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**QUARTERLY TECHNICAL PROGRESS REPORT ON WATER
REACTOR SAFETY PROGRAMS SPONSORED BY THE
NUCLEAR REGULATORY COMMISSION'S DIVISION
OF REACTOR SAFETY RESEARCH,
OCTOBER — DECEMBER 1975**

PREPARED BY AEROJET NUCLEAR COMPANY FOR

U. S. NUCLEAR REGULATORY COMMISSION

AND

ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION

IDAHO OPERATIONS OFFICE UNDER CONTRACT E(10-1) -1375

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SAFETY PROGRAMS SPONSORED BY THE NUCLEAR REGULATORY COMMISSION'S
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DIVISION OF REACTOR SAFETY RESEARCH, OCTOBER-DECEMBER 1975**

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ABSTRACT

Light water reactor safety activities performed during October–December 1975 are reported. The blowdown heat transfer test series of the Semiscale Mod-1 test program was completed. In the LOFT Program, preparations were made for nonnuclear testing. The Thermal Fuels Behavior Program completed a power-cooling-mismatch test and an irradiation effects test on PWR-type fuel rods. Model development and verification efforts of the Reactor Behavior Program included developing new analysis models for the RELAP4 computer code, subroutines for the FRAP-S and FRAP-T codes, and new models for predicting reactor fuel restructuring and zircaloy cladding behavior; an analysis of post-CHF fuel behavior was made using FRAP-T.

PREFACE

Aerojet Nuclear Company (ANC) performs technical activities in the water reactor safety programs at the Idaho National Engineering Laboratory (INEL) under the sponsorship of the U. S. Nuclear Regulatory Commission's Division of Reactor Safety Research. The current water reactor safety activities of ANC are accomplished in four programs: the Semiscale Program, the Loss-of-Fluid Test (LOFT) Program, the Thermal Fuels Behavior Program, and the Reactor Behavior Program.

The Semiscale Program consists of a continuing series of small-scale nonnuclear thermal-hydraulic experiments having as their primary purpose the generation of experimental data that can be applied to the development and verification of analytical models describing loss-of-coolant accident (LOCA) phenomena in water-cooled nuclear power plants. Emphasis is placed on acquiring system effects data from integral tests that characterize the most significant thermal-hydraulic phenomena likely to occur in the primary coolant system of a nuclear plant during the depressurization (blowdown) and emergency cooling phase of a LOCA. The current program of experiments employs test system Semiscale Mod-1, which has one intact (operating) loop with active components and a broken (blowdown) loop with passive components. These experiments include isothermal blowdown tests (no core heating) and blowdown heat transfer and core reflood tests using an electrically heated 40-rod core.

The LOFT Program is a nuclear test program for providing test data to support: (a) assessment and improvement of the analytical methods utilized for predicting the behavior of pressurized water reactors (PWRs) under LOCA conditions; (b) evaluation of the performance of PWR engineered safety features, particularly the emergency core cooling system (ECCS); and (c) assessment of the quantitative margins of safety inherent in the performance of these safety features. The test program utilizes the LOFT facility, an extensively instrumented 55-MW pressurized water reactor facility designed for the conduct of loss-of-coolant experiments (LOCES). The test programs, planned to start in early 1976, include a series of nonnuclear (without nuclear heat) LOCES followed by a series of low-power nuclear LOCES and then a series of high-power nuclear LOCES.

The Thermal Fuels Behavior Program is an integrated experimental and analytical program designed to provide information on the behavior of reactor fuels under normal, abnormal, and accident conditions. The experimental portion of the program is concentrated on testing of single fuel rods and fuel rod clusters under power-cooling-mismatch (PCM), loss-of-coolant, and reactivity initiated accident conditions. These tests provide in-pile experimental data for the evaluation and verification of analytical models that are used to predict fuel behavior under reactor conditions spanning normal operation through severe hypothetical accidents. Data from the program provide a basis for improvement of the models.

The Reactor Behavior Program encompasses the analytical aspects of predicting the response of nuclear power reactors under normal, off-normal, and accident conditions. Program areas include thermal-hydraulic (loss of coolant accident analysis) code development, fuel code development, code verification, and code optimization. The program produces computer codes for predicting the behavior of reactor systems and fuel, establishes data requirements for and performs verification analysis of analytical safety assessment codes, and maintains and upgrades computer codes to meet changing analytical and verification requirements.

A more detailed description of the four programs is presented in the quarterly technical report for January through March 1975, ANCR-1254. Later quarterly reports issued by Aerojet Nuclear Company are ANCR-1262 (for April-June 1975) and ANCR-1296 (for July-September 1975). Copies of all quarterly reports issued by ANC are available from the Technical Information Center, Energy Research and Development Administration, Oak Ridge, Tennessee, and National Technical Information Service, Springfield, Virginia.

SUMMARY

Water reactor safety program activities performed by Aerojet Nuclear Company during October through December 1975 are reported for the Semiscale Program, the LOFT Program, the Thermal Fuels Behavior Program, and the Reactor Behavior Program.

For the Semiscale Program, analyses of the isothermal test series (Group 1), including documentation of results, and performance of the blowdown heat transfer test series (Group 2) were completed. Analysis of the blowdown heat transfer test series results was undertaken, and preliminary results show that the measured system response is consistent with RELAP4/MOD5 calculations. For Test S-02-5 the predicted maximum rod clad temperature was 1,800°F and the measured value was 1,780°F. Departure from nucleate boiling (DNB) occurred at 0.5 second into Test S-02-5 as predicted. On the low power rod hot spots, DNB occurred between 0.5 and 4.0 seconds after rupture, whereas RELAP4 predicted DNB at 0.5 second followed by immediate rewet and then another DNB at 1.5 seconds after rupture. The measured peak clad temperature on the low power rods varied between 1,200 and 1,650°F, whereas the predicted peak temperature was 1,500°F.

Analysis of the Semiscale Mod-1 intact loop active steam generator performance indicates that the primary fluid transfers energy (about 2,695 Btu total) to the steam generator tube bundle and secondary fluid during the first 9 seconds of blowdown and that the steam generator secondary fluid and tube bundle transfer energy (about 1,435 Btu total) to the primary fluid between 10 and 40 seconds into the blowdown transient. The net energy transfer represents an amount that is less than 1% of the energy stored in the primary fluid prior to blowdown. As a result, the steam generator energy transfer did not have a significant effect on overall system performance.

To optimize break flow studies using Semiscale data, a method has been defined by which a highly probable equilibrium state point can be defined using a set of simultaneous pressure, temperature, and density measurements. A system of logic is employed by which the recorded values of the measurements are cross checked against one another and varied by amounts equal to or less than their respective measurement tolerances. An equilibrium set of state properties is thereby established. The method can correct for minor errors in the temperature and pressure measurements and larger errors in the density measurements. The method also can be used to assess the quality of experimental data by noting the amount the measurement values are adjusted in the process of establishing an equilibrium state point.

For the LOFT Program, in preparation for performance of mini-blowdown tests to qualify the LOFT blowdown suppression system, analyses were performed to predict the header pressure transient during mini-blowdown tests. Results indicate

that blowdown suppression system loads are not excessive. Preparations were made for the first series of nonnuclear LOCEs. An investigation was made to determine the practicality of using RELAP4/MOD5 homogeneous two-phase flow theory to predict heat transfer characteristics during the reflood portion of LOFT loss-of-coolant experiments. For this analysis a FLECHT experiment was modeled with the code, and the results were compared to FLECHT experiment data. Comparisons indicated reasonable agreement but that some additional features are required before a homogeneous theory can practically describe the reflood regime. It is concluded that approximate methods (the FLOOD4 code) should be employed for LOFT reflood analysis until a later version of RELAP4 is available.

In the Thermal Fuels Behavior Program, an irradiation effects scoping test (IE Scoping Test 1) was performed in the Power Burst Facility (PBF) reactor, and metallurgical examinations were completed of a fuel rod used in the third experiment in a series of power-cooling mismatch tests (i.e., the CHF Scoping Test, PCM-20 Test Series). The metallurgical examination included posttest measurement of rod dimensions, and cladding and fuel structure investigation results were summarized. Preliminary results of the IE Scoping Test 1 indicate that fuel rod zircaloy cladding experienced peak temperatures of 2,300 and 2,000°F at the 19.8-inch and 27.8-inch elevations, respectively, above the bottom of the fuel stack. An error analysis was performed of the measurement characteristics of spring-loaded thermocouples used to measure fuel rod temperatures in IE Scoping Test 1. Results indicate that high contact force of the thermocouple probe against the fuel rod produces a high thermal contact resistance, which is a dominant factor in overall thermocouple measurement error. A technique was implemented for determining power distribution and local power changes in an experiment fuel rod using self-powered cobalt neutron detectors.

For the Reactor Behavior Program, a description is given of the development and interrelationships of the various LOCA analysis computer codes. Program activities are reported for LOCA analysis development, containment analysis development, fuel rod model development, LOCA analysis verification, fuel behavior verification, and the industry cooperative program.

In the LOCA analysis development area, the reactor systems analysis code RELAP4/MOD5, Update 1, was completed, successfully checked out, and made operational on both the CDC-7600 and the IBM-360 computers. New analytical models developed and incorporated in the RELAP4 computer code are described below:

- (1) Small-break models in RELAP4 include: (a) a vertical slip model to provide realistic steam-water separation in vertical system components; (b) a horizontal slip model that allows for both cocurrent and countercurrent flow in horizontal components; (c) an improved mixture level model for tracking a single mixture level; and (d) a modified heat transfer correlation.

- (2) A new critical flow model (Henry-Fauske/HEM) combines the Henry-Fauske model with the homogeneous equilibrium model.
- (3) A downcomer penetration model was incorporated in the RELAP4 code that is used to predict the volumetric flux of ECC fluid from a PWR downcomer into the lower plenum; the model performed satisfactorily.

Containment analysis development utilized the KACHINA computer program to initiate development of a best-estimate advanced containment program (BEACON) based on KACHINA physics and numerical methods.

Fuel rod model development involved modifications to the FRAP-S and FRAP-T computer programs. Models were developed for FRAP-S that predict cladding creep deformation and axial cladding growth due to irradiation. In FRAP-T, a timing subroutine was incorporated that compiles and records all running time spent in each FRAP-T subroutine; other new models were incorporated in the FRAP-T program.

LOCA analysis verification assessed output uncertainty of the RELAP4 codes.

Fuel behavior verification efforts involved: (a) revising technical approaches and requirements for experiments concerning PCM conditions, irradiation effects, and LOFT fuel behavior; (b) reduction and analysis of data from instrumented test assemblies IFA-226 and IFA-429 which were irradiated in the Halden heavy boiling water reactor; (c) development of materials properties correlations; and (d) development of apparatus and evaluation of systems for postirradiation examination of full length power reactor fuel rods. Detailed discussions of the following items are presented in this report:

- (1) An analysis using the FRAP-T code was made of post-CHF LWR fuel behavior during postulated power-cooling mismatch conditions. Varying coolant flow rates were considered for moderate power (16kW/ft) and high power (23kW/ft) conditions. Four categories of post-CHF fuel behavior were delineated. Principal modes of fuel rod failure that were identified were: (a) cladding melting; (b) zircaloy oxidation and O₂ embrittlement; (c) cladding ballooning ending in rupture; and (d) cladding strain ending in rupture.
- (2) A model for fuel grain growth and restructuring in UO₂ and mixed oxide fuels is described (Section 6.2) in which equiaxed grain growth is modeled as a function of temperature and time and columnar grain formation is assumed to be governed by a threshold temperature.
- (3) A model is presented for zircaloy fuel cladding oxidation during reactor operation at temperatures between 240 and 400°C (Section 6.3). The model is applicable to both BWR and PWR conditions and to both Zircaloy-2 and Zircaloy-4.

- (4) Hydrogen uptake in zircaloy cladding is substantially controlled by the cladding oxide layer; a model for this phenomenon is presented in Section 6.4, and describes an analytical expression for determining the average weight fraction of H_2 in zircaloy during typical reactor operation at temperatures of 250 to 400°C.
- (5) A model for the effects of cold work and irradiation on the zircaloy strength coefficient and strain hardening exponent is described in Section 6.5. The effects of cold work and irradiation on cladding yield strength, ultimate strength, and stress-strain properties during plastic deformation can be directly obtained.
- (6) An analysis was performed to ascertain the effects of subchannel coolant flow blockage in the LOFT reactor core on adjacent fuel behavior during loss-of-coolant conditions. Results indicate that moderate blockage ($\leq 31\%$ of flow area) has minimal effects, but that more severe blockages ($\geq 50\%$ of flow area) may reduce coolant flow velocity and cause fuel failure propagation. The three-dimensional analysis was performed using the SCORE-EVET computer code.

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I. SEMISCALE PROGRAM

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The Semiscale Program objectives were described in ANCR-1254, the quarterly technical report for January–March 1975^[I-1]. Reported here are Semiscale Program technical activities accomplished during the current reporting period and selected preliminary evaluations of the Semiscale Mod-1 thermal-hydraulic performance during the blowdown heat transfer test series.

Analyses of the data obtained during the isothermal test series (Group 1)^[a] were completed and are documented in References I-3 through I-6. The major results of these analyses are summarized in Reference I-7.

The tests comprising the blowdown heat transfer test series (Group 2) also were completed during this reporting period. The blowdown heat transfer tests, described in ANCR-1230^[I-8], were the first Semiscale Mod-1 experiments to make use of an electrically heated core and active intact loop steam generator. These tests were designed to simulate the blowdown portion of a loss-of-coolant accident (LOCA). The primary purpose of this test series was to obtain information required to evaluate the heat transfer characteristics of the Semiscale Mod-1 core and to obtain information necessary for evaluating the analytical models that are currently used to calculate core flow and heat transfer coefficients. In addition, the blowdown heat transfer test series provide data necessary for determining the appropriate post-DNB power control characteristics required for simulating the thermal behavior of a reactor fuel rod. Test results also have provided an assessment of the initial hydraulic conditions appropriate for subsequent tests investigating core reflood behavior. The following material presents the core thermal response measured during Test S-02-5, the intact loop steam generator performance that occurred during Test S-02-4, and a discussion of a recently developed method to define equilibrium state points from experimental data.

1. BLOWDOWN HEAT TRANSFER TEST S-02-5 CORE THERMAL RESPONSE

T. K. Larson

Test S-02-5 was generally typical of the 200% cold leg break tests in the blowdown heat transfer test series conducted in the Semiscale Mod-1 system. The test was conducted from initial conditions of 100% rated core power, 100% rated core flow, 100% rated core differential temperature, and a system pressure of 2,250 psig. A comparison of the desired versus actual prerupture conditions is presented in Table I-I.

[a] The isothermal test series is described in Appendix A of ANCR-1194 (Reference I-2).

