent characteristics for the components, minimal cut sets, and for the system, simple input data are needed. If only the minimal cut sets are desired, then only the fault tree, as drawn by the engineer, is needed. If, in addition, the time-dependent characteristics are desired, then the failure rate for each component (or its mean time to failure) and its repair time, if the component is repairable, are all that are needed in addition to the fault tree. The computer code package PREP and KITT has been written to apply kinetic tree theory in order to automate completely the process of obtaining the minimal cut sets and the time-dependent characteristics.

The codes PREP and KITT are written in Fortran IV for the IBM 360/75 computer and can handle fault trees consisting of up to 2000 gates and up to 2000 components and inhibit conditions^[155]. PREP and KITT can handle any assortment of repairable and nonrepairable components on the fault tree. The codes can treat single-phase problems having the component failure rates and repair time constant within a time phase, but variable from phase to phase. For these multiple single-phase problems, each component may have its own unique phases (up to 50) and may be repairable in some phases and nonrepairable in others. The codes can handle constant repair times, exponential repair distributions, or the multiphase combinations of these two types of repair. Inhibit conditions with either constant probabilities or time-dependent probabilities can be included with the components. Average running time for a 500-component fault tree, to obtain the component, minimal cut set, and system characteristics at 50 time points is on the order of a few minutes.

In actual application, the fault tree and reliability engineering techniques, with the quantitative evaluations of fault tree theory, have yielded results that verify that these types of safety and reliability analyses are of practical value. The specific evaluations in this section are to the SPERT IV protection system and to the PBF evacuation and poison injection system.

1. GENERAL OBJECTIVES OF THE ANALYSIS PERFORMED ON PHYSICAL SYSTEMS

Fault trees, with the PREP and KITT evaluations, are used at the NRTS to analyze reactor protective systems. The reliability and safety analyses have the following objectives: In general, these objectives are presented in the order of greatest importance.

- (1) To obtain the minimal cut sets (critical paths) of the system. The information obtained includes identification of any single failures
- (2) To determine the minimal cut sets by which the system is most likely to fail, that is, to identify the most important minimal cut sets
- (3) To determine the quantitative impact on system reliability or system availability due to proposed design modifications
- (4) To determine the quantitative response of system availability with regard to particular maintenance schemes
- (5) To determine the quantitative reliability or availability of the system.

In those analyses in which the system reliability or availability numbers are used as representations of the system safety or reliability, confidence limits are placed on these numbers to account for possible errors in data. In all the quantitative analyses performed, the numerical results obtained are weighted

against operating experience as a final evaluation. Particular analyses that have been performed at the NRTS and which had as their objectives one or more of the previous objectives are summarized in the following sections.

2. ANALYSIS OF SPERT IV PROTECTION SYSTEM

A fault tree was constructed for the SPERT IV reactor protection system to identify the faults which could singly or in combination cause none of the four control rods to drop under scram conditions. The scope of the analysis excluded those events or conditions that could prevent the control rods from entering the core after energization power was removed from the coupling magnets; for example, mechanical jamming of a control rod during insertion was not considered. The top event on the fault tree was then "power not removed from any of the four coupling magnets under scram conditions".

The fault tree was constructed to consider a combined timer and manual scram. The timer scram was assumed to have been initiated by the test timer and the assumption was that if the timer scram failed, the console manual scrams would be initiated. The manual scram was thus considered a backup for the timer scram, and the fault tree depicted both the timer and manual scram failing such that none of the four coupling magnets was deenergized.

The reactor protection and control components encompassing the test timer and the console scram switches and the electrical circuits to the control rod coupling magnets were considered as constituting the system. Secondary failures were considered when they could nullify multiple parallel fault combination requirements, that is, when they could cause multiple failures necessary for system failure to become single failures necessary for system failure.

For the analysis, the operation of the SPERT reactor was assumed to consist first of a check of the manual and timer scrams and then actual operation of the reactor for a given period. A test was, therefore, considered to be first the checking and then the subsequent actual operation. The analysis considered the performing of any number of these tests (cyclic operation). The checking of the manual scrams consisted of raising the control rods and then scrambling them by the console scram switches. Verification of a successful scram was provided by the scram relay monitor indicators. The control rods were not actually raised and dropped for the timer checking. For each of the tests, verification of the manual and timer scram check was assumed.

During the actual operation period of each test, a timer and a manual scram were assumed to be initiated. As stated, the fault tree analysis was made to investigate the failure of both scrams when initiated, such that the coupling magnets of none of the four control rods were deenergized. The scrams were assumed initiated in each of the cyclic tests, and timer and manual scram failure in one or more of these cyclic tests was also to be analyzed. The specific objectives of the fault tree analysis were:

- (1) To identify all credible component failures and fault conditions that could result in scram failure in any of the tests

- (2) To identify the most significant possible weaknesses in the existing (base-line) system
- (3) To determine the impact on reliability of proposed system modification.

The fault tree itself, and the determination of its minimal cut sets by PREP and KITT, satisfied the first objective. In the PREP and KITT run to obtain the minimal cut sets, failure rates were assigned to the components on the fault tree to determine the importance of the minimal cut sets and components with regard to scram failure. The results of the run are shown in Table XLVII.

TABLE XLVII

COMPONENT FAILURE CONTRIBUTIONS, SPERT BASE-LINE SYSTEM

<u>Component</u>	<u>Manual Scram Failure</u>	<u>Failure Contribution</u>
Relays (8)		0.6477
Console Switches (2)		0.3076
Terminals and Connectors (27)		0.0262
Wires (76)		0.0185
	<u>Timer Scram Failure</u>	
Timer (1)		0.9927
Relays (14)		0.0071
Terminals and Connectors (26)		0.0001
Wires (71)		0.0001

Manual scram failure denotes the failure of the manual scram when initiated in any test to deenergize any of the coupling magnets, and timer scram failure denotes failure of the timer-scram to deenergize any of the coupling magnets.