TABLE I-I

DESIRED VERSUS ACTUAL PRERUPTURE CONDITIONS

Measurement (Detector)	Desired	Actual
System pressure (PV-10)	2,250 $\pm$ 5 psig	2,250 psig
Core power (PWRCORETOT)	1.57 - 1.6 MW	1.565 MW
Loop temperature		
Intact loop cold leg (RBU-14)	544 $\pm$ 2 $^{\circ}$ F	540 $^{\circ}$ F
Intact loop hot leg (RBU-2)	610 $\pm$ 3 $^{\circ}$ F	610 $^{\circ}$ F
Broken loop hot leg (TFB-30)	605 $\pm$ 5 $^{\circ}$ F	600 $^{\circ}$ F
Broken loop cold leg (TFB-23)	542 $\pm$ 4 $^{\circ}$ F	534 $^{\circ}$ F
Core flow rate (FTV-COREIN)	165 $\pm$ 5 gpm	154 gpm
Pressure suppression system		
Tank water volume (DPPSS-T-B)	$\sim$ 30 ft ³	$\sim$ 30 ft ³
Tank water temperature (TFPSS-33)	156	$\sim$ 156 $^{\circ}$ F
Tank water pressure (P-PSS)	17.5 $\pm$ 1 psig	17.5 psig
Steam generator secondary side		
Pressure (PU-SGSD)		860 psia
Temperature (TFU-SG4)		523 $^{\circ}$ F
Liquid level (DPU-SG-SEC)	116 $\pm$ 2 in.	Not available
Feedwater temperature (TFU-SGFW)	435 $\pm$ 10 $^{\circ}$ F	437 $^{\circ}$ F
Pressurizer		
Pressure (PU-PRIZE)	2,250 $\pm$ 5 psig	2,250 psig
Mass liquid water	20 lbm	20.85 lbm
Coolant pump		
Speed (PUMPU-RPM)		2,100 rpm
Pressure differential (DPU-12-10)		27.5 psid

The axial power profiles for the Semiscale Mod-1 heater rods for Test S-02-5 are shown in Figure I-1. The four centrally located high power rods have an axial profile as illustrated by the upper curve. The lower curve defines the axial profile for the 35 low

power rods comprising the remainder of the core. Total core power was 1.6 MW. The core differential temperature was approximately 66°F. A plan view of the Mod-1 core illustrating the location of the heater rods is shown in Figure I-2. Radial locations and elevations (above the core bottom) of the heater rod thermocouples are indicated. Figure I-3 illustrates the transient core power profile used for Test S-02-5. This profile was developed using test results (heat transfer coefficients) from a preceding test (Test S-02-4) and applying them to a one-dimensional nuclear rod model to obtain a transient clad temperature response. This method is documented in Reference I-9.

An experimental version of the RELAP4 computer code^[I-10] (Mod E, Update 26) was used to generate the pretest prediction for Test S-02-5. The RELAP4 model nodalization diagram used in the pretest prediction calculation of Test S-02-5 results is shown in Figure I-4.

Figure I-5 presents a comparison of high power rod hot spot clad temperature for two of the heater rods near the center of the core (locations D4 and E5, Figure I-2) and a comparable nodal location from the RELAP4 prediction. The measured temperatures presented in Figure I-5 are from thermocouples that face the center channel of the Semiscale Mod-1 core. Both of the thermocouples indicate departure from nucleate boiling (DNB) at about 0.5 second after rupture. The thermocouple of rod D4 shows an increase to about 1,750°F. The temperature decrease after 24 seconds was mainly the result of a premature core power trip apparently caused by equipment problems. The predicted hot spot temperature response is in good agreement with the measured temperatures. DNB was predicted to occur about 0.3 second after rupture. The model did not predict the initial temperature peak at about 10 seconds; however, the model did overpredict the final temperature.

Six of the high power rod hot spot thermocouple responses are shown in Figure I-6, which indicates a fairly uniform response of the rod cladding hot spots. The peak temperature (at 10 seconds) varies between 1,780 and 1,720°F. The differences in the peak clad temperatures of these four rods are related to differences in the local power density, heat transfer coefficient, thermocouple location (beneath the clad surface), or thermocouple elevation (29 in. is near the boundary of the high power zone as shown in Figure I-1). All the hot spot thermocouples indicate DNB at about 0.5 second after rupture.

Figures I-7 and I-8 show comparisons of several of the low power rod hot spot clad temperatures. The RELAP4 prediction is also included in Figure I-7. Comparison of Figures I-7 and I-8 illustrates the varied response of the low power rods. Some of the hot spots indicate DNB at about 0.5 second with subsequent temperature increases to about 1,650°F, others show rewetting at 1 second, and still others do not indicate DNB until 3 or 4 seconds after rupture. The RELAP4 prediction shows a DNB at 1.5 seconds. A maximum low power rod clad temperature of 1,500°F was predicted. The data show that it varied between 1,200 and 1,650°F, depending on the rod location.

Fig. I-1 Heater rod axial power profile (Blowdown Heat Transfer Test S-U2-5).

Fig. I-2 Locations of heater rods and thermocouples for Test S-02-5.

Fig. I-3 Transient core power for Test S-02-5.

Fig. I-4 RELAP4 model nodalization diagram for Test S-02-5.

Fig. I-5 High power rod hot spot clad temperatures, Test S-02-5.

Fig. I-6 High power rod hot spot clad temperatures, Test S-02-5.

Fig. I-7 Low power rod hot spot clad temperatures, Test S-02-5.

Fig. I-8 Low power rod hot spot clad temperatures, Test S-02-5.

2. STEAM GENERATOR PERFORMANCE DURING THE BLOWDOWN HEAT TRANSFER TEST SERIES

J. M. Cozzuol

An analysis of the thermal response of the LOFT-simulated intact loop steam generator during tests of the Semiscale Mod-1 blowdown heat transfer test series has been performed to evaluate the heat transfer occurring in the steam generator during the blowdown transient. Control of the steam generator secondary feedwater flow and inlet temperature for each test established the required primary loop fluid temperature conditions prior to rupture. At approximately one second after rupture, the steam generator secondary feedwater and discharge valves were closed. All tests in the series^[I-8] were of the 200% offset shear cold leg break type, except for the first test (Test S-02-1) which was a 200% offset shear hot leg break.

Steam generator instrumentation for the test series provided data for both qualitative and quantitative analysis of steam generator performance during blowdown. Instrumentation within the steam generator included a pressure transducer and four secondary fluid thermocouples to provide an indication of primary-to-secondary and secondary-to-primary heat transfer. The steam generator configuration and secondary-side instrumentation locations are shown in Figure I-9. Instrumentation located external to the steam generator (but near the primary side inlet and outlet) provided measurements of fluid density, flow rate, pressure, and temperature at these locations.

The pressure history measured by the transducer located in the steam dome provides a means to analyze the steam generator thermal response during blowdown. Since the behavior of the steam generator during Test S-02-4^[I-11] was representative of its behavior for the overall blowdown heat transfer test series, the analysis presented here is of that test. The measured secondary pressure (PU-SGSD) obtained during Test S-02-4 is shown in Figure I-10. The increase in pressure that occurs (after the steam generator feedwater and discharge valves are closed) is due to the increase in secondary fluid temperature caused by heat transfer from the primary fluid. At approximately 9 seconds after rupture, the primary fluid temperature has fallen below the secondary fluid temperature and a reversal in heat transfer direction occurs. As a result, the secondary fluid temperature begins to decrease, leading to steam condensation and a corresponding reduction in the secondary pressure.

Pretest and posttest analysis of the steam generator thermal response has been performed using the RELAP4^[I-10] computer program. All RELAP4 calculations that are discussed were performed with an experimental version of the RELAP4 code, i.e., MOD E, Update 26. The steam generator model used in the RELAP4 program consisted of four control volumes on the primary side, each with a single two-dimensional heat slab, and one control volume on the secondary side containing the secondary fluid, as shown in Figure I-11. Both primary and secondary fluid-to-tube heat transfer coefficients were based on fluid conditions.

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Fig. I-9 Intact loop steam generator configuration.

Fig. I-10 Intact loop steam generator secondary pressure response, Test S-02-4.

Fig. I-11 RELAP4 MOD E (Update 26) intact loop steam generator model.

An evaluation of the user-specified steam generator model used in the RELAP4 program was made by comparing the calculated secondary pressure with test data. As can be seen in Figure I-12, the test prediction of the secondary pressure compares quite well with Test S-02-4 data until 9 seconds after rupture. After this time, the calculated value decreases only slightly, whereas the test data indicate a pressure decrease of approximately 55 psi. The calculated pressure does not decrease significantly during the latter portion of blowdown because the steam generator secondary-to-primary heat transfer is effectively limited by a low secondary fluid-to-tube heat transfer coefficient. A heat transfer coefficient of 5 Btu/hr-ft²-°F was used in the RELAP4 pretest prediction during the latter portion of blowdown whenever heat transfer from a stagnant fluid volume to an adjacent heat slab was predicted to occur. For this heat transfer condition, the coefficient of 5 Btu/hr-ft²-°F is the minimum value supplied by the RELAP4 program.

To improve analytical comparison with the test data, a posttest calculation for Test S-02-4 using a larger minimum secondary fluid-to-tube heat transfer coefficient was performed. Use of a turbulent natural convection heat transfer correlation^[I-12] indicated that a minimum heat transfer coefficient of approximately 250 Btu/hr-ft²-°F would be more appropriate. The secondary pressure resulting from this posttest calculation is shown in Figure I-12 for comparison with both the pretest calculation and the data. The posttest RELAP4 calculation provides better modeling of the steam generator performance, although the secondary fluid-to-tube heat transfer coefficient of 250 Btu/hr-ft²-°F still appears to be somewhat low.

The sensitivity of the steam generator primary side fluid temperature calculated by RELAP4 to the secondary fluid-to-tube heat transfer coefficient also was investigated. Figures I-13 and I-14 compare Test S-02-4 primary fluid temperatures near the outlet and inlet of the steam generator. Both pretest and posttest RELAP4 calculated fluid temperatures are shown in the figures. As shown in these figures, an increase in the secondary fluid-to-tube heat transfer coefficient from 5 Btu/hr-ft²-°F to 250 Btu/hr-ft²-°F in the RELAP4 model has very little effect on the resulting calculated primary side fluid temperatures.

Calculations were performed to evaluate the heat transfer occurring in the steam generator during blowdown for Test S-02-4. Each calculation consisted of a simple energy balance using the steam generator tube bundle metal and secondary side fluid as a control volume. The change in energy state of the steam generator external structure, as well as external heat losses, were neglected. An estimate of the secondary fluid volume capable of acting as a heat sink, or source, was assumed sufficiently large that the heat transfer was regarded as a maximum probable value. Both the heat transfer to the tube bundle metal and secondary fluid during the early portion of the blowdown, and the heat transfer from the tube bundle metal and secondary fluid during the latter portion of blowdown were determined.

The results of the steam generator heat transfer calculations for Test S-02-4 indicate that an energy transfer to the tube bundle and secondary fluid of approximately 2,695 Btu occurred between 1 and 9 seconds after rupture, and an energy transfer from the tube

Fig. I-12 RELAP4 prediction and calculation of steam generator secondary pressure with fluid-to-tube heat transfer coefficients of 5 and 250 Btu/hr-ft²-°F.

Fig. I-13 Sensitivity of calculated steam generator primary fluid outlet temperature to minimum allowable secondary heat transfer coefficient.

Fig. I-14 Sensitivity of calculated steam generator primary fluid inlet temperature to minimum allowable secondary heat transfer coefficient.

bundle and secondary fluid to the primary fluid of approximately 1,435 Btu occurred between 10 and 40 seconds after rupture. Compared to the energy stored in the primary fluid prior to rupture, the energy transfer occurring in the steam generator during blowdown is small (less than 1%). As a result, it is expected that the steam generator heat transfer has little effect on the overall system blowdown performance. This conclusion is consistent with results obtained from Test S-01-4 and Test S-01-5 of the Semiscale Mod-1 isothermal test series^[I-3].

3. A METHOD OF DEFINING EQUILIBRIUM STATE POINTS

USING EXPERIMENTAL DATA

D. G. Hall

3.1 Introduction

Recording the history of thermodynamic state at a given location during a thermal-hydraulic experiment typically involves simultaneous recordings of the histories of at least two measurable fluid properties. The state history is then reconstructed from the composite measurements. Temperature and pressure measurements are used to define thermodynamic state because accurate measurements of these properties can be made relatively easily. The measurement of fluid density is more difficult, but is required to establish quality when the fluid is saturated.

The state history can be constructed using density and either temperature or pressure measurements. However, a two-component measurement of the state can produce errors in the definition of the state which are not easily detected. If both a temperature and a pressure measurement are made in addition to the density measurement, sufficient information is available to cross check the measurements and define the thermodynamic state with a higher degree of reliability. Measurement errors and instrumentation malfunctions can also be corrected to a limited extent if three measurements are made. This capability can be invaluable when applied to key measurements in complex thermal-hydraulic experiments such as the Semiscale LOCA experiments which are costly to repeat.

A method has been developed and is reported herein which utilizes a set of temperature, pressure, and density measurements made at the same location to define an equilibrium state point. This is accomplished by using a system of logic, which incorporates the measurement accuracies, to transform a set of measurements which in general do not precisely define an equilibrium state into a set of values, which are closer to those of the actual equilibrium state point. The logic can correct for inaccuracies in temperature and pressure measurements by amounts equal to the respective measurement tolerances and is capable of making large corrections in the value of the density for subcooled and superheated state points. Use of the method provides two types of information which are useful in the analysis process. The first type is a measure of the quality of the measurements, which is determined from the amount the values of the measured parameters must be changed to define an equilibrium state point. The second type consists of additional thermodynamic parameters that can be obtained once the state has been defined.

3.2 Theory

Temperature and pressure data have measurement accuracies associated with them, which define an error region around a point having coordinates equal to the measurements as shown in Figure I-15. The notation symbology for Figure I-15 and the discussion that follows is given below:

NOMENCLATURE

p	Pressure
T	Temperature
ν	Specific volume
δ	One sigma pressure error
ϵ	One sigma temperature error
ζ	One sigma density error
ρ	Density
χ	Quality

SUBSCRIPTS

c	Calculated value
g	Saturated vapor
l	Saturated liquid
m	Measured value
sat	Saturated value
subcool	Subcooled condition
superheat	Superheated condition

All the points within the error region represent possible values for the actual data point considering the normal accuracy of the instrumentation. The basic approach used in the method is to vary the pressure and/or temperature in a prescribed way within the error region, so as to drive the calculated density to approach the measured density, such that the calculated value is within the error band of the density measurement.

Fig. I-15 Error regions for subcritical measured state points.

When the measured temperature-pressure combination indicates a subcooled or a superheated state (which is nearly always the case), the variable to which the density is least sensitive is varied first in an attempt to match the measured density with the calculated density. This means that for a state which is indicated to be subcooled by the measured temperature-pressure combination, the pressure will be varied first; whereas, for a state indicated to be superheated, the temperature will be varied first. If a match cannot be achieved by varying the first parameter by an amount equal to the tolerance of the measurement, the second parameter to which the density is more sensitive is varied. The routes of search shown in Figure I-15 reflect this procedure.

Because of the principle of varying the most significant parameter as little as possible in the process of establishing an equilibrium state point, a hierarchy among the three state property measurements is established for determining the state point. When a subcooled state is indicated, the hierarchy is temperature, pressure, and density; and when a superheated state is indicated the hierarchy is pressure, temperature, and density in that order. In both cases, the value of the density measurement is usually replaced by a calculated value so that a consistent set of equilibrium state point properties will be specified. Replacement of the measured value of the density with the calculated value is justified because the density measurement is usually the least accurate of the three measurements, particularly when the flow structure at the measurement station becomes complex. In addition, the calculated value is generally within the error band of the density measurement and is thus a probable value.

When a saturated state is indicated, all three measurements are of equal importance and the density measurement must be relied upon explicitly to define the state point. However, if the measured density is outside the range of saturated densities corresponding to the measured temperature-pressure combination, the hierarchies described above are again employed as the search is carried into the subcooled or superheated regions.

During the search procedure, the temperature and pressure are never allowed to assume values outside the measurement error region. This restriction ensures an output of probable values for the temperature and pressure, providing the instrumentation is operating normally. When the measured state point is subcooled or superheated, a temperature-pressure combination outside the error region can be required in order to establish an equilibrium state point using the measured density. If this occurs, the density measurement is assumed to be faulty and is replaced by the value calculated using the measured temperature-pressure combination. This procedure is termed density screening. The usefulness of incorporating this feature in the method to identify unreliable density measurements will be illustrated in the following material.

3.3 Method

Application of the considerations described in the previous section has produced a method with which equilibrium state points can be defined while at the same time retaining values of pressure, temperature, and density within the error bands of the respective measurements for nearly all cases. The method is presented in algebraic form in Table I-II. A step by step description of the method follows.

Consider first point "a" in Figure I-16 for which the measured temperature-pressure combination indicates a saturated state. If the measured density is contained in the range of saturated densities corresponding to the measured temperature-pressure combination, a quality is calculated and the state point is defined by $[T_m, p_m, \rho_m, \chi(\rho_l, \rho_g, \rho_m)]$. If the measured density is greater than $\rho_l(T_m, p_m)$, a subcooled state is indicated. In this case the next point that is checked is $(T_m, p_m + \delta)$. If the measured density is less than or equal to the calculated density plus the density tolerance $[\rho_m \leq \rho_c(T_m, p_m + \delta) + \zeta]$ the state is defined to be $[T_m, p_m + \delta, \rho_c(T_m, p_m + \delta)]$. If the measured density is greater than

TABLE I-II

EQUILIBRIUM STATE POINT DERIVATION

State Indicated by Measured Temperature and Pressure	Density Comparison	State Point Definition
Saturated	$\rho_L (T_m, p_m) \geq \rho_m \geq \rho_g (T_m, p_m)$	$[T_m, p_m, \rho_m, x (\rho_L, \rho_g, \rho_m)]$
Saturated	$[\rho_C (T_m, p_m + \delta) + \zeta] \geq \rho_m > \rho_L (T_m, p_m)$	$[T_m, p_m + \delta, \rho_C (T_m, p_m + \delta)]$
Saturated	$\rho_m > [\rho_C (T_m, p_m + \delta) + \zeta]$	$[T_m - \epsilon, p_m + \delta, \rho_C (T_m - \epsilon, p_m + \delta)]$
Saturated	$\rho_g (T_m, p_m) > \rho_m \geq [\rho_C (T_m + \epsilon, p_m) - \zeta]$	$[T_m + \epsilon, p_m, \rho_C (T_m + \epsilon, p_m)]$
Saturated	$\rho_m < [\rho_C (T_m + \epsilon, p_m) - \zeta]$	$[T_m + \epsilon, p_m - \delta, \rho_C (T_m + \epsilon, p_m - \delta)]$
Subcooled	$\rho_m \geq [\rho_C (T_m, p_m) - \zeta]$	$[T_m, p_m, \rho_C (T_m, p_m)]$
Subcooled	and $\rho_m < [\rho_C (T_m, p_m) - \zeta]$ and $p_{sat} (T_m) \geq p_m - \delta$ and $\rho_L (T_m, p_{sat} (T_m)) \geq \rho_m \geq \rho_g [T_m, p_{sat} (T_m)]$	$[T_m, p_{sat} (T_m), \rho_m, x (\rho_L, \rho_g, \rho_m)]$
Subcooled	Same as above except $\rho_m > \rho_L [T_m, p_{sat} (T_m)]$	$[T_m, p_{sat} (T_m), \rho_L, x = 0.0]$
Subcooled	Same as above except $\rho_m < \rho_g [T_m, p_{sat} (T_m)]$	$[T_m, p_{sat} (T_m), \rho_g, x = 1.0]$
Subcooled	$[\rho_C (T_m, p_m) - \zeta] > \rho_m \geq [\rho_C (T_m, p_m - \delta) - \zeta]$ and $p_{sat} (T_m) < p_m - \delta$	$[T_m, p_m - \delta, \rho_C (T_m, p_m - \delta)]$
Subcooled	$\rho_m < [\rho_C (T_m, p_m - \delta) - \zeta]$ and $T_{sat} (p_m - \delta) \leq T_m + \epsilon$ and $\rho_L [T_{sat} (p_m - \delta), p_m - \delta] \geq \rho_m \geq \rho_g [T_{sat} (p_m - \delta), p_m - \delta]$	$[T_{sat} (p_m - \delta), p_m - \delta, \rho_m, x (\rho_L, \rho_g, \rho_m)]$
Subcooled	Same as above except $\rho_m > \rho_L [T_{sat} (p_m - \delta), p_m - \delta]$	$[T_{sat} (p_m - \delta), p_m - \delta, \rho_L, x = 0.0]$
Subcooled	Same as above except $\rho_m < \rho_g [T_{sat} (p_m - \delta), p_m - \delta]$	$[T_{sat} (p_m - \delta), p_m - \delta, \rho_g, x = 1.0]$

TABLE I-II (Contd.)

State Indicated by Measured Temperature and Pressure	Density Comparison	State Point Definition
Subcooled	$[\rho_c (T_m, p_m - \delta) - \zeta] > \rho_m \geq [\rho_c (T_m + \epsilon, p_m - \delta) - \zeta]$ and $T_{sat} (p_m - \delta) > T_m + \epsilon$	$[T_m + \epsilon, p_m - \delta, \rho_c (T_m + \epsilon, p_m - \delta)]$
Subcooled	$\rho_m < [\rho_c (T_m + \epsilon, p_m - \delta) - \zeta]$ and $T_{sat} (p_m - \delta) > T_m + \epsilon$	$[T_m, p_m, \rho_c (T_m, p_m)]$
Superheated	$\rho_m \leq [\rho_c (T_m, p_m) + \zeta]$	$[T_m, p_m, \rho_c (T_m, p_m)]$
Superheated	$\rho_m > [\rho_c (T_m, p_m) + \zeta]$ and $T_{sat} (p_m) \geq T_m - \epsilon$ and $\rho_L [T_{sat} (p_m), p_m] \geq \rho_m \geq \rho_g [T_{sat} (p_m), p_m]$	$[T_{sat} (p_m), p_m, \rho_m, x (\rho_L, \rho_g, \rho_m)]$
Superheated	Same as above except $\rho_m < \rho_g [T_{sat} (p_m), p_m]$	$[T_{sat} (p_m), p_m, \rho_g, x = 1.0]$
Superheated	Same as above except $\rho_m > \rho_L [T_{sat} (p_m), p_m, \rho_L, x = 0.0]$	$[T_{sat} (p_m), p_m, \rho_L, x = 0.0]$
Superheated	$[\rho_c (T_m - \epsilon, p_m) + \zeta] \geq \rho_m > [\rho_c (T_m, p_m) + \zeta]$ and $T_{sat} (p_m) < T_m - \epsilon$	$[T_m - \epsilon, p_m, \rho_c (T_m - \epsilon, p_m)]$
Superheated	$\rho_m > [\rho_c (T_m - \epsilon, p_m) + \zeta]$ and $p_{sat} (T_m - \epsilon) \leq p_m + \delta$ and $\rho_L [T_m - \epsilon, p_{sat} (T_m - \epsilon)] \geq \rho_m \geq \rho_g [T_m - \epsilon, p_{sat} (T_m - \epsilon)]$	$[T_m - \epsilon, p_{sat} (T_m - \epsilon), \rho_m, x (\rho_L, \rho_g, \rho_m)]$
Superheated	Same as above except $\rho_m < \rho_g [T_m - \epsilon, p_{sat} (T_m - \epsilon)]$	$[T_m - \epsilon, p_{sat} (T_m - \epsilon), \rho_g, x = 1.0]$
Superheated	Same as above except $\rho_m > \rho_L [T_m - \epsilon, p_{sat} (T_m - \epsilon)]$	$[T_m - \epsilon, p_{sat} (T_m - \epsilon), \rho_L, x = 0.0]$
Superheated	$[\rho_c (T_m - \epsilon, p_m + \delta) + \zeta] \geq \rho_m > [\rho_c (T_m - \epsilon, p_m) + \zeta]$ and $p_{sat} (T_m - \epsilon) > p_m + \delta$	$[T_m - \epsilon, p_m + \delta, \rho_c (T_m - \epsilon, p_m + \delta)]$
Superheated	$\rho_m > [\rho_c (T_m - \epsilon, p_m + \delta) + \zeta]$ and $p_{sat} (T_m - \epsilon) > p_m + \delta$	$[T_m, p_m, \rho_c (T_m, p_m)]$

Fig. I-16 Search routes within error regions.

$[\rho_c(T_m, p_m + \delta) + \xi]$, the density is calculated at the most subcooled state point in the error region $[(T_m - \epsilon), (p_m + \delta)]$ and the state is defined by $[(T_m - \epsilon), (p_m + \delta), \rho_m(T_m - \epsilon, p_m + \delta)]$.

The case in which the measured density is less than $\rho_g(T_m, p_m)$ is handled in a similar manner except the pressure is held constant first and the point $(T_m + \epsilon, p_m)$ is checked. If the measured density is within the density tolerance of the calculated density, the state point is defined by $[(T_m + \epsilon), p_m, \rho_c(T_m + \epsilon, p_m)]$. If $\rho_m < [\rho_c(T_m + \epsilon, p_m) - \xi]$, the state point is defined to be the most superheated in the error region, $[(T_m + \epsilon), (p_m - \delta), \rho_c(T_m + \epsilon, p_m - \delta)]$.

Consider next point "b" in Figure I-16 for which the measured temperature-pressure combination corresponds to a subcooled state point. The three saturation line locations indicated in Figure I-16 illustrate the three possible relationships the saturation line can have to the error region:

Position 1: The state point is sufficiently subcooled so that the saturation line does not pass through any part of the error region.

Position 2: The saturation line does not cross the T_m isotherm in the error region.

Position 3: The saturation line does cross the T_m isotherm in the error region.

If the measured density is greater than $[\rho_c(T_m, p_m) - \xi]$, the subcooled state point is confirmed and defined by $[T_m, p_m, \rho_c(T_m, p_m)]$. If measured density is less than $[\rho_c(T_m, p_m) - \xi]$, the saturation pressure corresponding to T_m is found and compared with the lower bound value of the measured pressure ($p_m - \delta$). If the saturation line crosses the T_m isotherm in the error region [i.e., $p_{sat}(T_m) \geq (p_m - \delta)$], the state point is defined by calculating a quality when $\rho_l \geq \rho_m \geq \rho_g$. If the measured density is less than ρ_g or greater than ρ_l , the output density is defined to be ρ_g or ρ_l with a corresponding quality of 1 or 0, respectively.

When the saturation line does not cross the T_m isotherm in the error region, the subcooled density at the point $(T_m, p_m - \delta)$ is calculated. If the measured density is greater than or equal to $[\rho_c(T_m, p_m - \delta) - \xi]$, the subcooled state point is confirmed and the state point is defined by $[T_m, p_m - \delta, \rho_c(T_m, p_m - \delta)]$. If the measured density is less than $[\rho_c(T_m, p_m - \delta) - \xi]$, the saturation temperature corresponding to $p_m - \delta$ is calculated and compared with $T_m + \epsilon$ to determine if the saturation line crosses the $p_m - \delta$ isobar in the error region. If this is the case, the state point is defined by calculating a quality if $\rho_l \geq \rho_m \geq \rho_g$ or by assigning density the value of ρ_g or ρ_l , whichever is appropriate, if ρ_m is outside these bounds.

When the saturation line does not pass through the error region [i.e., $T_{\text{sat}}(p - \delta) > T_m + \epsilon$] and the measured density is less than $[\rho_c(T_m, p_m - \delta) - \xi]$, the density at the point $(T_m + \epsilon, p_m - \delta)$ is calculated. If the measured density is greater than or equal to $[\rho_c(T_m + \epsilon, p_m - \delta) - \xi]$ the state point is defined by $[T_m + \epsilon, p_m - \delta, \rho_c(T_m + \epsilon, p_m - \delta)]$. If the measured density is less than $[\rho_c(T_m + \epsilon, p_m - \delta) - \xi]$, it is assumed that the density measurement is faulty and it is replaced by the density calculated using T_m and p_m . The state point is thus defined by $[T_m, p_m, \rho_c(T_m, p_m)]$. This last case is the process by which density screening is accomplished.

The procedure that is followed in defining an equilibrium state point when the measured temperature-pressure combination indicates a superheated state point is similar to that outlined above for the subcooled case. The principal difference is in the search route as shown in Figure I-16 for point "c". For the superheated case, the saturation line intersection with the p_m isobar is checked first and then followed, if necessary, by a check for intersection with the $T_m - \epsilon$ isotherm.

3.4 Discussion

The equilibrium state point method was applied to a set of temperature, pressure, and density measurements made in spool piece 23 near the vessel side of the break during Semiscale Mod-1 Test S-02-4 [I-11]. A complete description of the system and measurement stations may be found in Reference I-11. Figures I-17, I-18, and I-19 show comparisons between the measured values and the values output by a computer program named STATE which incorporates the equilibrium state point method. The measured temperatures and pressures agree well with the STATE values with a notable exception at $t = -5$ seconds. Significant differences are seen to exist between the measured density and the STATE density in Figure I-19 prior to rupture, during subcooled blowdown ($0 \text{ sec} \leq t \leq 3.5 \text{ sec}$), and when the fluid superheated late in the transient ($t \geq 22.5 \text{ sec}$).

The results at $t = -5$ seconds illustrate a limitation of the method when the state is subcooled or superheated. The measured temperature-pressure combination corresponded to a subcooled state point and the corresponding calculated density was higher than the measured density. The measured density was sufficiently lower than the calculated density to necessitate an adjustment in both the temperature and the pressure approximately equal to the respective measurement tolerances. This resulted in temperature and pressure values which do not appear to be realistic when compared with neighboring values as shown in Figures I-17 and I-18. The output values were nonetheless within the error bands of the measurements. It is evident that the same logic that corrects for temperature and pressure errors by amounts equal to the measurement tolerances can also cause the output of values which are in error by the same amount. Superficially, it appears that better results would be obtained if the density measurement was not used in the definition of subcooled and superheated state points. While this may be true for the example under discussion, such an approach can result in significant errors in the state point history that cannot be easily detected. Even if the values output by STATE are in error by their respective measurement tolerances, the output values are still within the measurement accuracies of the

Fig. I-17 Pressure measured in Spool 23 compared to pressure from STATE subroutine.

Fig. I-18 Temperature measured in Spool 23 compared to temperature from STATE subroutine.

Fig. I-19 Density measured in Spool 23 compared to density from STATE subroutine.

instrumentation, and, in addition, they have been confirmed by a third measurement. The possibility of calculating values of temperature and pressure which are in error by the amount of the respective measurement tolerances is thus the price that must be paid for the three beneficial aspects of the method: (a) temperature and pressure measurement errors within the measurement tolerances will be corrected at least part of the time; (b) data output by the method will carry a higher confidence factor because it has been confirmed by a third measurement; and (c) the method has a built-in check which will ignore subcooled and superheated density readings that are suspect and replace such measurements with more correct values.

The method succeeded in correcting significant errors in the density measurements as shown in Figure I-19. The density oscillations in the subcooled fluid prior to rupture which are intuitively questionable have been eliminated by application of the method. The density decrease immediately after rupture which is not supported by the pressure and temperature histories at that time is not present in the density history calculated by STATE. The density oscillations which were indicated to have occurred when the fluid was superheated ($t \geq 22.5$ sec) have also been eliminated. Most of these oscillations were eliminated by the density screening feature of the method. The feature of the method which utilizes the measured density only as an indicator to confirm a subcooled or superheated state point also aided in eliminating spurious density readings. The final result is a density history which is consistent with the general characteristics of the temperature and pressure histories.