To determine the relative importance of the components and minimal cut sets, the two types of scram failure, manual and timer, were separately depicted in the fault tree. As observed from the table, the relays in the electrical circuit contributed most ($\sim 65\%$) to manual scram failure, whereas the 76 wires contributed least ($\sim 2\%$). For the timer scram failure, the timer itself was the most critical component because it contributed 99% to timer scram failure. These relative contributions to manual and timer scram failure, along with the minimal cut sets obtained for the fault tree, identified the possible critical weaknesses in the system.

With the identification of possible weaknesses, the areas within the respective scram systems that contained the critical components were subsequently identified. Modifications became evident and were subsequently proposed that would increase the reliability of the scram systems. The particular modifications proposed were (a) to place a second timer control relay in parallel with the existing timer relay (Modification 1) and (b) to place a manually set timer in parallel with the existing automatic timer (Modification 2). The impacts of these modifications were determined by using the PREP and KITT codes to evaluate the corresponding modified fault tree. The results of the impact evaluation are shown in Figure 272. The failure probability is the cumulative probability that both the timer and manual scrams will fail in any one or more of the tests performed resulting in none of the four coupling magnets being deenergized. For example, the failure probability for 200 tests denotes the probability of both the timer and manual scram failing in one or more of the 200 tests performed.

As evident from the figure, the proposed modifications significantly increased the reliability of the manual and timer scram system. These modifications were made evident from the fault tree analysis on the base-line system, and the impacts of these modifications were then determined from the PREP and KITT computer runs. Modification 1 was consequently decided upon as a change to be incorporated in the SPERT IV protective system which would be practical in cost and which would significantly increase system reliability.

FIG. 272 FAILURE PROBABILITIES FOR THE SPERT PROTECTION SYSTEM.

3. ANALYSIS OF PBF EVACUATION SYSTEM

The PBF evacuation system consists of two subsystems: the siren subsystem and the microphone notification subsystem. In the evacuation system, three independent radiation detectors sense excessive prescribed levels of gamma radiation. Signals from these detectors pass through three independent circuits, each consisting of a signal conditioner, a recorder, and a buffer. These three circuits constitute a two-out-of-three logic matrix for two separate trip channels, one to the siren speakers and the other one to the microphone oscillator, preamplifiers, and speakers. The siren subsystem and the microphone subsystem are thus input by the three independent detectors and their associated independent circuits, but are tripped separately by two-out-of-three logic matrices consisting of the three detectors and associated circuits.

A fault tree analysis performed for the PBF evacuation system had two objectives:

- (1) To determine whether the system contained any single failures and in general to determine the minimal cut sets (critical paths) of the system
- (2) To investigate an optimal testing frequency for those components which did not already have an established testing frequency.

The top event on the fault tree for this analysis was "no speaker giving an alarm when excessive radiation existed". Included as physically constituting the system were the detectors used to sense the radiation, the sirens and speakers used as alarms, and all interconnecting circuits and equipment such as the signal conditioners, recorders, and preamplifiers. Secondary failures were considered when they could cause multiple failures necessary for the top failure to become only a single failure.

The fault tree was first input to the PREP and KITT codes to determine the minimal cut sets of the system. Of the minimal cut sets obtained for the fault tree, none were minimal cut sets consisting of only one component. Hence, the system contained no single failures. From the fault tree and the minimal cut sets obtained, modifications were evidenced that would improve the reliability of the evacuation system. These modifications included the establishment of particular administrative controls, relocation of time delay relays, and separate cabling of the wiring.

For the investigation of optimal testing frequencies, failure rates for all the components on the fault tree were input to the PREP and KITT codes. Two testing intervals (once every two weeks and once every two months) had been established for particular subsystems of the evacuation system. The components in these subsystems were assigned the respective fixed testing intervals. The testing intervals of the remaining components were allowed to vary. These fixed testing times and the varying testing times that were to be investigated were also input to the PREP and KITT codes along with the component failure rates. The results of the computer run are shown in Figure 273.

FIG. 273 SYSTEM AVAILABILITY VERSUS COMPONENT CHECKING INTERVAL FOR THE PBF EVACUATION SYSTEM.

The availability is the probability of the evacuation system giving an alarm when excessive radiation exists, that is, the probability of the previously defined top event not existing. The checking interval is the particular checking interval used for those components that do not have the established checking intervals. The most-probable-value curve is the best estimate of the availability of the system versus checking interval when the component failure rates are input. The 90% upper bound and 90% lower bound are 90% confidence bounds for the availability curve which account for possible errors in the component failure rates. Thus, from the figure, if the components not having the established testing intervals were tested every six months and the components having the established testing intervals were tested at these respective intervals then the best estimate, or most probable value, of the system availability would be 0.996. However, the chance that the system availability actually lies between 0.983 and 0.998 is 90% if possible errors in the component failure rates are taken into account.

The quantitative availability for this system (the system number) is more meaningful because the possible errors in the component failure rates have been taken into account. From the figure, even if possible errors in the component failure rates are considered, the availability of the evacuation system is quite high and entirely acceptable for the testing frequency considered. Further, the availability of the system is rather insensitive to the testing frequency used for those components not having the established testing intervals.

In summary, the fault tree analysis for the PBF evacuation system verified that no single failure modes exist and made evident modifications which would increase the reliability of the system. The testing frequency study demonstrated

quantitatively a high availability of the system and an insensitivity to the particular checking interval used.

4. ANALYSIS OF PBF POISON INJECTION SYSTEM

The poison injection system is used as an emergency shutdown system for the PBF reactor in the event the control rods and the transient rods do not enter the core. The poison injection system consists of a 150-gallon storage tank containing gadolinium nitrate (a thermal neutron absorber) pressurized by two independent nitrogen bottles. Each nitrogen bottle is connected through separate lines to a common three-parallel matrix of solenoid control valves. This matrix connects to the storage tank. The nitrogen in each bottle is stored at high pressure.

The poison injection system is activated by manually switching three push-button switches to reduce the nitrogen bottle pressure and in turn pressurize the storage tank. Each of the three push-button switches operates two solenoid control valves in the three-parallel matrix. This two-out-of-three logic arrangement of the solenoid control valves and their associated control circuits protects against inadvertent release of the poison solution into the reactor vessel. It also has the redundant feature of allowing failure in a circuit from push-button switch to solenoid valves without preventing the injection of poison solution.

Once the storage tank is pressurized by manual activation of the three push-button switches, the poison solution is then distributed from the tank into the reactor vessel and primary coolant leg. The solution leaves the tank through a feeder pipe-line which connects to four separate pipe lines. These four lines penetrate the reactor vessel and primary coolant leg and distribute the poison solution through attached spray nozzles.

The poison injection system was subjected to a fault tree analysis in order to investigate the following:

- (1) Possible weaknesses in the system. These weaknesses would be determined both from the fault tree itself and from the minimal cut sets of the fault tree to be obtained by PREP and KITT.
- (2) The response of the system availability with regard to various maintenance checking intervals used for the components of the system. This information would be determined from the quantitative characteristics obtained by PREP and KITT.
- (3) Differences that would result in system availability due to particular design modifications. The quantitative characteristics obtained by PREP and KITT would be used.

For the fault tree analysis, the top event of the fault tree was "no poison solution discharged into the feeder pipe line when all three push-button switches are activated". The components from the nitrogen bottles to the feeder pipe

line, with their associated control circuitry, were defined as constituting the system. This definition of the system included all those components and circuits from the push-button switches to the input terminals of the solenoid valves. As in the previous fault tree analyses, secondary failures were considered when they could produce single failures from multiple failures.

The fault tree, having been drawn for the base-line system design, was input to the PREP and KITT computer codes to obtain the minimal cut sets. In the same computer run, failure rates and various checking intervals were assigned to the components to investigate also the response of system availability with regard to checking interval. From the fault tree itself, possible weaknesses in the system were discerned. Additional possible weaknesses were also discerned from the minimal cut sets obtained and the subsequent location in the fault tree of the components constituting the minimal cut sets. With these possible weaknesses identified, design modifications were formulated that would increase the availability of the system. Certain design modifications were input to additional PREP and KITT computer runs to evaluate their quantitative improvement of the system availability.

Figure 274 shows the probability of the system being unavailable versus component checking interval which was obtained for the base-line system and for the system incorporating the design modifications referred to in the previous discussion. The quantity actually plotted in the figure is the system failed probability, or one minus the system availability. The system failed probability is thus the system unavailability and is the probability of the top event existing. From the previous definition of the top event, the failed probability is consequently the probability of "no poison solution being discharged into the feeder pipe line when all three push-button switches are activated", and this probability is plotted versus the component checking interval.

In Figure 274 the upper curve is the base-line system failed probability; the middle curve is the failed probability for the system incorporating modifications which make it partially redundant; and the lower curve is the failed probability for the system incorporating further modifications that make it completely redundant. For the base-line and modified system curves, certain components had established checking intervals and these were held fixed; the checking intervals of the remaining components were allowed to vary. For each curve, the particular components having the fixed and varying checking intervals were the same. The checking interval plotted in Figure 274 is the varying checking interval. Each curve in the figure thus depicts, for a particular design, the change in system failed probability due to different checking intervals being used for those components not having established checking intervals.

For example, from the figure, if all the components in the base-line system design which did not have established checking intervals were checked every 100 hours and the remaining components were checked at their respective established intervals then the system failed probability would be 6×10^{-2} . Again, for the base-line system, if the same components were again checked every 1000 hours and the remaining components were checked at their established checking intervals (these established intervals having the same value as for the previous case) then the system failed probability would be 4×10^{-1} .

FIG. 274 SYSTEM UNAVAILABILITY VERSUS COMPONENT CHECKING INTERVAL AND DESIGN FOR THE PBF POISON INJECTION SYSTEM.

The results from the PREP and KITT runs which are illustrated in Figure 274 not only show, for a given design, the change in system availability with regard to component checking interval, but also the change in system availability with regard to change in design. If a given availability is desired (or equivalently, if a given failed probability or unavailability is desired), then either the base-line system design with a given checking interval may be used or a modified system design with a larger checking interval may be used. The design modifications allow the same system availability to be attained with larger component checking intervals. Hence, design modifications allow less maintenance to be performed with the same resultant system availability.

The modifications that made the base-line system design partially redundant and completely redundant consisted of successively incorporating more piping

redundancy from the nitrogen bottles to the 150-gallon tank. These modifications increased independence of the circuits as verified from the results shown in Figure 274.

Figure 275 shows the failed probability for the completely redundant design when possible errors in the component failure rates are taken into account. The most probable value curve in Figure 275 is the same curve as the completely redundant curve in Figure 274.

FIG. 275 SYSTEM UNAVAILABILITY VERSUS COMPONENT CHECKING INTERVAL FOR THE COMPLETELY REDUNDANT DESIGN OF THE PBF INJECTION SYSTEM.

When the possible errors in the component failure rates are large then the resulting possible errors in the quantitative system characteristics should be computed. Not only when the system numbers are to be used as representing the safety or reliability of the system, but also when comparisons are to be made, as in Figure 274. The differences that are observed in the system characteristics, such as in the availability or reliability, that are due to different checking intervals or different design modifications may be of no significance when possible errors in the component failure rates are taken into account. The pertinent consideration is whether or not the differences are real when the errors in component failure rates are taken into account or equivalently the pertinent consideration is the confidence level at which the differences become significant. For the comparisons in Figure 274, the confidence levels were computed for the differences that were observed due to design modifications and for the possible errors in component failure rates. That is, the probability that the difference observed was actual was computed for each set of curves in Figure 274. In the quantitative analysis performed, these confidence levels were factored into the differences observed.

Finally, with regard to the poison injection system analysis, Figures 276 and 277 show the behavior of the system failed probability. Figure 276 is a plot of both the system failed probability, Q , versus time and the system failure intensity, W , versus time for the completely redundant system, for a checking interval of 180 hours. The failure intensity, W , is the series of straight lines and the failed probability, Q , is the series of lower curved lines. The quantities actually plotted are $W \times 10^6$ and $Q \times 10^4$; thus, at 180 hours the failure intensity is $W = 48 \times 10^{-6}$ and the failed probability is $Q = 34 \times 10^{-4}$.