Comparisons between the measured and STATE values are presented in the form of property differences in Figures I-20, I-21, and I-22. This type of plot clearly shows the amount that the measured temperature and pressure had to be adjusted in order for the measured and calculated densities to differ by less than the density measurement tolerance. The density difference plot presented in Figure I-22 shows the amount the STATE density varied from the measured density. Plots of this type can be used to assess data quality.

Fig. I-20 Difference between pressure measured in Spool 23 and pressure from STATE subroutine.

The history of the difference between the measured and STATE pressures presented in Figure I-20 shows that the value of the pressure was varied by the full amount of measurement tolerance (30 psi) only three times. The measured value was retained most of the time prior to $t = 2$ seconds and after $t = 17$ seconds. During much of the saturated blowdown ($3.5 \text{ sec} \leq t \leq 22.5 \text{ sec}$) the measured value of pressure deviated from the STATE value; however, the deviations were in general less than 20 psi, which tends to indicate acceptable consistency in the data. Nevertheless, a complete assessment of data quality requires the use of all three comparisons.

The temperature difference history shown in Figure I-21 also indicates that few deviations equal to the tolerance of the temperature measurement were necessary. In fact, the measured value of the temperature was retained for a great majority of the data. The fact that the measured temperature was retained, while the value of the pressure was

Fig. I-21 Difference between temperature measured in Spool 23 and temperature from STATE subroutine.

Fig. I-22 Difference between density measured in Spool 23 and density from STATE subroutine.

modified during saturated blowdown indicates that the measured temperature-pressure combinations corresponded to subcooled state points. The pressure was thus decreased to obtain an intersection with the saturation line at constant temperature. If the error were shared between the temperature and pressure measurement (as would be the case if the saturation line were approached along a line perpendicular to it instead of along an isotherm) the above mentioned pressure deviations would be reduced. The temperature and pressure deviations during subcooled blowdown would be less than half the allowed measurement tolerances, which is indicative of good measurements made within the tolerance of the instrumentation.

The density difference history is the most graphic indicator of data quality during periods other than saturated blowdown. For subcooled and superheated states the measured density is usually replaced by a calculated density which may be outside the density error band by virtue of the density screening feature. During saturated blowdown the measured density must be relied upon to determine the fluid quality. For this reason the temperature and pressure difference histories are more indicative of the accuracy of the data during saturated blowdown. The density difference history presented in Figure I-22 shows that the measured density was retained during almost all of the period from $t = 4$ seconds to $t = 22$ seconds. This result is as expected since this is approximately the period of saturated blowdown. The magnitude of the density differences which occurred prior to $t = 4$ seconds and after $t = 18.5$ seconds are in general within the density measurement tolerance of $\pm 0.5 \text{ lbm/ft}^3$. The only period during which a significant difference persists for more than about half a second is during the interval $t = -0.5$ second to $t = 2$ seconds. Figure I-19 shows that the measured density did take an unexplained drop during this period. This drop is not supported by the pressure and temperature measurement and is therefore probably the result of some temporary malfunction of the densitometer associated with the initiation of blowdown. This example illustrates how the density screening feature of the method can be employed to produce highly probable data values despite densitometer malfunction.

In addition to producing histories of equilibrium state properties, the method also produces fluid quality information which can be used to produce a history of thermodynamic quality as shown in Figure I-23. Such a plot is useful in tracking the rapidity with which the change of state is occurring, and also provides information about the consistency of the measured data. An orderly change of state indicates consistent data unless complex phenomena are expected to have occurred during the history of events.

3.5 Conclusions

The equilibrium state point calculational method accounts for the anomalies in a set of fluid property measurements and produces a corresponding set of data that approximate the actual properties with a good degree of reliability. Output temperature and pressure values are cross-checked using the measured density and are guaranteed to be within the measurement tolerance for each measurement. Output densities are either equal to the measured values or are probable values derived using pressure and temperature measurements. The method also provides a density screening feature for subcooled and superheated state points which corrects density measurements that cannot be approached

Fig. I-23 Calculated fluid quality in Spool 23.

by densities calculated at the extremities of the temperature-pressure error regions. The method thus represents a useful analytical tool with which measured state point data can be translated into usable equilibrium state point data so that other thermodynamic parameters of interest can be obtained. In addition, the usefulness of this method in assessing the consistency and therefore the quality of measured state point data has been demonstrated.

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II. LOFT PROGRAM

L. J. Ybarrondo, Program Manager

The Loss-of-Fluid Test (LOFT) Program objectives were described in ANCR-1254, the quarterly technical report for January–March 1975^[1-1]. Major activities during the second quarter of FY-1976 centered on final preparations for the series of nonnuclear loss-of-coolant experiments (LOCE). Presented below are highlights of these activities, and a discussion of the practicality of predicting heat transfer characteristics during the reflood portion of a LOCE in the LOFT reactor with a homogeneous two-phase flow theory.

1. NONNUCLEAR TESTING PREPARATION AND ANALYSIS

Final assembly of the mobile test assembly (MTA) and blowdown suppression system, installation and calibration of the experimental instrumentation, and final preparations for retrieving and processing the experimental data were completed in preparation for the start of the first nonnuclear LOCE, designated L1-1.

The on-line digital data display system was completed. This system consists of a display panel with 23 test parameters (steam generator pressure and temperature; reactor vessel pressure, temperature, and flow; accumulator pressure level; etc.) shown digitally during the data acquisition function.

The final major phase of plant testing, prerequisite to performing LOCE L1-1, was started and by the end of December 1975 was approximately 85% complete. This phase of testing is the first utilizing the experimental instrumentation. Baseline information was acquired from the test for comparison with data from LOCEs, including the primary coolant pump coastdown and the time required for emergency core coolant injected into the cold leg of the primary coolant system to reach the reactor vessel lower plenum with the system at ambient temperature, i.e., the cold wall delay time, which was 1.9 ± 0.5 seconds.

“Mini-blowdown” tests (so called because only a small water volume is vented from a portion of the piping that is isolated from the system by valves) are planned for early 1976 to determine experimentally the maximum downcomer submergence level (and associated pressure loading) that is allowable in the pressure suppression tank. Preparations completed during this quarter for the mini-blowdown tests included: (a) digitizing the data from the Semiscale suppression tank experiments (including load cell calibration) on the modular computer (MODCOMP); (b) running a detailed RELAP4 model of the quick-opening blowdown valve and downstream piping to predict mass and energy flow to the header during the mini-blowdowns; and (c) setting up a CONTEMPT computer program model to utilize the RELAP4 output for predicting the header pressure transient during the mini-blowdowns. From the analyses it was determined that the tests will not impose loads on the blowdown suppression system greater than 50% of the design load limits.

Data were analyzed from Semiscale Test S-02-9^[II-1] (suppression tank response to LOCE with downcomer exit 1.0 inch above liquid surface) to determine condensation effectiveness in a pressure suppression tank with no downcomer submergence. This analysis indicates that complete condensation of the injected steam did occur. The condensation resulted in significant stratification in the suppression pool temperature, which resulted in a higher liquid pool surface temperature, and thus higher vapor pressure, than predicted by the pretest calculations, which assumed complete mixing. This result indicates that operation of the LOFT blowdown suppression tank with no initial downcomer submergence may be a viable option. However, the aforementioned temperature stratification in the suppression pool must be taken into account.

The remaining fuel bundle for the LOFT 5.5-foot nuclear core was received. Assembly of the fuel modules (i.e., attachment of the instrument stalks and support structures to the fuel bundles) was initiated at the LOFT site. Figure II-1 shows a prototype fuel module assembled and ready for placement in its storage locker.

2. PREDICTION OF REFLOOD HEAT TRANSFER

C. W. Solbrig

It is necessary to use a reflood model to predict reflood phenomena that occur during a LOFT nuclear LOCE. It would be most convenient to use the same code to predict reflood phenomena that is used for predicting the blowdown portion of the experiment. Also, LOFT uses 5.5-foot fuel rods so that use of a fundamental calculation method such as RELAP4 would be better than using a correlation developed for 12-foot rods. The RELAP4 computer code^[II-2] is representative of the current generation of computer codes used in predicting behavior of nuclear power reactors during a hypothetical loss-of-coolant accident (LOCA). To determine the practicality of using these codes to predict heat transfer during the reflood portion of a LOFT nuclear LOCE, the latest available version of the RELAP4 code (MOD5, Update 1) was used to model one of the representative forced bottom flooding full length emergency coolant heat transfer (FLECHT) experiments^[II-3].

Following is a discussion and comparison of the RELAP4 model of FLECHT and of the FLECHT experiment data.

2.1 FLECHT Experiment

The temperature-time profiles for FLECHT Test 0085 are shown in Figure II-2 for five different rod elevations. The FLECHT temperature profiles are shown at 2, 4, 6, 8, and 10 feet above the entrance of the tube bundle. The lower elevations are seen to quench first (decrease temperature rapidly) with the 2-foot elevation quenching at about 32.5 seconds.

Fig. II-1 An assembled prototype fuel module ready for storage.

The initial power profile was a symmetric chopped cosine distribution. The initial rod surface temperatures were also symmetric. Thus, the temperature at the 2- and 10-foot elevations were the same initially; the temperature at the 4- and 8-foot elevations were the same, while the 6-foot elevation had the highest initial temperature. Although the 2- and 10-foot elevations had the same initial temperature, the amount of cooling each location received was considerably different.

The temperature at each elevation was observed to decrease (turn over) considerably before it quenched. The increased amount and velocity of steam generated at low level turned the temperature around at high elevations, but a given location on a rod did not quench until the liquid steam interface reached that location.

Fig. II-3 Noding diagram for RELAP4 homogeneous model of FLECHT Test 0085.

2.2.1 Saturated Inlet Fluid with No Slip. The results of the basic homogeneous RELAP4 calculation are presented in Figure II-2. The input described the experimental conditions in all instances except for inlet water temperature. Saturated water was assumed, even though subcooled water was actually used in the FLECHT experiment. The introduction of subcooled water in the RELAP4 calculation results in long computer run time. Saturated water was therefore used for the base computation because of its fast computational time. A parameter study was done from this base case to investigate the effect of other variables.

The computational results are at slightly different elevations (0.1-foot increment differences) than those shown experimentally, but the conclusions would not change with interpolation. The RELAP4 run with saturated inlet water showed less cooling in the bottom half of the rods than in the top half, which is opposite to what the FLECHT experiment results showed. Quenching did not occur in the computations at any elevation, although the temperature did turn over at the 8.1- and 9.9-foot elevations. The calculations were carried out to 60 seconds to determine if the temperature at the 2.1-foot elevation would turn over and quench. The calculations were terminated at that point, since no tendency to turn over or quench was observed. Quench at the 2-foot elevation occurred at 32.5 seconds in the FLECHT experiment.

The heat transfer regimes contained in the RELAP4 code are listed in Table II-I. The selected RELAP4 heat transfer regimes used on the heater rods in the RELAP4 model of the

TABLE II-I

HEAT TRANSFER COEFFICIENTS IN RELAP4

<u>Mode</u>	<u>Correlation</u>	<u>Comment</u>
1	Dittus-Boelter	Forced convection
2	Thom	Subcooled nucleate boiling
3	Schrock-Grossman	
4	McDonough, Milich, and King	Transition boiling
5	Groeneveld	Film boiling
6	Modified Bromley	Laminar pool film boiling
7	Free convection to steam	
8	Forced convection to vapor	
9	Dougall-Rohsenow	Basically forced convection

FLECHT experiment consisted of Modes 7 and 9. Mode 7 was selected in heat slab numbers 7 to 11 inclusive, and Mode 9 was used at all other nodes for most of the transient. RELAP4 predicts that the heat transfer coefficient increases with axial location up the rod. The fluid temperature quickly reached the saturation temperature (within 1.5 seconds), which is nearly the same in all volumes. Thus, the heat transfer is better at corresponding locations on the top of the rod than at the bottom. The code should select a different heat transfer regime at both the bottom and top locations. Part of this difficulty is due to the low density of the water in the lower location; the density of the fluid never does approach that of liquid. Densities at two locations are shown in Figure II-4 and II-5. The density at the inlet does not reach 15 lbm/ft³ while the density at 2.1 feet does not reach 3 lbm/ft³, and both values are far from all liquid. The low densities at the inlet are of course due to the assumed entry of a homogeneous saturated fluid.

It can be concluded from this calculation that the RELAP4 calculation with saturated inlet fluid predicts much higher temperatures in the lower part of the rods and lower temperatures in the upper part of the rods than those which occurred in the FLECHT experiment.

Fig. II-4 RELAP4-calculated fluid density 0.3 foot above inlet of rod bundle (saturated inlet fluid assumed).

Fig. II-5 RELAP4-calculated fluid density 2.1 feet above inlet of rod bundle (saturated inlet fluid assumed).

2.2.2 Subcooled Inlet Fluid with No Slip. Since the FLECHT experiment conditions included subcooled inlet water, a computer run was made which assumed subcooled inlet water (115°F). The results were different than the results which assumed introduction of saturated water. This particular problem is difficult to run with RELAP4. A very small time step must be used when the steam-water interface crosses a junction because there is a sharp discontinuity in the junction flow rate at that time. As an explanation, the steam in the tube flows downward as it condenses on the subcooled water, while flow of the subcooled water is upward. Consequently, when the interface crosses the junction, the mass flow rate switches from a negative to a large positive value almost instantaneously; very small time steps are thus needed to accommodate this occurrence. This problem increased the code running time by a factor of three.

Figures II-6 and II-7 show calculated temperatures at the 2- and 10-foot elevations. Calculations for the subcooled case were performed to only 15 seconds because of the computer costs. The temperature at 2.1 feet turned over at 12 seconds in the subcooled case whereas the temperature in the case which assumed saturated inlet fluid did not turn over within 62 seconds. The measured FLECHT temperature turned over at 8 seconds. The calculated temperature for the subcooled run at the 9.9-foot elevation did not turn over. The density for the subcooled run at the 2.1-foot elevation was much higher (19.8 lbm/ft³

Fig. II-6 RELAP4 calculations and FLECHT data comparisons at 2-foot level.

Fig. II-7 RELAP4 calculations and FLECHT data comparisons at 10-foot level.

at 12 seconds and 42.3 lbm/ft³ at 15 seconds) than in the saturated case. By comparison, the density at the 2.1-foot elevation for the saturated inlet fluid case was much lower, as shown in Figure II-5.

In summary, the measured temperatures agreed with the computed temperature values for the subcooled run.

2.2.3 Saturated Inlet Fluid with Slip. For this run, the standard slip option in RELAP4 with saturated inlet water was used. The code became unstable at 2 seconds. Another computation was performed with a constant slip velocity of 1 ft/sec. It appeared to run as well as RELAP4 without slip. The temperatures calculated were very similar for the saturated runs with and without slip (see Figures II-6 and II-7). The densities calculated were slightly higher.

A run was attempted at 5 ft/sec, but it became unstable. Since the standard slip model yields slip velocities larger than 5 ft/sec., the slip model, in its present state, does not appear to be useful in solving the reflood problem.

2.2.4 Conclusions. RELAP4 calculations were made using a model consisting of 20 control volumes to simulate a FLECHT experiment. A comparison of the data indicates that RELAP4 calculated temperatures do not agree well with the recorded FLECHT experiment temperatures when saturated inlet fluid is assumed. Comparisons showed reasonable agreement when subcooled inlet fluid (as used in the FLECHT experiment) is used, but computer time increased significantly. It was concluded that approximate methods (the FLOOD4 code) should be used for LOFT reflood analysis until RELAP4/MOD6 is available.

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III. THERMAL FUELS BEHAVIOR PROGRAM

J. G. Crocker, Program Manager

Accomplishments of the Thermal Fuels Behavior Program during October – December 1975 include the completion of the PCM-20 CHF Scoping Test metallurgical examinations and the Irradiation Effects Scoping Test 1.