FIG. 276 TIME BEHAVIOR OF THE SYSTEM CHARACTERISTICS FOR THE COMPLETELY REDUNDANT DESIGN OF THE PBF INJECTION SYSTEM.

FIG. 277 COMPARISON OF THE TIME-DEPENDENT UNAVAILABILITIES FOR THE COMPLETELY REDUNDANT DESIGN OF THE PBF INJECTION SYSTEM.

For any given checking interval assigned to the components that did not have the established intervals, the system failed probability actually varies with time as shown in Figure 276. Immediately after the first check, in the figure after 180 hours, the system failed probability drops to a value near zero because of the maintenance performed at this check. Those components that are checked and found to be failed are repaired. (For these analyses, the repair time was considered negligible. If the repair time is not negligible compared to the checking interval, it should and can be taken into account by PREP and KITT.) Immediately after the next check, in the figure after 360 hours, the failed probability drops again to a value near zero because of the maintenance performed, and so forth. The system failure intensity, W , exhibits the same behavior, again because of the periodic maintenance performed.

Curves of the failed probability versus time, such as those of Figure 276, were obtained for each of the checking intervals considered for the components. The maximum value of the failed probability for the given checking interval was used as representing this checking interval. These points of maximum failed probability for a given checking interval were then plotted for Figure 274. Thus, for the 180-hour checking interval, the value $Q = 34 \times 10^{-4}$ was used for Figure 274; that is, the point $(180, 34 \times 10^{-4})$ was plotted for the completely redundant curve. This procedure was repeated for each of the checking intervals considered for a particular design and for each of the different designs considered.

The system failure intensity in Figure 276 shows the time at which the system is most likely to fail, as was described in the first portion of this section. The failure intensity, W , as shown in Figure 276 achieves its maximum value immediately before the check times, and hence, the system is most likely to fail immediately before it is checked. This characteristic of the system most likely failing immediately before the check times is a general behavior for any redundant system having no single failures.

Figure 277 shows a comparison of the behavior of the system failed probability for two different checking intervals used for the components for the completely redundant design. The lower series of similar curves represent the system failed probability for a checking interval of 180 hours, and the upper two curves represent the system failed probability if a checking interval of 720 hours is used. The 180-hour series of curves is identical to that of Figure 276 but the curves are plotted on a different scale.

5. CONCLUSIONS

With use of fault tree analysis, and the subsequent evaluations by PREP and KITT, reactor protective systems can be analyzed objectively for safety and reliability. Even though most of the component failure rate data now available have large associated errors, quantitative information can be obtained from the fault tree by an accounting of these errors. In actual application to physical systems, fault tree analyses and the obtained quantitative information have verified the practicality and value of this technique of safety evaluation.

XI. TOPICAL AND TECHNICAL PROGRESS REPORTS PUBLISHED DURING 1970

W. A. Yuill, V. F. Baston, O. L. Cordes, Release of Radioiodine from Open Pools, IN-1449 (December 1970)

An analytical approach based on mass transfer relationships is presented for calculating radioiodine evolution from open waste storage pools. The analytical relationships are checked by comparing iodine water-gas phase partition coefficients that were calculated through use of the mass transfer relationships and data taken from the literature with observed coefficients also taken from the literature. Calculations, based on these relationships, indicate radioactive iodine release from open waste storage pools, under certain conditions, can be a potential problem unless proper precautions are taken. The usefulness of these analytical relationships for more general gaseous releases is demonstrated in applications to gas evolution from waste storage facilities.

C. E. Slater, Comparison of Predictions from the Reactor Primary System Decompression Code (RELAP3) with Decompression Data from the Semiscale Blowdown and Emergency Core Cooling (ECC) Project, IN-1444 (December 1970).

The complete code RELAP3 was partially evaluated by comparing predicted transient pressures and residual water with experimental data from the Semiscale Blowdown and Emergency Core Cooling (ECC) Project. RELAP3 predicted the pressure behavior as a function of time within a maximum deviation of ± 100 psi of the experimentally measured pressure data; agreement was generally much better. Agreement between analytical and experimental results depended on the value of discharge coefficient selected in the analysis. A discharge coefficient of 0.6 gave best agreement with experimental data for the 100% breaks (100% break of four-inch ID pipe); whereas, a value of 0.8 gave the best agreement for the 2% breaks. The residual water in the semiscale vessel was predicted within a maximum deviation of $\pm 20\%$ of the measured residual water.

G. E. Gruen, WHAM Prediction of Semiscale Test Results, IN-1431 (October 1970).

The WHAM computer code was used extensively for prediction of subcooled pressure histories in the semiscale system. The various semiscale configurations included a vessel with internals and a coolant loop having a double-ended-break capability. Comparisons of the analytical results with the experimental data showed that WHAM accurately predicts the pressure histories for blowdowns produced by small breaks. Less accurate, but more conservative, predictive results were obtained from analyses of large breaks. The conclusion was reached that calculated pressure histories from WHAM are sufficiently reliable for use as forcing function input to structural dynamics codes employed in predictive analyses of postulated decompressions in large PWR's.

E. D. Hughes, A Correlation of Rod Bundle Critical Heat Flux for Water in the Pressure Range 150 to 725 psia, IN-1412 (July 1970).

Rod bundle critical heat flux data for water in the pressure range 150 to 725 psia have been compiled and several correlations derived. The analysis

has been conducted only for steady state data obtained from uniform axial flux profile rods with stable upflow of the coolant. The selected correlation, that is, the best fit of data, was compared with data outside the range of parameters on which it was based and no definite error trends were noted. An analysis of the methods of correlation in current use is presented and the most accurate correlations are shown to be those that incorporate observed experimental results into the formulation of the correlation.

H. W. Heiselmann, D. J. Olsen, J. F. Whitbeck, Semiscale Blowdown and Emergency Core Cooling (ECC) Project Test Report -- Tests 803 through 820, IN-1404 (October 1970).

A portion of the results of the Semiscale Blowdown and Emergency Core Cooling (ECC) Project tests are reported. The results were obtained from decompression experiments on a circulating loop connected to a simulated reactor vessel (without internals) that constitute an extension of previous decompression tests with only a vessel. All tests were initiated from isothermal fluid conditions. Reported are pressure, temperature, strain, momentum flux, thrust, and limited flow and density information for various break sizes, configurations, and initial conditions. The amount of water remaining, blowdown time, and indications of the thermodynamic processes are also reported.

R. C. Schmitt et al, Simulated Design Basis Accident Tests of the Carolinas Virginia Tube Reactor Containment -- Final Report, IN-1403 (December 1970).

Several simulated design basis accident (DBA) tests were performed early in 1969 on the Carolinas Virginia Tube Reactor (CVTR) containment system. The final results of these DBA tests are presented in this report, and include results of calculations of the containment heat transfer properties and comparisons of the measured and calculated containment responses to simulated DBA conditions. Steam injection conditions and flow rates, containment pressure and temperature response histories, pressure-suppression spray-system flow rates and effects, and a description of data acquisition instrumentation and equipment and data acquisition methods are also included.

G. E. Bingham, Final Results of the Carolinas Virginia Tube Reactor Containment Leakage Rate Tests, IN-1399 (June 1970).

A number of leakage rate tests were performed on the Carolinas Virginia Tube Reactor containment system to investigate the effects of pressure, temperature, and design basis accident (DBA) conditions on containment leakage rate. The leakage rates were determined by both the absolute method and the reference vessel method using data from several different leakage-measuring systems and two containment-temperature measuring systems. Presented in this report are the results of the different methods of measuring and calculating leakage rates; the influence of temperature, pressure, and DBA conditions on leakage; and an error analysis of the leakage rate measurement methods and the associated instrumentation systems.

D. L. Parry and D. L. Robinson, Incipient Failure Detection by Acoustic Emission, A Development and Status Report, IN-1398 (August 1970).

A practical incipient failure detection system, based on the phenomenon of acoustic emissions from imperfections in structural materials under stress, has been developed and tested on pressure vessels of various sizes and degrees of complexity. The acoustic emission technique has proven capable of rapidly detecting and locating flaws during hydrostatic acceptance and requalification testing of nuclear reactor pressure vessels. The technique, coupled with other standard nondestructive testing techniques, can provide improved capability for the integrity analysis of a wide variety of complex industrial structures.

D. J. Olson and J. F. Whitbeck, Semiscale Blowdown and Emergency Core Cooling (ECC) Project Test Report -- Tests 822 and 823, IN-1393 (October 1970).

The preliminary test results of two decompression experiments (Tests 822 and 823) performed in the Semiscale Blowdown and Emergency Core Cooling (ECC) Project are presented with minimal comment and evaluation in order to make the test data available for model development and evaluation in a timely manner. Tests 822 and 823 were initiated by hot leg breaks of a system configuration that includes both an operating loop and a simulated core with internals. The break size in Test 822 was 30% (0.027 ft²) of the pipe area, and the break size in Test 823 was 100% (0.09 ft²). The measurements obtained provided information on fluid conditions as a function of time including quality, velocity, and mass flow rate as well as subcooled pressure loads throughout the loop and thrust measurements on the vessel.

D. R. Evans, MOXY: A Digital Computer Code for Core Heat Transfer Analysis, IN-1392 (August 1970).

The core heat transfer code MOXY was developed for predictive and post-test analysis of core heat transfer during an LOCA. The code employs models that describe thermal radiation, conduction, and convection to steam, and incorporates the following advances in the heat transfer models over previous versions: (a) the fuel rod and sink conduction models use an implicit numerical integration technique that has unconditional numerical stability, and (b) the code solves a three-dimensional core model.

The code developed has been evaluated by comparisons between calculated rod surface temperatures and experimental temperatures. These comparisons indicate that the assumptions in MOXY regarding gray diffuse surfaces and symmetry are realistic.

C. F. Carmichael et al, LOFT Core Length Study, IN-1391 (August 1970).

A scoping study was performed to determine the nuclear core length and other selected parameters that are required for the Loss-of-Fluid Test (LOFT) system to have a thermal response during a loss-of-coolant accident (LOCA) similar to that of a large pressurized water reactor (PWR). This study was used as the basis for a recommendation regarding a minimum core length that would yield a thermal response similar to that of a large PWR.

The general criterion for a similar thermal response is that the fuel cladding hot-spot temperature history during the time preceding the injection of emergency core coolant into the core approximates that of a large PWR. The RELAP3 computer code was used to determine the pressure, coolant enthalpy, and mass flow rate existing at the entrance to the LOFT core during a transient. These quantities were then input into the THETA1 code that was used to determine the fuel cladding temperature history. The effects of various primary system parameters, initial operating conditions, and calculational methods on the predicted core thermal response of LOFT were also determined during this study.

R. T. Jensen, Experimental Results of the Fuel Heatup Simulation Tests (FHUST) - Emergency Core Cooling Test Series, IN-1390 (September 1970).

Bench-type bottom flooding coolant injection tests with 9- and 49-rod arrays of 30- and 36-inch uniformly electrically heated rods, respectively, were performed to determine the heat transfer mechanisms occurring during emergency core cooling (ECC) injection and to provide data to be used in developing thermodynamic models for predicting core temperature response during ECC. Several flow and heat transfer regimes occurred. These regimes included convection in water, nucleate boiling at the quenching front, flow film boiling in an annular two-phase flow, film boiling in dispersed two-phase flow, and convection in superheated steam.

R. H. Meservey and R. Herzel, Brittle Behavior of Zircaloy in an Emergency Core Cooling Environment, IN-1389 (September 1970).