The following sections describe: (a) metallurgical test results from the PCM-20 CHF Scoping Test; (b) preliminary test results from the Irradiation Effects Scoping Test 1; (c) error analysis of spring-loaded thermocouples used in the Irradiation Effects (IE) Program; and (d) fuel rod local power determination.

1. PCM-20 CHF SCOPING TEST, POSTIRRADIATION EXAMINATION

G. R. Smolik and R. R. Hobbins

1.1 Introduction

The purpose and preliminary test results of the CHF Scoping Test were briefly discussed in the previous quarterly report^[1-7]. During the current quarter, the posttest metallurgical examinations were conducted on the fuel rod.

Following the Power Burst Facility (PBF) in-pile tests, the fuel rod was transferred to the INEL hot cells for examination. The principal results of these examinations are discussed in the following sections.

1.2 Visual and Photographic Examinations

The fuel rod was visually inspected through the hot cell windows and with the hot cell periscopes; the rod was photographed through the hot cell windows at approximately one-fourth actual size and at a magnification of approximately 3.7 times. Sufficient photographs were taken to construct composites of the entire length of the fuel rod. The photographs were taken at four orientations and were designated 0, 90, 180, and 270 degrees to correspond respectively with the sides on which thermocouples were located 19, 25, 21, and 27 inches from the bottom of the fuel stack.

Observations through the periscope and photographs in Figure III-1 showed that the lower 14 inches and the upper 10 inches of the fuel rod had experienced normal operating

Overall view of rod showing extent of deflection.

View of rod at higher magnification shown details of rod condition

Higher magnification photographs showing details of rod conditions.

Proto No. 75-4738

Fig. III-1 PCM-20 CHF Scoping Test fuel rod UT-A-0005 showing posttest condition (photographed from 90-degree orientation).

conditions. This is indicated by the lustrous black oxide which existed as a continuous adherent film typical of those developed in 680°F autoclave water tests. At locations between 14 and 29-1/2 inches from the bottom of the fuel stack, the oxide layer showed discoloration and loss of adherence indicating higher temperature exposures. The lowest position at which oxide had been lost was 17-1/2 inches from the bottom of the fuel stack when examined from the 90-degree side. It is believed that much of the oxide loss was caused by thermal stresses or by mechanical stresses which resulted when the fuel rod bowed. The fuel rod appeared horizontally deflected approximately one inch over the full length of the rod. It was not uniformly bowed, but rather a concentrated bend was located 30-1/2 inches from the bottom of the fuel stack.

Photographs showed evidence of necked regions, or waisting, at pellet interfaces indicating that cladding collapse had occurred. Waisting appeared most predominantly at the interval of 22 to 23-1/2 inches and at 26-1/2 and 28-1/2 inches from the bottom of the fuel stack when viewed from the 90-degree orientation. Collapse probably occurred between these locations; however, waisting may have been visually obscured by the spalling of oxide.

1.3 Posttest Measurement of Rod Diameters

The external diameter of the test rod was measured on two perpendicular planes, at 45 and 135 degrees. Measurements were made with a micrometer, and the accuracy was estimated to be within ± 0.001 inch. The diameters on these planes are plotted with respect to the distance from the bottom of the fuel stack in Figure III-2.

Diameters were reduced by 0.003 inch or more from 19 to 31 inches above the bottom of the fuel stack. Greatest reductions in diameter, some exceeding 0.005 inch, were measured from 19 to 24 inches. Similar reductions in diameter were experienced on each plane, which indicates that the cladding collapsed into a circular rather than an elliptical geometry.

1.4 Metallographic Examination

The test fuel was sectioned for posttest examinations according to the schedule outlined in Table III-I. Metallographic examinations of the cladding and of the fuel were conducted.

1.4.1 Cladding Investigations. Thicknesses of ZrO_2 layers and oxygen-stabilized α -zirconium were measured at various cross sections of the fuel rod. This information was then applied to the growth rate equation $\xi = 1.6 (Dt)^{1/2}$ using the diffusion coefficient of oxygen in zirconium $D = 0.196 \exp(-41,000/RT)$, in which t = time (seconds); R = the gas constant; and T = temperature in ($^{\circ}\text{K}$), as presented by Lemmon^[III-1]. This equation, expressing ξ as the combined thickness of ZrO_2 and

Fig. III-2 Diametral measurements of CHF Scoping Test fuel rod.

oxygen-stabilized α -zirconium, had been used to successfully predict cladding temperatures in the examination of a PCM-20 test fuel rod. The observed thickness and calculated temperature at various locations of PCM-20 test fuel rod UT-A-0005 are listed in Table III-II. The duration that the fuel rod was subjected to conditions above transition boiling was taken to be 60 seconds. The gas constant R was expressed as .1987 cal/K-mole. Temperatures were therefore calculated as degrees Kelvin and converted to Fahrenheit.

Photographs in Figure III-1 and metallographic cross sections showed that oxide had broken off at some locations. Loss of oxide would cause calculated values to be lower than actual temperatures. Temperatures calculated for various cross sections are correlated with the locations in Figure III-3. Figures are also included which illustrate the microstructures of the cladding observed at various locations and are shown in Figures III-4 through III-10.

TABLE III-I

EXAMINATION SCHEDULE FOR PCM-20 TEST FUEL ROD UT-A-0005

Distance of Section From Bottom of Fuel Stack (inches)	Orientation and Location of Surface Examined[a]	Purpose of Examination
6-3/16 to 6-11/16	----	Analyze fission products
6-11/16 to 7-7/16	T:6-3/4	Determine fuel and cladding structures at a location having experienced low levels of power generation
7-7/16 to 8-3/16	L:45°	Investigate cladding oxidation characteristics
16-9/16 to 17-1/16	----	Analyze fission products
17-1/16 to 17-13/16	T:17-11/16	Perform cladding structural examinations near a postulated location of boiling transition
17-13/16 to 17-15/16	----	Analyze gas in cladding
18-13/16 to 19-9/16	L:145°	Investigate oxide spalling on cladding
20-11/16 to 21-7/16	T:20-15/16	Examine fuel and cladding structures near thermocouple located 21 in. from bottom of fuel stack
22-5/16 to 23-1/16	T:23	Examine fuel and cladding structures near the hottest anticipated temperature of the rod
23-1/16 to 23-13/16	L:45° & 225°	Investigate for cladding collapse at pellet interfaces
23-13/16 to 24-9/16	T:24-1/2	Investigate fuel and cladding structures
26-9/16 to 27-3/16	T:26-11/16	Examine fuel and cladding structures near centerline thermocouple.
28-1/16 to 28-13/16	T:28-3/16	Investigate fuel and cladding structures near a postulated location of boiling transition
28-13/16 to 29-5/16	----	Analyze fission products

[a] T designates a transverse section; L designates a longitudinal section. An azimuthal orientation was selected such that thermocouples located 19, 25, 21, and 27 inches from the bottom of the fuel stack were established as 0, 90, 180 and 270 degrees, respectively.

TABLE III-II
CALCULATED CLADDING TEMPERATURES FOR OXIDE THICKNESS

Location from Bottom of Fuel Stack (in.)	Azimuthal Orientation (degrees)	ZrO ₂ + Stabilized α -Zr Thickness (cm)	Calculated Temperature (°F) [a]
6-3/4	0-360	none resolvable	----
17-11/16	45	0.0008	1,640
	350	0.0002	1,360
20-15/16	180 [b]	0.0005	1,540
	315	0.0015	1,800
23	45 [c]	0.0025	1,950
	180	to	to
	330	0.003	2,010
24-1/2	90	0.0014	1,760
	300	0.002	1,890
26-11/16	0	0.0025	1,950
	150	0.002	1,890
	180	0.0015	1,800
28-3/16	0	none resolvable	----
	150	0.001	1,770
	300	0.0013	1,700

[a] Times for which CHF existed at various locations along the test rod varied from 40 to 65 seconds. There is also a small amount of distortion near the outer edges of photomicrographs. These factors result in uncertainties of -30 to +65°F for calculated values assuming time equal to 60 seconds.

[b] The 180-degree location was near an attached thermocouple. The structure was equiaxed recrystallized α -Zr grains at this location which indicates a lower exposure temperature.

[c] Much of the oxide was lost from 0 to 180 degrees; however, intermittent patches indicated layers from 0.0025 to 0.003-cm thick had been formed.

Structures observed at various cross sections give indications of exposure temperatures. At locations of less than 8-3/16 inches from the bottom of the fuel stack, as received structure was observed. This shows that the cladding was not heated above the recrystallization temperature of about 1,200°F. Two distinct types of structures were observed at cross sections 17-11/16 and 18-3/16 inches from the bottom of the fuel stack. Equiaxed recrystallized α grains were observed around portions of the cladding. This type of structure indicates that the $\alpha \rightarrow \alpha + \beta$ transition temperature of approximately 1,530°F had not been exceeded. Transformed martensitic structures formed from prior β grains were observed on the same cross sections. This latter structure indicates the cladding was heated into the $\alpha + \beta$ region or above the $\alpha + \beta \rightarrow \beta$ transformation temperature. Temperatures required for the above transformations are approximately 1,530 and 1,780°F, respectively. A good correlation was noted between the orientations at which the recrystallized α structures were found and can be found in Figures

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Fig. III-3 View of CHF Scoping Test fuel rod showing cladding surface temperature distribution along the fuel stack as calculated by thickness of ZrO_2 and oxygen stabilized α -Zr layers.

Photo No. 75J96

Etched; Polarized Light

500X

Fig. III-4 Transverse section of CHF Scoping Test fuel rod showing microstructure 6-3/4 inches from bottom of fuel stack.

Etched; Polarized Light 100X

Etched; Polarized Light 100X

Fig. III-5 Transverse section of CHF Scoping Test fuel rod showing microstructures observed 17-11/16 inches from bottom of fuel stack.

Fig. III-6 Transverse section of CHF Scoping Test fuel rod showing microstructures on the plane of an external thermocouple located 20-15/16 inches from bottom of fuel stack.

Photo No. 75J40

Etched; Polarized Light

100X

Fig. III-7 Transverse section of CHF Scoping Test fuel rod showing microstructure on a plane 23 inches from bottom of fuel stack.

Photo No. 75J74

Etched; Polarized Light

100X

Fig. III-8 Transverse section of CHF Scoping Test fuel rod showing martensitic transformed prior β grain structure at 24-1/2 inches from bottom of fuel stack.

Photo No. 75J54

180°

Photo No. 75J56

Etched; Polarized Light

100X

Etched; Polarized Light

100X

Fig. III-9 Transverse section of CHF Scoping Test fuel rod showing microstructure 26-11/16 inches from bottom of fuel stack.

Fig. III-10 Transverse section of CHF Scoping Test fuel rod showing recrystallized α -Zr and martensitic transformed prior β grains observed 28-3/16 inches from bottom of fuel stack.

III-5 and III-10 and the lower temperatures calculated from oxide thicknesses in Table III-II. Figure III-7 shows transformed martensitic structure in large prior β grains over the entire cross section 23 inches from the bottom of the fuel stack. This structure indicates a cladding temperature well in excess of 1,780°F. The calculated temperatures in Table III-II also indicate this location had experienced the highest temperature (2,010°F).

There was some indication that attached external instrumentation caused irregularities in cladding temperatures. Figures III-5, III-6, III-8, and III-9 indicate recrystallized α grains; i.e., lower temperature had been experienced beneath the thermocouple located at 180 degrees. A composite illustrating the microstructures near the 180-degree thermocouple (located 20-15/16 inches from the bottom of the fuel stack) is presented in Figure III-11.

Cladding collapse was observed at a void between two pellets. This is illustrated by the longitudinal section in Figure III-12. The section located 23-1/16 to 23-13/16 inches from the bottom of the fuel stack corresponds with a location at which waisting was visually observed. Chung et al.^[III-2] indicate that such deformation would probably occur between 1,470 and 1,560°F. Indications are that the cladding collapsed during the first high temperature excursion. This is suggested by the fairly continuous oxide layer on the concave section of the cladding. The oxide apparently formed after the cladding had collapsed. The zircaloy cladding would also be more ductile and likely to collapse before significant oxygen would be absorbed. Evidence of cladding collapse and indications of pellet-to-cladding gaps were observed from transverse sections of the test rod. Figure III-13 shows macrographs of sections 6-3/4, 20-15/16 and 26-11/16 inches from the bottom of the fuel stack. The macrographs at 20-15/16 and 26-11/16 inches show the UO_2 fuel intact and the fuel-to-cladding spacings to be uniformly small. It was therefore believed that fair estimates of fuel-to-cladding gaps could be obtained from 100X micrographs. Radial gaps of 0.005 and 0.0012 inch were measured from the micrographs for the 20-15/16-inch position (Figure III-6) and the 26-11/16-inch position (Figure III-9). These measurements show good agreement with the difference of 0.0015 inch between micrometer measured external diameters shown for similar positions in Figure III-2. Microstructure observed on the inner wall of the cladding showed that fuel and cladding had chemically interacted. Oxygen-stabilized α -zirconium indicated that oxygen was transferred from the fuel into the Zircaloy-4 cladding during contact at high temperatures. This phase was most commonly observed at the hottest region of the rod where the posttest cold-fuel-to-cladding gaps were smallest. The section of cladding in Figure III-7 illustrates this phenomenon. Stabilized α -zirconium is also shown on the collapsed cladding in Figure III-12. The alpha phase exists only on surfaces which mated with the fuel. This indicates that contact is instrumental in forming the oxygen-stabilized alpha phase on the inner wall.

180 degree

Photo No. 76-40

50X

Fig. III-11 Transverse section of CHF Scoping Test fuel rod showing the microstructures near the 180-degree thermocouple on a plane located 20-15/16 inches from the bottom of the fuel stack.

Fig. III-12 Longitudinal section of CHF Scoping Test fuel rod showing collapse of zircaloy cladding into a gap between fuel pellets at a location 23-1/2 inches above bottom of fuel stack.

1.4.2 UO₂ Fuel Structure Investigation. Structures of the UO₂ fuel were examined at the locations indicated in Table III-III. The metallographic sections were polished and etched.

No evidence of melting near the centers of pellets was found and porosity was noted throughout the fuel. Apparently the exposure time was not sufficient to have caused significant void migration. There was evidence of grain growth and some void agglomeration toward the centers of fuel pellets located 6-3/4 to 25-11/16 inches above the bottom of the fuel stack. The most significant grain growth was noted in a range of 18-7/8 to 23-3/4 inches from the bottom of the fuel stack. The locations and grain sizes measured near the edges and at interiors of fuel pellets are listed in Table III-III. An example of the fuel structure outside of the hot section of the rod is shown in Figure III-14. This photomicrograph shows a grain size of 3.0 μm near the edge of the fuel, 6-3/4 inches from the bottom of the fuel stack. Maximum grain growth, near the instrumentation hole, was 23 inches from the bottom of the fuel stack and grain sizes at this location were found to be 13.7 μm. Four inches lower, grain sizes of 23.1 μm were found at the center of a fuel pellet not drilled for instrumentation. This indicates that exposure temperatures were high, or that times at temperatures were greater in pellets below the instrumentation hole than in drilled pellets. This agrees with the axial power distribution. Example of the grain structure found at the latter two positions are shown in Figure III-15 and III-16.

Fig. III-13 Transverse sections showing macrographs of CHF Sooping Test fuel rod located (a) 6-3/4, (b) 20-15/16, and (c) 26-11/16 inches from bottom of fuel stack.

TABLE III-III
GRAIN SIZES AND CALCULATED TEMPERATURES OF UO_2 FUEL^[a]

Location from Bottom of Fuel Stack (in.)	Grain Size Near Edge (μm)	Grain Size Near Center (μm)	Calculated Temp. ^[a] Near Center ($^{\circ}F$)
6-3/4	3.0	4.6	3840-3925
18-7/8	2.5	23.1	4900-5030 ^[b]
20-15/16	2.9	13.5	4515-4625
23	3.2	13.7	4525-4635
23-3/4	---	11.7	4400-4530
26-11/16	3.4	4.6	3840-3925
28-3/16	---	3.2	---

[a] The range of temperature at each location was obtained by assuming the accumulated time at maximum temperature varied from 40 to 65 seconds.

[b] The fuel pellet was complete at this position. A central hole for instrumentation existed in all sections above this location.

Photo No. 75J77

Etched

500X

Fig. III-14 Transverse section of CHF Scoping Test fuel rod showing UO_2 fuel microstructure near edge of a pellet 6-3/4 inches above bottom of fuel stack.

Photo No. 75J90

Etched

500X

Fig. III-15 Transverse section of CHF Scoping Test fuel rod showing grain sizes of $13.7 \mu\text{m}$ in UO_2 fuel near instrumentation hole located 23 inches from bottom of fuel stack.

Photo No. 75J10

Etched

500X

Fig. III-16 Longitudinal section of CHF Scoping Test fuel rod showing UO_2 microstructure at center of a solid pellet 18-7/8 inches above bottom of fuel stack.

A grain growth relationship presented by Nichols^[III-3] was used to calculate fuel exposure temperatures from CHF durations. This relationship is expressed as:

$$D^3 - D_0^3 = k_0 t \exp (-Q/RT)$$

where

D	=	Final grain size (μm)
D_0	=	initial grain size (μm)
t	=	exposure time (hr)
T	=	exposure temperature (K)
R	=	gas constant (1.987 cal/K-mole)
k_0	=	$8.57 \times 10^{14} \mu\text{m}^3/\text{hr}$
Q	=	124,000 cal/mole

Temperatures calculated by this technique should be interpreted with caution. The growth rate equation was developed for long time applications whereas the PCM-20 rod underwent short time temperature transients. Nevertheless, temperatures which were calculated using times from 40 to 65 seconds are listed in Table III-III. Calculations indicated that the temperature of the fuel near the centerline thermocouple was from 3,840 to 3,925°F. Temperature ranges indicated for positions near the instrumentation hole were 4,525 to 4,635°F and at the center of an undrilled solid pellet were 4,900 to 5,030°F.

Cracking patterns of the fuel are shown in the macrographs of Figure III-13. The primary cracks are radial and taper toward the outside edge of the pellets. Such indications of crack propagation from pellet centers are typical of cracks formed during cooling. There is no evidence of circumferential cracks around central regions of enhanced grain growth. Radial cracks also did not show any termination or alteration as a result of high temperature healing. This indicates that such cracks formed during one of the cycles and persisted throughout the remainder of the experiment.

1.5 Summary of Postirradiation Examinations

The primary observations noted in the postirradiation examination involving visual, photographic, and metallographic techniques are:

- (1) Cladding collapsed symmetrically at hot regions of the test rod. Reductions of external diameters were equal to or greater than 0.003 inch from 19 to 31 inches above the bottom of the fuel stack. Greatest reductions in diameter, some of which exceeded 0.005 inch, occurred between 19 and 24 inches.
- (2) Metallographic structures indicative of highest exposure temperature were observed approximately 23 inches from the bottom of the fuel stack. A cladding exposure temperature of 2,000°F was calculated from ZrO_2 and

stabilized α -Zr thickness at this location. Martensitic structures observed in sections 17-11/16 and 28-3/16 inches from the bottom of the fuel stack showed that cladding temperatures of greater than 1,530°F occurred between these locations. Visual appearance indicated that temperatures greater than normal operating conditions existed from 14 to 29-1/2 inches above the bottom of the fuel stack.

- (3) Structures of both recrystallized α grains and transformed prior β grains were observed at sections 17-11/16 and 28-3/16 inches from the bottom of the fuel stack. This indicates temperature differences of 250°F or greater existed circumferentially on the cladding at these locations. Exposure temperatures calculated from oxide thickness agree with these estimated temperature differentials.
- (4) Oxygen-stabilized α -zirconium formed on the inside of the cladding as a result of contact between the fuel and cladding at high temperatures.
- (5) External instrumentation attached to the test fuel rod appears to cool the cladding locally.
- (6) Cladding collapsed into the voids at pellet interfaces. Photographic examinations indicated that collapse occurred at locations from 22 to 28-1/2 inches from the bottom of the fuel stack. A metallographic section confirmed that this phenomenon occurred at a location of 23 1/2 inches.
- (7) The highest UO_2 fuel temperatures, as indicated by grain growth, were between 18-7/8 and 23-3/4 inches from the bottom of the fuel stack. Grain growth was also greater near the center of pellets below the instrumentation hole than in drilled pellets. The cracking pattern is characterized by radial cracks that formed during cooling and remained throughout the test.

2. IRRADIATION EFFECTS SCOPING TEST 1, PRELIMINARY TEST RESULTS

W. J. Quapp

2.1 Introduction

The first test in the Irradiation Effects Test Series, IE Scoping Test 1, which is part of the Nuclear Regulatory Commission's program to define the behavior of fuel rods during transient reactor conditions^[III-4], was conducted on October 30, 31, and November 1, 1975, in the PBF at the Idaho National Engineering Laboratory (INEL).

This test was conducted to evaluate methods and procedures for remote fabrication of irradiated fuel rods, test instruments, handling plans, and reactor capability prior to the actual beginning of the test series. The test was conducted with the single rod test hardware shown in Figures III-17 and III-18. Figure III-17 is a schematic of the test train installed inside the PBF in-pile tube. In Figure III-18, the fuel rod is shown installed inside the test shroud with the locations of the major instrumentation noted. A complete description of the test objectives, hardware, and results will be presented in the test results reports^[III-5]. The test used an unirradiated, UO_2 , archival Saxton fuel rod (supplied by Westinghouse Electric Corporation) which was modified at the INEL to contain an internal centerline fuel thermocouple. Future tests in this series will use irradiated UO_2 rods from the Saxton reactor. In addition to the scoping nature of the test, fuel behavior data were acquired which will be part of a larger data base on many fuel rods to be used for development and verification of fuel behavior codes.

2.2 Test Conduct and Results

The test included a 21.5-hour preconditioning period as shown in Figure III-19, during which time the fuel rod performance was nominally similar to that experienced during normal low-power operation in a power reactor. Following the preconditioning period, a power ramp brought the fuel rod to about 20.4 kW/ft peak linear heat rating. After about 78 minutes of operation at these conditions, a flow reduction transient was initiated to induce film boiling. After a flow reduction from 1.83×10^6 lb/hr-ft² to 9.6×10^5 lb/hr-ft², film boiling was indicated by a fuel rod length change and temperature excursions on four of the cladding surface thermocouples located at 23.8 and 27.3 inches above the bottom of the fuel rod (see Figure III-20). After approximately 2 minutes, the flow was further reduced to 7.95×10^5 lb/hr-ft², after which film boiling was observed to extend downward on the fuel rod to the 19.8-inch thermocouple location.

Based on the thermocouple readings, the fuel rod experienced film boiling for a total of 4.8 minutes over the 23.8- to 27.8-inch region. The region of the fuel rod around the 19.8-inch thermocouple was in film boiling for 1.4 minutes. The test was terminated by a relatively fast run-in of the control rods. A temporary reduction in the coolant flow through the test shroud and an increase of the fuel rod internal pressure was observed at approximately 80 seconds following the shutdown. This flow reduction was presumed to be associated with fuel rod failure since radiation alarms indicated fission product activity in the loop shortly thereafter.

Fuel rod failure was later confirmed during removal of the fuel rod and shroud from the test train. The fuel rod was found to be heavily oxidized over the 18.75-inch to 32.0-inch elevation. A portion of the fuel rod from the 21.5-inch to 23.75-inch elevation (see Figure III-21) had broken within the shroud during the handling process. The fuel rod failure is considered to be a result of the stresses associated with flow-induced vibrations and thermal stresses after cooldown of the heavily oxidized and completely embrittled cladding.

Fig. III-17 IE Scoping Test 1 experiment: assembly and instrumentation shown installed in PBF in-pile tube.

Fig. III-18 IE Scoping Test 1 fuel rod and instrumentation as installed in flow shroud.

Fig. III-19 Rod power and coolant flow history during IE Scoping Test 1.

Fig. III-20 Flowrate, cladding temperature, cladding length, and rod power during film boiling operation.

Fig. III-21 IE Scoping Test 1 fuel rod showing spalling oxide and break area in film boiling region.

Two types of cladding surface thermocouples were used on the experiment fuel rod: (a) zircaloy clad thermocouples with tungsten-rhenium alloy thermoelements (one wire of W/5% Re; second wire of W/26% Re) which were spring-loaded against the fuel rod cladding normal to the surface (Thermocouples 1 to 6), and (b) titanium clad thermocouples with chromel-alumel thermoelements which were laser welded against the cladding surface (Thermocouples 7 and 8). Based on the high cladding temperatures necessary to cause the extensive corrosion observed, it was concluded that the spring-loaded thermocouples (Thermocouples 1 to 6) were not measuring the cladding surface temperature but a temperature at least 1,000°F lower. Thermocouple 7, a welded junction thermocouple at the 19.8-inch elevation, gave the highest reading of about 1,900°F. A postirradiation examination of this rod is underway.

Preliminary examinations of the zircaloy cladding oxidation were used for estimating the peak cladding temperatures. The examinations revealed peak temperatures of approximately 2,300 and 2,000°F at the 19.8-inch and 27.8-inch elevations, respectively. The balance of the postirradiation examination is expected to be completed early in 1976, and the results will appear in a later quarterly report. The results of this experiment and others in this series to follow will be used for development and comparison against predictions of computer codes such as the FRAP-T^[III-6] transient fuel model code.

3. IRRADIATION EFFECTS PROGRAM SPRING-LOADED THERMOCOUPLE ERROR ANALYSIS

K. R. Perkins

3.1 Introduction

Fuel rods irradiated prior to their use in irradiation effects experiments are employed to obtain baseline data on irradiated rod behavior. Due to radiation levels, it is necessary to attach the required fuel cladding surface thermocouples while the rods are in a hot cell. Thus a remote attachment technique is required.

The prototype spring-loaded thermocouple probe shown in Figures III-22 and-23 has been used in the IE (Irradiation Effects) Scoping Test I discussed above. But the occurrence of unexpectedly large errors (on the order of 1,000°C) during periods of film boiling forced a reevaluation of potential error sources. These errors are estimated by comparison with thermocouples welded to the cladding at the same axial location but at different azimuthal locations. Thus the difference in the reading may be due to asymmetric fluid conditions, but during periods of stable film boiling this is extremely unlikely.

Fig. III-22 Prototype spring-loaded thermocouple probe installed.

Fig. III-23 Structural schematic of prototype spring-loaded thermocouple probe end.

The following analysis has been performed assuming the conditions of the first IE Scoping Test.

3.2 Analysis

In addition to the measurement errors associated with using a thermocouple to measure wall temperatures, there may be disturbing effects on the flow which change the local heat transfer coefficient (and thus change the quantity to be measured). However, at the thermocouple location the disturbing influence is proportional to the blocked area, which is only about 3% of the flow area for the

spring-loaded thermocouple. Downstream effects may occur due to the turbulent wake behind a thermocouple. Therefore, experimental results for regions within 40 diameters (approximate turbulent development length) of a major disturbance should be viewed with caution, but the turbulence behind the relatively small thermocouple probe should be no more severe than that generated by grid spacers in a PWR.

The main sources of the thermocouple errors are expected to be the thermal contact resistance between the probe and the cladding, the thermocouple conduction error, and possible oxide buildup. An accurate treatment of these three interrelated effects would require a three-dimensional numerical model of the complicated rod-probe geometry. However, an estimate of the relative magnitude of each contributing error can be obtained using several simplifying assumptions as outlined below.

3.3 Thermocouple Conduction Errors

Since the tungsten-rhenium wires are surrounded by BeO insulation, conduction down the thermocouple wires can be neglected. However, during periods when film boiling occurs on the fuel rod, the thermocouple sheath can act effectively as a heat conductor to the cooler surrounding fluid, reducing the local cladding surface temperature. This locally suppressed temperature is the temperature which is measured by the thermocouple (neglecting additional errors to be discussed). The magnitude of the local temperature suppression can be estimated using thin fin theory with several simplifying assumptions.

In subcooled film boiling the vapor thickness is estimated to be 0.003 inch, based on the assumption of a laminar boundary layer. While in saturated film boiling the thickness of the vapor layer can be estimated from an energy balance on the generated vapor and is calculated to vary from 0.033 inch to 0.040 inch within 10 inches downstream of that point of departure from nucleate boiling (DNB). Since the vapor blanket which occurs during low-quality film boiling is on the order of 0.010 inch, it is assumed that the entire thermocouple probe remains outside the vapor blanket. The convective heat transfer coefficient for flow over a small diameter rod (the thermocouple probe) is given in Reference III-7.

For the conditions of the IE Scoping Test, the calculated heat transfer coefficient is 7,500 Btu/hr-ft²-°F. With such a high heat transfer coefficient even a short fin might be very effective in cooling. In fact, Equation 11-30 of Reference III-8 indicates that even the 0.12-inch cooling length results in a heat flux at the cladding surface which is within one percent of that which would occur for an infinite fin.

The simplifications from the foregoing allow assuming a system analogous to that of Schneider^[III-9] and the difference between the fuel surface temperature and the thermocouple reading can be determined by a similar development.

The surface temperature of the fuel can be found in terms of the cladding surface temperature and the gas gap conductance from an energy balance. The fuel surface temperature can then be combined with the preceding temperature difference calculation to give the thermocouple conduction error.

For the conditions of the IE Scoping Test, the thermocouple conduction error was calculated to be approximately 170°F. It should be noted that several approximations were used in calculating the difference between cladding surface temperature and the thermocouple indication which may affect the accuracy of the result. It is therefore recommended that a more rigorous development be used to obtain a correction factor for the thermocouple conduction error. The present results are intended only to approximate the scope of the problem. A worst case solution may also be obtained by assuming that the fuel rod surface is perfectly insulated. This development results in an error estimate of 46% in the difference between cladding surface temperature and coolant temperature, an error of 660°F for the conditions of the IE Scoping Test.

3.4 Thermal Contact Resistance

The contact force of 2.75 lbf on the thermocouple probe gives a contact pressure of 1,650 psi. At this high contact pressure, data presented by Rohsenow and Hartnett^[III-10] indicate that the thermal contact resistance is fairly insensitive to the surface finish and the contact medium (at these high pressures sharp roughness peaks are flattened and good metal-to-metal contact is achieved). There is expected to be a slight reduction in contact resistance as the zircaloy softens at elevated temperatures, but this effect is expected to be small (since the high contact pressure has already achieved good metal-to-metal contact), and the change in the contact resistance will not be noticeable during the thermal transient. Although contact coefficient data are not presented for zircaloy, the data in Reference III-10 are presented for a broad range of material conditions and the contact coefficient is estimated to be $1,000 \pm 500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$. Since this is of the same order as the film boiling heat transfer coefficient (about $500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$), the surface heat flux should be nearly constant (neglecting the contribution from thermocouple conduction) and the measurement error can be determined by dividing the surface heat flux by the contact coefficient. For the conditions of the IE Scoping Test the error due to thermal contact resistance is calculated to be approximately $660 \pm 500^\circ\text{F}$.