Scoping tests were conducted to determine the extent and nature of embrittlement of zircaloy under high temperature conditions. The purpose of these tests was to provide information on zircaloy embrittlement that could be used in the planning of other experimental programs, particularly those programs in which heat transfer processes under loss-of-coolant accident conditions are being studied through use of zircaloy-clad electrical heaters as fuel pin simulators. The information acquired was to be used to predict the capability of zircaloy-clad electrical heaters to withstand loss-of-coolant tests and to assess the effect of the environment during a loss-of-coolant accident (LOCA) on the brittle nature of the zircaloy fuel cladding. Samples cut from zircaloy tubing were exposed in a steam environment at temperatures typical of those predicted for an LOCA. Physical and metallurgical properties of the tubing were determined before and after the exposures. In addition to the degree of embrittlement, changes in tubing dimensions, weight, and electrical resistivity were determined for a wide range of temperature histories. The degree of protection against embrittlement afforded by a corrosion film previously deposited on the tubing was also investigated.

D. C. Slaughterbeck, Review of Heat Transfer Coefficients for Condensing Steam in a Containment Building Following a Loss-of-Coolant Accident, IN-1388 (September 1970).

A literature search of available technical publications and a survey of recent experiments and current practices relevant to the prediction of heat transfer in a containment building following a loss-of-coolant accident have been completed. Very little information with direct applicability to large

power reactor containments is available on which to base concrete recommendations. A correlation based on the work of Tagami is suggested for use in safety analyses because its trend is consistent with the Carolinas Virginia Tube Reactor (CVTR) measurements and because its use leads to predicted containment pressures being higher and thus more conservative than those measured at CVTR. This conservatism is desired because of the uncertainty associated with extrapolating results from atypical small-scale experiments to large power plants.

R. W. Shumway and J. O. Zane, Simulated Emergency Flow Effects Tests (SEFET) Project (Technical Description) IN-1387 (June 1970).

The technical program is described for the proposed SEFET Project that was designed to determine the magnitude of the radial flow and coolant bypass problem following a loss-of-coolant accident. Calculations indicate that this problem may cancel the margin of safety previously thought to exist in emergency core cooling systems. Since the primary loop pressure drop controls coolant bypass, full-length heater rods and full-length steam generator tubes were to be used in SEFET to obtain the same amount of superheating and loop pressure drop as will occur in a pressurized water reactor. The magnitude of radial flow was to be controlled by core temperature gradients and the ratio of the hot-to-cold assembly volumes. SEFET rods were to be placed in a wedge-shaped array and the power to the rods was to be controlled to provide as much, or more, radial flow as will occur in a reactor.

R. W. Shumway and J. O. Zane, Pressurized Water Reactor - Full Length Emergency Cooling Heat Transfer (PWR-FLECHT) Test Project (Technical Description) IN-1386 (April 1970).

The PWR-FLECHT project provides data from which heat transfer correlations can be developed to predict the thermal response of PWR cores from the time emergency core coolant fills the lower core plenum until the core is reflooded.

This report, which presents the technical program description for the PWR-FLECHT project, includes accomplishments through FY-1970 and a detailed description of the FY-1971 program plan. The PWR-FLECHT project was designed to provide experimental data from 12-foot-long electrically heated rod bundles for use in determining the performance of emergency core cooling systems during a loss-of-coolant accident. Maximum temperature rises and temperature arrest times are presented with respect to the cold flooding rate in a stainless steel-clad, electrically heated rod bundle. Heat transfer coefficients are given for various bundle elevations with respect to time after flooding. The temperature-time behavior is also given for one test performed with a zircaloy-clad, electrically heated rod bundle.

R. W. Griebel and J. O. Zane, Boiling Water Reactor -- Full Length Emergency Cooling Heat Transfer (BWR-FLECHT) Tests Project (Technical Description) IN-1385 (June 1970).

This report provides a technical description of the BWR-FLECHT project. Included in the report are the technical basis for the project, the accomplishments thus far, a detailed test plan, and a description of the facilities.

BWR-FLECHT tests are conducted using full-length 49-rod, electrically heated bundles to simulate nuclear fuel pins. Both stainless steel- and zircaloy-clad bundles are used, and both core spray and core flooding techniques of emergency core coolant injection are employed. Tests are performed with either steady state or transient power input to the rods. The stainless steel-clad bundles are tested in sequences to obtain parametric heat transfer information. The zircaloy-clad bundles are tested to determine the behavior of zircaloy during the ECC phase of a loss-of-coolant accident and to provide demonstrations of ECC system effectiveness.

Test results currently are available for bottom flooding and top spraying for various bundle powers, flow rates, and initial temperatures. The heat transfer characteristics of the BWR emergency cooling systems have been thoroughly investigated for initial temperatures up to 1800°F at atmospheric pressures under both steady state and transient conditions.

The test results show that for both bottom flooding and top spraying with delayed flooding coolant injection modes, the ECC systems for current generation BWR reactors are capable of terminating the core thermal transient over the parameter ranges investigated.

H. W. Heiselmann and J. O. Zane, Semiscale Blowdown and Emergency Core Cooling (ECC) Project, (Technical Description) IN-1384 (July 1970).

A description of the objectives, test program, and test equipment for the Semiscale Blowdown and Emergency Core Cooling (ECC) Project is presented. Experimental information that will aid in predictive code development and evaluation is to be obtained from the test system. Blowdown tests have been performed with systems that consisted of a simulated vessel only, and the simulated vessel with an operating primary loop. Tests are to be performed with systems that are to include core heat, emergency core coolant injection, and more than one operating primary loop. Test information is to be obtained that is expected to aid in defining blowdown phenomena such as core fluid availability, system mass leak rate, and the interaction between the system coolant and the emergency core coolant.

C. F. Obenchain and L. J. Ybarrondo, Technical Assistance in Reactor Safety Analysis, (Technical Description) IN-1383 (May 1970).

The program of Technical Assistance in Reactor Safety Analysis is described, including its purpose, objectives, and justification. Technical accomplishments through FY-1970 are summarized and a program description for FY-1971 is presented.

H. D. Curet et al, Loss-of-Coolant Accident Analysis Program (Technical Description) IN-1382 (June 1970).

The Loss-of Coolant Accident Analysis Program provides experimentally verified analytical techniques for the Loss-of-Fluid Test (LOFT) Program and for the evaluation of light-water power reactors during a loss-of-coolant accident. The principal features of the Loss-of Coolant Analysis Program include core thermal models, primary loop hydraulic models, structural

models, and containment models. This document includes a description of the purpose, objectives, and justification of the Loss-of-Coolant Accident Analysis Program as well as its relationship to other nuclear safety programs. It also describes the models developed, the work which has been accomplished on each of these models to the present time, and the computer codes which employ these models. The computer codes include MOXY, THETA1, RELAP3, STRAP, and CONTEMPT-PS. A time schedule and work plan are also included for future development of these models.