3.5 Oxide Buildup

A thick layer of ZrO_2 on the fuel rod would add additional thermal contact resistance between the rod and the thermocouple probe. If we assume a large oxide layer of 0.006 inch (15μ or about 2% of the cladding thickness) the thermocouple error for the peak heat flux in the IE Scoping Test is calculated to be approximately 110°F.

3.6 Conclusion

From the analysis it is evident that contact resistance dominates the total expected thermocouple error. Therefore, a means of reducing this resistance must be developed. One such means under investigation is the use of an electric discharge welding technique to attach the thermocouple to the cladding remotely.

If the large error from contact resistance (including oxide formation) can be eliminated, only the relatively small error due to the fin effect will remain. It is expected that analytical correction for the conduction error will result in acceptable measurement accuracy.

4. FUEL ROD LOCAL POWER DETERMINATION

W. J. Quapp and C. M. Allison

4.1 Introduction

In nearly all reactors a change in the axial flux distributions accompanies an increase in reactor power due to movement of the control rods. In Thermal Fuels Behavior Program experiments conducted in the PBF, a change in the flux distribution causes a corresponding change in the experiment power distribution, which must be accounted for when fuel performance measurements of a local nature (cladding and fuel temperatures) are made.

As an example of this effect, two flux profiles are presented in Figures III-24 and III-25. They have been prepared from data taken by gamma scanning an aluminum - 0.51% cobalt flux wire at 0.5 inch and 0.3 inch, respectively. The cobalt wire data in Figure III-24 were taken during the IE Scoping Test 1 reported in Section 2 above. This test lasted about 23 hours, during which time the reactor power varied from zero to 26 MW with a time average value of 7.1 MW. The data presented in Figure III-25 were from a neutron detector calibration test conducted for one hour at a nearly constant reactor power of 13.3 MW. The startup and shutdown ramps were made as steep as possible to minimize the time at reactor power levels other than 13.3 MW.

Comparing Figures III-24 and III-25, the peak-to-average values are 1.25 and 1.33, respectively. The broader peak seen in Figure III-24 results from operation over a wide range of reactor power and consequent shifting of the location of the peak axial flux. Since the reactor time-averaged power level was 7.1 MW for the gamma scan data of Figure III-24 versus 13.3 MW for Figure III-25, the location of the peak flux is seen to be 12 and 17 inches, respectively, corresponding to the lower position of the top entry control rods of the PBF at a lower power level.

Fig. III-24 Results of cobalt wire gamma scan from IE Scoping Test 1.

4.2 Technique

A technique that is essentially identical to that used in the Halden Reactor Project in Norway to determine the power distribution and local power changes has been implemented using axially distributed self-powered cobalt neutron detectors. The technique fits a Fourier sine series to the detector output to represent the axial flux profile. Normally, four or five detectors are sufficient to adequately describe the axial flux distribution. Comparisons against flux wire and fuel rod gamma scan data have been performed with resultant comparisons found to be very good. Figure III-26 presents one such comparison on a fuel rod from a Japanese experiment irradiated in Halden. The experiment contained three axially distributed flux sensors. The time-averaged signal from each flux sensor was determined for the last one month and last three months of operation. The solid lines represent the Fourier series representation of the flux profile. The triangles are data points taken from the ⁹⁵Zr/⁹⁵Nb gamma scan of that fuel rod by measuring the gamma ray intensities for isotopes ⁹⁵Zr and ⁹⁵Nb.

Similarly good comparisons have been made against the cobalt wire gamma scan data obtained from PBF irradiations.

Fig. III-25 Results of cobalt wire gamma scan from self-powered neutron detector calibration test.

An evaluation of the flux profiles during the Irradiation Effects Scoping Test 1 has been performed. The data are shown in Figure III-27 for rod linear power levels of 4, 7, 10, and 20 kW/ft. The peak can be seen to shift with increasing fuel rod power. The peak-to-average power is in good agreement with the gamma scan data from the flux wires obtained from the calibration test.

4.3: Conclusion

Future work in this area is being directed to developing an automatic interface between the test results data tape and the programs used to adapt the Fourier series to the detector output. This requires rewriting of the data tape with the calculated data added to the data bank for later analysis. The end result of this effort will allow direct presentation of data from devices such as fuel thermocouples for comparison with the actual local linear heat rating.

Fig. III-26 Axial flux profile as three-term sine function from average of power detector signals for last one-month and last three-month periods of operation compared with points from PIE gamma scan.

Fig. III-27 Axial flux profiles for indicated rod linear power levels during IE Scoping Test 1.

5. REFERENCES

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IV. REACTOR BEHAVIOR PROGRAM

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1. INTRODUCTION

Figure IV-1 illustrates the coupling between the various loss-of-coolant accident (LOCA) analysis codes currently under development within the Reactor Behavior Program. These codes include the following:

- (1) The RELAP4 reactor systems analysis code
- (2) The reactor containment codes CONTEMPT-LT, CONTEMPT4, BEACON and RELAP4
- (3) The fuel rod analysis codes FRAP-S and FRAP-T
- (4) The SCORE thermal-hydraulic analysis code.

As illustrated in the figure, RELAP4 is used to provide blowdown analyses of both pressurized water reactor (PWR) and boiling water reactor (BWR) reactor systems. It is used also to perform experimental systems and component thermal-hydraulic analyses.

Blowdown information obtained with RELAP4 also provides boundary conditions for the other codes indicated in the figure. That is, blowdown information from RELAP4 is fed into the containment analyses (mass and energy rates), fuel rod analyses (hydraulic conditions for determining heat transfer rates), and core analyses (hydraulic conditions at the core boundaries).

RELAP is undergoing a continuous upgrading process as successive versions (MODS) of the code are released. As illustrated in the figure, new and improved models are developed within the Advanced Model Development activity and incorporated into the code. Some of the more significant improvements to be made include reflood, drift flux, and nonequilibrium capabilities.

The containment analysis codes, CONTEMPT-LT, CONTEMPT4, RELAP4, and BEACON are used to predict the pressure-temperature response of reactor containments to hypothesized accidents. These codes also are continuously upgraded. The BEACON code currently under development will provide a transient, multidimensional containment analysis capability.

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Fig. IV-1 LOCA analysis programs.

The fuel rod analysis codes, FRAP-S (steady state) and FRAP-T (transient) are designed to predict fuel and cladding thermal and mechanical response and gap response under steady state operating, as well as transient and accident, conditions. Upgraded versions of both of these codes are prepared periodically at approximately 12-month intervals.

The SCORE code is a multidimensional thermal-hydraulic code used for detailed reactor core analyses. The initial production version of this code will be released as a homogeneous, equilibrium code. Future versions will provide drift flux and drift flux/unequal temperature capabilities.

2. LOCA ANALYSIS DEVELOPMENT

In computer code configuration control, problems encountered by users of the RELAP4 program^[a] during the reporting period were identified, analyzed, and corrected. Much of this activity was carried out in direct support of preparation and internal release of RELAP4/MOD5, as reported below. A revision history of RELAP4 was maintained and provides a traceable record of all code versions, up to and including RELAP4/MOD5, Update 1.

In the RELAP4 production code development effort, completion and checkout of the RELAP4/MOD5 computer program was accomplished. The downcomer model (based on Battelle-Columbus data), lower plenum entrainment model, and critical flow models [Homogeneous Equilibrium Model (HEM), Henry-Fauske/HEM and Modified Burnell/HEM] were completed. A number of so-called "dials" were also incorporated into RELAP4/MOD5. These "dials" are used to perform uncertainty analyses with RELAP4.

Checkout of RELAP4/MOD5 was accomplished by successfully completing seven checkout runs. These computer runs consisted of three PWR runs, two BWR runs, a loss-of-fluid test (LOFT) run, and a Semiscale run. The code performed well during the checkout and is substantially improved over RELAP4/MOD3, not only in terms of new models added, but also in terms of general code applicability. The new junction-smoothing option enables the code to run through problem areas where it would previously bog down, superheat or abort. Water-packing improvements have decreased running time significantly. RELAP4/MOD5 was made operational on both the CDC-7600 and the IBM-360. The MOD5 models are operating as designed, the code is free of known major programming errors (although several potential problem areas remain), and MOD5 runs in a reasonable time for typical applications.

[a] A description of the RELAP4 program is given in Reference I-10.

For RELAP experimental code development, the new blowdown and reflood heat transfer correlations and logic were developed, and the moving mesh scheme to be used in the core heat transfer calculations during reflood was implemented. These developments provide a capability to include new models in RELAP4/MOD6.

A review of the RELAP4 jet pump model was completed. While recognizing the shortcomings of the model, it was concluded from the review that the RELAP4 jet pump model is operating as designed. It was further concluded that any improvements that could be made to the jet pump model without additional data (either experimental or based on calculations from advanced multidimensional codes) would be minimal.

In the area of advanced LOCA model development, application of three numerical integration schemes to well- and ill-posed problems showed that in each case well posed difference systems were obtained for certain constraints but that none of the three schemes can be universally applied. Development of a two-pressure hydrodynamic model was completed; the two-pressure model was developed for the case of two-phase separated plane flow, including transverse momentum considerations, and provides additional physical features of flow phenomena compared to a single-pressure (hydrostatic) model. The two-pressure model was demonstrated to be a viable approach for the case of separated flow.

RELAP4 modularization was implemented to provide the framework for model additions to future versions of the program. The NIFTE subroutine was rewritten and the direct matrix inversion portion converted to a separate subroutine. The time advancement can now advance algebraic as well as differential momentum equations. This addition allows implicit numerical advancement techniques to be used for internal choking, downcomer models, and flooding models. The handling of boundary conditions was changed to eliminate nearly one-ninth of the terms in the equations for determining flow changes. A small sample problem shows a 25% reduction of computational time in NIFTE. In addition, the option of choosing a different numerical advancement technique was provided.

Work on the user-convenient SCORE/EVET code led to completion of input and output packages.

Documentation of the expansion and tees calculations^[IV-1] using the SPLEN-1 code was completed. Calculations to obtain the steady state flow solution in a plane channel expansion and tee demonstrated the capability of the code to make realistic calculations in recirculating flows.

2.1 Small-Break Analysis Models in the RELAP4 Computer Code

K. R. Katsma, S. R. Fischer, and W. H. Rettig

New analytical models were developed and incorporated into the RELAP4 computer code to analyze the thermal-hydraulic phenomena expected during a postulated small break in a large nuclear power plant. The new models include a vertical slip model, a horizontal slip model, an improved mixture level model, and an improved heat transfer model.

The vertical slip model is designed to provide realistic separation of steam and water in vertical system components during the relatively slow transients expected during postulated small breaks. The slip model computes the separated fluid component velocities and directions at a vertical junction. An empirical correlation was developed for calculating the slip velocities. The intent was to develop a slip correlation which was a simple function of the void fraction and which could be used to calculate slip velocities for void fractions $0 < \alpha < 1.0$. Use of the vertical slip model allows the energy transfer between control volumes to be based on individual liquid and vapor component flows rather than the net junction flow. The homogeneous momentum equation is not altered by the model, but the energy equation is modified. The vertical slip model when used with the bubble rise model in RELAP4 eliminates the vapor-mixture layering that previously was computed in vertically stacked control volumes.

The flow in horizontal system components may become stratified during the slow transients expected in small-break accidents. To model this phenomenon, a horizontal slip model was developed that allows for both cocurrent and countercurrent flow in horizontal components. A paired junction approach is used where steam flows through the upper junction and the two-phase mixture flows through the lower junction. The flow areas, inertias, and friction losses associated with each of the two junctions are dependent on the relative amount of steam and two-phase mixture present.

The new mixture level model allows a single mixture level to be tracked through a series of vertically stacked control volumes.

The existing heat transfer correlation model was modified to allow different heat transfer correlations to be used above and below a steam two-phase mixture interface within a control volume. The new model utilizes a local quality based on the bubble rise model to determine the appropriate heat transfer regime. The volume average quality was utilized in earlier versions of the code.

Figure IV-2 indicates the RELAP4 small-break prediction of core heat slab temperatures for a postulated 0.1-ft² cold-leg break in a PWR. The core heat slab temperatures, as the mixture level drops through the active core length, are shown.

As the upper slabs uncover, reduced heat transfer and higher cladding temperatures result. In previous RELAP4 calculations, without small-break models, all three heat slabs followed the same time-temperature history as shown by Heat Slab 1 in Figure IV-2.

The new models developed for small-break analysis have been incorporated into a current version of RELAP4/MOD5 and have been evaluated using a variety of simple test problems, experimental data and models of large reactor systems. The new models are reviewed in detail in Reference IV-2. Calculations made with and without the new models are compared and discussed.

Computational results for a spectrum of small breaks in a large PWR plant are also presented. The models appear to give physically realistic results and, in general, improve the agreement between experimental and analytical data.

2.2 Henry-Fauske/Homogeneous Equilibrium Critical Flow Model T. L. DeYoung

Three new critical flow models have been developed and incorporated in the RELAP4 computer code^[I-10]. These are: (a) HEM; (b) the modified momentum/HEM; and (c) the Henry-Fauske/HEM. The latter is described here, and its application to the RELAP4 computer code is discussed. Results obtained with the Henry-Fauske/HEM are compared to those using the Moody, Sonic, and Henry-Fauske critical flow models currently in the RELAP4 code.

Indications are that the break flow attained with the Henry-Fauske/HEM is initially lower but later in the transient is higher than that observed using the other critical flow models. A consequence of this variation is a somewhat slower system depressurization than experienced with either the Moody, Henry-Fauske, or Sonic critical discharge models.

2.2.1 Model Description. As the name implies, the Henry-Fauske/HEM is a combination of the Henry-Fauske critical flow model^[IV-3] and the homogeneous equilibrium critical flow model. The Henry-Fauske model is applied in the subcooled region (Figure IV-3) with complete conversion to the homogeneous equilibrium model at transition quality X_T which may be varied from the nominal value of 0.02.

Fig. IV-2 Core heat slab temperatures for a 0.1-ft² cold-leg break in a PWR.

The mass flux (G) in this transition region is the minimum of either the result from the Henry-Fauske model or the homogeneous equilibrium mass flux (G_{HEM}); modified by the square root of the quotient of transition quality and actual quality X (Figure IV-3). As indicated, results from the HEM are used in the saturated region above transition quality.

Fig. IV-3 Critical mass flux vs. quality.

2.2.2 Comparison With Moody, Sonic, and Henry-Fauske Models. A series of LOCA blowdown simulations, using an appropriately modified version of RELAP4, was made to compare results between the Henry-Fauske/HEM,

Moody model, Sonic model, and Henry-Fauske model for critical discharge flow. A simplified 14-volume model of a PWR system, shown in Figure IV-4, was used for this study. Three system variables were selected to illustrate differences between the various critical flow models (see Figure IV-4): (a) the pump-side break (Junction 15) flow rate; (b) the vessel-side break (Junction 16) flow rate; and (c) the upper plenum (Volume 3) pressure.

Plots of these variables, to essentially complete system depressurization and with an expanded time scale for the first two seconds of transient, are presented in Figures IV-5 through IV-10. Data in Figures IV-5 through IV-8 indicate that the predicted break flow using the Henry-Fauske/HEM is less than that observed with the other three critical discharge models during the initial portion of the transient and greater during the latter part of blowdown. This difference results in a reduced rate of system depressurization as represented by the upper plenum pressure trace in Figures IV-9 and IV-10.

Flow in the nominal transition regime ($0.00 < \text{Quality} < 0.02$) is well illustrated in Figures IV-7 and IV-8. Results from the Henry-Fauske/HEM and the Henry-Fauske model overlaid exactly until the fluid saturated. Almost immediately the Henry-Fauske/HEM flow dropped below that for the Henry model as source quality forced the former model onto the modified HEM critical-flow data curve (see Figures IV-8 and IV-3). Transition was completed (actual quality = transition quality) at about one second into the transient as shown in Figure IV-8 and confirmed by computer code data. From that point HEM results prevailed as indicated in Figure IV-3.