D. H. Walker and G. W. Parker, Fission Product Behavior Experimental Program (Technical Description) IN-1381 (June 1970).

The Fission Product Behavior Experimental Program is intended to provide experimental data which will be used for assessing the adequacy of analytical models developed to describe the release of fission products and their subsequent behavior in a loss-of-coolant accident. Four specific areas are to be studied: (a) transport properties of noble gases and iodine in porous ceramic membranes, (b) fission product release upon cladding failure and subsequent transport of fission products through the reactor system to the containment, (c) time-dependent depletion of fission products in containment atmospheres, and (d) factors affecting the chemical state of radioiodine in containment atmospheres. This report presents a technical description of the program, including accomplishments to date, and a detailed description of the FY-1971 program plan.

D. H. Walker and W. A. Yuill, Fission Product Behavior Analytical Development Program, (Technical Description) IN-1380 (June 1970).

The program for development of analytical models for predicting the behavior of fission products in a loss-of-coolant accident for a water-moderated nuclear power reactor is described. Model development is proposed for specific steps in the transport of fission products from the fuel to the reactor containment building. These areas of fission product behavior are: (a) release from the fuel to the fuel-cladding gap, (b) venting of the fuel-cladding gap upon cladding rupture, (c) transport through the reactor vessel, and (d) behavior within the containment building. A summary of related analytical programs on fission product behavior and a summary of the models existing for each of the steps for which development is planned are included.

J. L. Plum, SECHT-IV Performance Evaluation of Stainless Steel and Zircaloy-Clad Electrically Powered Heaters, IN-1378 (June 1970).

The Scoping Emergency Cooling Heat Transfer (SECHT) Project was a parametric experimental study of bottom and top flooding modes of emergency core cooling for nuclear fuel assemblies. Four series of tests (SECHT-I, -II, -III, and -IV) were conducted. All series utilized electrically powered heaters to simulate nuclear fuel pins. Heaters for the first three series were clad with relatively inexpensive stainless steel. Zircaloy, the normal cladding material for nuclear fuel pins, was used as the cladding for heaters utilized for the SECHT-IV series of tests described in this report. This series consisted of a limited number of tests conducted to obtain thermal-response data from zircaloy-clad heaters operated at conditions similar to those of selected tests

of the SECHT-III series. Tests were conducted at different initial cladding temperatures ranging from 1000 to 1800°F; all other parameters of interest were held constant. Data from comparable tests of the two series were used to evaluate the relative thermal response of heaters clad with the two different materials but otherwise identical. This evaluation verified that for initial cladding temperatures up to 1800°F, stainless steel may be substituted for zircaloy as a cladding material for electric heaters to be used as simulators for nuclear fuel pins.

R. C. Schmitt, Evaluation and Comparison of Structural Dynamics Investigations of the Carolinas Virginia Tube Reactor Containment, IN-1372 (May 1970).

Results of investigations to determine the dynamic structural properties of the Carolinas Virginia Tube Reactor Containment are compared. Two independent experimental techniques, an ambient vibration survey and forced excitation tests, and analytical modeling were used. Decisive agreement was not achieved. However, the differences and their possible explanations are of importance as a measure of the understanding of dynamic analysis for complex structures, particularly as related to potential seismic response, and of the need for additional investigations.

D. L. Parry and D. L. Robinson, Elk River Acoustic Leak Detection Tests, IN-1371 (May 1970).

This report discusses laboratory tests and subsequent field tests conducted to show the feasibility of using acoustic techniques for detecting and locating leaks in the primary coolant system of water-cooled nuclear power reactors. The laboratory tests showed that acoustic bursts can be created in a crack when water is forced through the crack under temperature and pressure conditions sufficient to cause the water to flash to steam within the length of the crack. Tests conducted on the Elk River reactor verified laboratory findings. The Elk River tests also show the definite potential for using acoustics to detect and locate leaks in reactor pressure systems.

J. A. Dearien, C. Noble, E. D. Uldrich, STRAP -- A Computer Code for Static and Dynamic Structural Analysis and Studies Made Using the Code, IN-1362 (June 1970).

The computer code STRAP (STRuctural Analysis Package), developed to perform static and dynamic analyses of reactor structural systems, is described. The code uses the finite element method to model a structure and an uncoupled model summing procedure to perform the dynamic analysis. STRAP is a three-dimensional structural analysis code and can be used in the static and dynamic analyses of structures having up to 250 degrees of freedom. Studies on structural modeling techniques were made using the code, and guidelines are given for the modeling process.

C. M. Moser and R. W. Griebel, SECHT III -- An Experimental Investigation of Top and Bottom Flooding of a Nuclear Bundle Simulator, IN-1355 (February 1970).

The Scoping Emergency Cooling Heat Transfer (SECHT) Project was an experimental project directed at scoping the parameters of bottom and

top flooding modes of emergency core cooling for nuclear reactor fuel bundles. The SECHT project consisted of four series of tests of which the third series (SECHT III tests) is described in this report. The primary objective of the SECHT III tests was to perform timely sensitivity studies by using readily available hardware. Exact scaling of a segment of a reactor core was not accomplished; therefore, the results presented in this report are more qualitative than quantitative. The SECHT III testing program consisted of 55 experiments that were designed to form a parametric study over the range of variables thought possible at the initiation of the emergency core coolant injection in contemporary water cooled reactors. The test parameter variations included: initial bundle temperatures between 800 and 2000°F, cold flooding rates between 0.45 and 7.2 in./sec, average surface heat fluxes between 0 and 15,000 Btu/hr-ft², and coolant inlet temperatures between 100 and 200°F. The test vessel was fitted with a constrictive device which was placed over the exit as a means of providing preliminary data on the effects of water carry-over. Specific results show that the arrest and turnaround of the bundle temperature excursion occurs more quickly in tests which have high initial temperatures and that the sensitivity of turnaround time to bundle power decreases with increasing bundle initial temperature. The effect of the constriction is shown to be strongly dependent on flow rate.

G. H. Hanson, Subcooled-Blowdown Forces on Reactor System Components; Calculational Method and Experimental Confirmation, IN-1354 (June 1970).

The two spatial integrals for the force which is due to fluid pressure acting over a wetted surface and the force which is due to friction between the wetted surfaces and the fluid are two terms in the conservation-of-momentum equation. With one-dimensional subcooled-blowdown digital computer programs such as BURST (Blowdown Under a Rapid Sonic Transient) and WHAM (WaterHAMmer), the calculation of the transient hydraulic force on a coolant loop component during blowdown is accomplished more easily and through only one spatial integration by evaluating the remaining terms in the momentum equation. The calculation method described was confirmed by experimental blowdown studies, which were carried out using two experimental facilities, the semiscale vessel and the pipe blowdown apparatus. The information obtained using this calculational method is sufficiently reliable for use as forcing-function input for structural dynamics codes to establish the response of the components in a power reactor system to a loss-of-coolant accident.

G. F. Brockett, H. D. Curet, H. W. Heiselmann, Experimental Investigations of Reactor System Blowdown, IN-1348 (September 1970).

The purpose of the Semiscale Blowdown and Emergency Core Cooling (ECC) Project is reported as it relates to: (a) the need for experiments to test the analysis development, (b) the need for experimental data to answer specific questions unique to current 1000-megawatt pressurized water reactor (PWR) designs, and (c) the need for experimental data to answer specific questions arising in applying LOFT results to large power reactors. The experimental facilities, instrumentation, and measurements are described and the experimental results are presented and discussed. The subcooled behavior typifying that of the PWR systems is discussed prior to the two-phase behavior

typifying the two-phase decompression phenomena of the PWR and boiling water reactor (BWR) systems. The results presented are described in relation to (a) the fundamental phenomena that are expected and prescribed by analysis, (b) the specific need for experimental evaluation of an analysis area, and (c) the empirical needs of the analysis development for large systems. Implications of certain of the results to large power reactors and the effects of scaling are commented on in selected cases.

W. H. Rettig et al, RELAP3 -- A Computer Program for Reactor Blowdown Analysis, IN-1321 (June 1970).

This report documents the RELAP3 computer program, written in FORTRAN IV, 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, pressures, 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.

K. A. Dietz (ed.), Quarterly Technical Report Nuclear Safety Program Division October 1 - December 31, 1969, IN-1320 (August 1970).

This report contains: (a) a description of the RADHT radiation heat transfer subroutine for a BWR-FLECHT rod bundle, (b) a comparison of analytical techniques for determining the mass and energy in the liquid and vapor regions of a PWR containment following a loss-of-coolant accident, (c) studies of the effect of flow restrictions in a small rod bundle under emergency core coolant injection conditions, (d) results of studies of the release of noble gas fission products to the fuel-cladding gap of UO_2 fuel rods during reactor operation, (e) results of studies of particulate-iodine sampler development and iodine behavior studies, and (f) a summary of accomplishments in the SPERT Project -- Subassembly Test Program.

K. A. Dietz (ed.), Quarterly Technical Report Nuclear Safety Program Division, July 1 - September 30, 1969, IN-1319 (May 1970).

This report contains: (a) results of an investigation of the critical flow condition during a loss-of-coolant accident, (b) an examination of rod bundle critical heat flux data and correlations and their applicability to loss-of-coolant accident analyses, (c) a comparison of RELAP3 calculated results with CSE experimental results, (d) a preliminary summary of the Carolinas Virginia Tube Reactor in-plant tests and results, and (e) a summary of results and publications of the SPERT Project -- Subassembly Test Program.

K. A. Dietz (ed.), Quarterly Technical Report - LOFT Program Office - April 1 - June 30, 1969, IDO-17303 (March 1970).

This report contains: (a) a description of the analytical development effort in blowdown analysis with emphasis on the WHAM and RELAP3 codes,

(b) a parametric analysis of the pressure transient occurring in a containment due to postblowdown energy sources, (c) a description of the capabilities and auxiliary programs of the structural analysis code, STRAP, (d) results of an experiment to determine the behavior of zircaloy cladding during a loss-of-coolant accident, (e) results of the Test Series II, Group I, single-loop semiscale tests, (f) a comparison of results from the convectional flow model with experimental data, (g) a review of the data available on the release of noble gases from UO_2 fuel rods, and (h) a review of decontamination of containment surfaces and of the applicability of protective coatings.

D. J. Olson and J. F. Whitbeck, Semiscale Blowdown and Emergency Core Cooling (ECC) Project Test Report -- Test 821, IN-ITR-203 (July 1970) (limited distribution).

Results of Test 821 of the Semiscale Blowdown and Emergency Core Cooling (ECC) Project are reported. Test 821 was a hot leg blowdown from isothermal (no core heat) conditions of 2322 psia and 583°F. The blowdown nozzle was orificed to 10% (0.009 ft²) of the pipe cross-sectional area. This test was the first test with a system configuration that includes both an operating loop and a simulated core with internals. As such, it is a reference test against which future tests to be conducted with core heat can be compared.

D. J. Olson and J. G. Van Haverbeke, Description of Facility and Data Acquisition and Processing System for the Heated Core Portion of the 800 Test Series of the Semiscale Blowdown and Emergency Core Cooling (ECC) Project, IN-ITR-202 (July 1970) (limited distribution).

The facility and the data acquisition and processing system used for the 800 series heated core tests of the Semiscale Blowdown and Emergency Core Cooling (ECC) Project are described. The primary purpose of the report is to provide sufficient physical detail regarding the semiscale system to permit modeling and analysis by those engaged in the use of semiscale test results for code development and evaluation. The size and configuration of the components, materials of construction, and distribution of liquid volumes in the simulated pressurized water reactor system are given. The location and type of data acquisition devices and the method of processing data are also described.

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