All of these graphs were based upon calculations for which the break junction discharge coefficient was unity. Figures IV-11 through IV-16 present a comparison between the same system variables (as seen in Figures IV-5 through IV-10) but with results for the

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Fig. IV-4 Simplified 14-volume model of PWR system.

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Fig. IV-5 Pump-side break flow rate.

Fig. IV-6 Pump-side break flow rate (expanded time scale).

Fig. IV-7 Vessel-side break flow rate.

Fig. IV-8 Vessel-side break flow rate (expanded time scale).

Fig. IV-9 Upper plenum pressure.

Fig. IV-10 Upper plenum pressure (expanded time scale).

Fig. IV-11 Pump-side break flow rate showing discharge coefficient comparison.

Fig. IV-12 Pump-side break flow rate showing discharge coefficient comparison (expanded time scale).

Fig. IV-13 Vessel-side break flow rate showing discharge coefficient comparison.

Fig. IV-14 Vessel-side break flow rate showing discharge coefficient comparison (expanded time scale).

Fig. IV-15 Upper plenum pressure showing discharge coefficient comparison.

Fig. IV-16 Upper plenum pressure showing discharge coefficient comparison (expanded time scale).

Moody model now based upon two different discharge coefficients, 1.0 and 0.6, while the Henry-Fauske/HEM retains its unity discharge coefficient. All of these plots indicate that Henry-Fauske/HEM results, with unity discharge coefficient, fall about midway between Moody model results with the two different discharge coefficients. Since a discharge coefficient of 0.6 has been recommended for use with the Moody critical flow model^[IV-4], a higher initial break flow rate and subsequent faster system depressurization will be characteristic of the Henry-Fauske/HEM when compared to the Moody model under these conditions.

2.3 Downcomer Penetration Model

S. R. Fischer

A model to predict emergency core cooling (ECC) liquid penetration of a PWR downcomer during periods of reverse core vapor flow was developed and integrated into the RELAP4/MOD5 code. The model, based on recent work by Battelle Columbus^[IV-5], computes the component mass flows at the downcomer-lower plenum junction based on the liquid flow into the upper annulus, the liquid subcooling, and the vapor flow from the core into the lower plenum.

The Battelle Columbus downcomer penetration data were correlated using a linear least squares technique which yielded the following correlation:

$$J_{\ell \text{JDLP}}^* = .36266 + 1.134 J_{\ell \text{IN}}^* - 1.1625 J_{\text{G CORE}}^* (1 - .0045 \Delta T_{\text{SUB}}) \quad (\text{IV-1})$$

where

$$J_{\ell \text{JDLP}}^* = \left(\frac{J_{\ell \text{JDLP}}^2 \rho_{\ell}}{g D_h \Delta \rho} \right)^{1/2}$$

$$J_{\ell \text{IN}}^* = \left(\frac{J_{\ell \text{IN}}^2 \rho_{\ell \text{IN}}}{g D_h \Delta \rho} \right)^{1/2}$$

$$J_{\text{G CORE}}^* = \left(\frac{J_{\text{G}}^2 \rho_{\text{g}}}{g D_h \Delta \rho} \right)^{1/2}$$

$$D_h = \text{hydraulic diameter (ft)}$$

$$\Delta T_{\text{SUB}} = T_{\text{LIQ}} (\text{lower plenum}) - T_{\text{LIQ}} (\text{cold leg}) \geq 0$$

$$J_{\ell \text{JDLP}} = \text{volumetric flux of liquid at junction of downcomer and lower plenum based on annulus area}$$

J_G	=	volumetric flux of vapor at junction of core and lower plenum based on annulus area
J_{lIN}	=	volumetric flux of liquid from cold leg into upper annulus based on annulus area
ρ_l	=	liquid density at specified junction (lb/ft ³)
g	=	gravity (32.17 ft/sec ²)
ρ_g	=	vapor density at specified junction (lb/ft ³)
$\Delta\rho$	=	$\rho_l - \rho_g$

The correlation allows one to compute the volumetric flux of water from the downcomer into the lower plenum. To compute the component mass flow rates the void fraction at the downcomer-lower plenum junction must be known. No experimental information on the void fraction or vapor volumetric flux at the downcomer-lower plenum junction is available from the Battelle data.

The correlation was integrated into RELAP4/MOD5 with the following assumptions:

- (1) The correlation is used to compute the slip flow conditions at the downcomer-lower plenum junction only after ECC flow has been initiated.
- (2) The downcomer penetration model is used prior to the lower plenum entrainment model; that is, the lower plenum entrainment model overrides any calculations made by the downcomer penetration model.
- (3) The void fraction $\bar{\alpha}$ at the downcomer-lower plenum junction is computed by averaging volume void fractions on either side of the junction.
- (4) The net junction flow rate remains unchanged from the value computed from the simultaneous solution of the conservation equations.
- (5) The model remains consistent with the vertical slip model in that

$$v_{SLIP} = v_l - v_g = \frac{J_{lJDLP}}{1 - \alpha} - \frac{J_{gJDLP}}{\alpha} .$$

J_{lJDLP} is computed from Equation (IV-1) and

$$J_{gJDLP} = \frac{G_{NETJDLP} - \rho_l J_{lJDLP}}{\rho_g}$$

Proper implementation of the downcomer penetration model requires the use of the simple PWR downcomer system shown in Figure IV-17. Single junctions must be specified at the cold leg and upper annulus, between the downcomer and lower plenum, and between the core and lower plenum.

Using a RELAP4 model of the Battelle Glass Vessel Test Apparatus, several series of runs were made to check out the operation of the downcomer penetration model. RELAP predictions with and without the penetration model were made. The model was found to perform satisfactorily, giving reasonable results in good agreement with the Battelle experimental data.

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Fig. IV-17 Simplified RELAP model of PWR downcomer for use with downcomer penetration model.

3. CONTAINMENT ANALYSIS DEVELOPMENT

The INEL effort directed toward development of a best-estimate containment program commenced in July 1975. The general program objectives were to participate in the Marviken Phase II containment experimental program, to gain familiarity and experience with the KACHINA^[IV-6] computer program developed by Los Alamos Scientific Laboratory (LASL), and to initiate development of a Best-Estimate Advanced Containment computer program (BEACON) based on the physics and numerical methods of KACHINA. INEL had not used KACHINA previously, and the effort was not completely defined until sufficient experience with the code was acquired to allow knowledgeable decisions on the recommended development effort. Tasks have been defined, and the following discusses tasks completed and the progress of major tasks undertaken.

3.1 Installation of KACHINA at INEL

The KACHINA computer program was installed on the Berkeley CDC-7600 computer used by INEL. Certain computer system dependent subroutines required minor code changes to either achieve use of the subroutine function or to bypass the particular subroutine. The plotting routines required significant changes before plots were obtained. The code was successfully installed and numerous sample problems have been evaluated, including use of the standard plot features.

3.2 Program Development Recommendations

Existing containment codes are multi-volume, multi-junction codes that represent each volume using lumped parameter techniques. Therefore, the codes cannot discriminate beyond "average" conditions in a volume. These codes are also considered conservative; however, normally the degree of conservatism is not known.

One project task was to review containment phenomena and recommend what level of analytical sophistication should be provided in a best-estimate containment code. There is a need for a containment program which will satisfy at least the following three requirements: (a) a check on the accuracy and conservatism of lumped parameter codes; (b) provision for more detailed information on pressure, temperature, and mass distribution which is required for structural calculations; and (c) modeling of the phenomena in the containment that cannot be analyzed with lumped parameter codes. Such phenomena include BWR dynamics problems, the detailed thermal-hydraulic interaction of steam, air, water, and ice in an ice condenser compartment, mass transfer between water phases in the jet of hot water and steam escaping from the primary system during a LOCA, mass transfer between water phases when subcooled water is sprayed into hot vapor mixtures, liquid de-entrainment, and all multidimensional geometry effects.

A study was performed of the important phenomena that occur in containment systems during a LOCA or an operational transient, particularly the phenomena that require more detailed modeling capability. Development of a modular code was recommended that would have multidimensional and multicompartment modeling capability. The code would contain a library of modules and subroutines which are required to model the complex thermal-hydraulic phenomena that occur in a containment system.

The study recommended developing the code in several orderly stages, beginning with an existing two-dimensional, two-phase, two-component code such as KACHINA. Modules and subroutines should be added to develop a true general purpose containment code that can conveniently analyze most, if not all, existing containment problems and yet be flexible enough to be applied to new containment concepts when they are developed.

An integral part of the development plan is confirmation of the analytical models with experimental data. Early comparison of models and data should be performed to assure reality and accuracy in the analytical models. At a later stage, an independent detailed program verification effort should be undertaken to assure that the program is applicable for use in reactor safety evaluation.

3.3 KACHINA Familiarization

KACHINA was selected to serve as the numerical thermal-hydraulic basis for BEACON development. The code was installed on the INEL computer system, and evaluations of the code theory and sample problems were completed.

The physical problem addressed by KACHINA is the transient flow of a two-phase, two-component mixture. Each phase is treated as a field consisting of a large number of particles. The particles in each field can interpenetrate the other field similar to dispersion without colliding.

The evaluation of KACHINA uncovered a number of deficiencies in the code which will have to be removed before KACHINA may be applied to general containment thermal-hydraulics. Code programming standards and general applicability features must also be improved to incorporate KACHINA into a system program. The technical problems in employing KACHINA can be grouped in three categories: (a) generic problems related to the finite difference solution of transient non-linear partial differential equations, such as the convergence problems in iterating the pressure and the vapor density, numerical diffusion, and the range of void fraction; (b) assumptions made in formulating the equations in KACHINA, such as the energy equations; and (c) additional physical correlations that will be required to model the containment problems. The latter category includes wall and interphase friction, mass and heat transfer between phases, and bubble and droplet growth.

While the problems with KACHINA are real, the code is considered to be a theoretical state-of-the-art program. Assumptions and compromises had to be made in order to produce a code. The code was not developed for containment thermal-hydraulics, and some of the difficulties encountered are the result of applying it to different problems. Finally, the governing continuum equations that describe the two-phase, two-component thermal-hydraulics are transient nonlinear partial differential equations. The construction, existence, and stability of solutions of such systems are not well understood and are the subject of current mathematical research.

3.4 Program Structure and Improvements

The LASL version of KACHINA has been modified to serve as a basis for the development of BEACON. The following four non-analytical program changes have been completed:

- (1) Incorporation of the INEL variable format input processing subroutines (INP).
- (2) Modifications to allow a large variety of mixed input units. All code calculations and output are in SI units.

- (3) Partial restructuring of the KACHINA computational code to facilitate future development work.
- (4) Incorporation of the INEL plotting capabilities.

The modified KACHINA program is identified as the KACHINA multi-field fluid computer program, INEL version 2, to differentiate this program from the LASL-developed KACHINA program initially installed at INEL, and from subsequent versions of BEACON.

3.4.1 INP and Variable Units Input Module. The INEL version 2 KACHINA input module offers advantages over the original KACHINA input method including:

- (1) Free-field format input
- (2) Card number to identify data sets
- (3) Arbitrary ordering of data cards
- (4) Use of comment cards and comments on data card
- (5) Data input error detection and message output
- (6) Modification or replacement card input capability
- (7) Capability to run successive cases with minimal input
- (8) Inputting of physical variables in various mixed units
- (9) Optional means of inputting and defining the KACHINA mesh fluid properties.

3.4.2 Optional Method of Specifying Fluid Variables. An optional means of specifying KACHINA cell region fluid properties and state conditions is also provided by the KACHINA, INEL version 2. When this option is selected, it is assumed that the fluid components are air, steam (water in the vapor state), liquid (water in the liquid state), or a mixture of any of these. The KACHINA fluid variables are then set internally to values representative of the above fluids. Input is also simplified by the use of an alphanumeric word to define the type of fluid in a cell region. The normal KACHINA input of partial densities for the two-vapor and two-droplet components is then computed internally according to the fluid specification. Another feature of the input option is the capability to input state-point variables other than density and internal energy to define the cell region initial thermodynamic state conditions.

Presently, ideal-fluid equations of state are used to compute the KACHINA partial densities and material energies input from the fluid specification input and various pairs of state-point variable input. Allowable state-point pair input variables are:

- (1) Pressure-temperature
- (2) Pressure-internal energy
- (3) Pressure-enthalpy
- (4) Density-temperature
- (5) Density-internal energy
- (6) Density-enthalpy

3.4.3 KACHINA Code Restructure. A number of programming changes were made to the LASL KACHINA code without changing the basic fluid dynamics computational technique. These changes were made for a number of reasons:

- (1) To incorporate the INP input and variable units capability
- (2) To facilitate future analytical model changes and additions to the program
- (3) To reduce general programming costs (e.g., compilation time, checkout, errors, etc.).

The FORTRAN-implemented overlay used in the LASL KACHINA program has been eliminated, and segmented overlay is used instead. This allows the flexibility of defining and changing the overlay structure with segmentation directive cards, without having to modify the FORTRAN code. The major subroutines were subdivided into several small and logical routines.

These changes have been made without disturbing the basic fluid dynamic computations. Test cases show numerical results to be identical to the LASL KACHINA program. Execution times have not changed significantly. Core memory required is increased but may be reduced by a different overlay arrangement, which can now be implemented by segmentation control card directives.

The restructuring task does not represent the final modular structure of BEACON, which will evolve gradually as code development progresses.

3.5 Cartesian Geometry

One of the project tasks was to develop a KACHINA module capable of solving two-dimensional cartesian geometry problems. Significant progress had been achieved on this task by the end of December 1975. Successful sample problems have been obtained.

3.6 Obstacle and Source Cells

KACHINA has very limited geometry modeling capability, and one of the prime tasks was to add non-fluid obstacle cells. Several sample calculations were subsequently completed.

A second task of similar status and progression is the assignment to add a source cell model. Reactor coolant system blowdown is normally modeled in containment codes as a time-dependent table of mass and energy additions. BEACON is being developed to allow time-dependent additions of mass, energy, and momentum in any computational cell or reasonable group of cells. Either air or water, single- or two-phase, may be added to the cell. This feature may also be used to model a jet flow source to the computational mesh.

3.7 Flow Models

Work has commenced to extend the flow models in KACHINA to enable calculation of single-phase fluid flow. A LASL equation of state for water was added to a test version of KACHINA, and other minor modifications were made to the code.

4. FUEL ROD MODEL DEVELOPMENT

4.1 FRAP-S and FRAP-T Development

4.1.1 FRAP-S. Subroutines were added for predicting the time-dependent creep deformation of fuel rod cladding, and a model to predict axial cladding growth due to irradiation was incorporated in the FRAP-S code. The optional plotting package was expanded to generate plots of a number of new variables. Code input routines were modified so as to accept all input data in SI units. Work is underway to convert all output quantities to SI units also.

4.1.2 FRAP-T. The FRAP-T code has been converted to run on the CDC-7600 computer system. Besides giving additional versatility, the conversion will aid in FRAP-T development since the CDC system provides more comprehensive checks of the program logic and also flags all undefined variables.

A timing subroutine was developed and incorporated into FRAP-T that maintains a running account of time spent in each of the subroutines of FRAP-T. Through the use of the timing subroutine, several redundant calculations were identified and corrected, resulting in a substantial decrease in execution time. Also, by analyzing the time subroutine results, an error in the plenum temperature calculation was found and corrected.

Several new models were added to FRAP-T. An interim model which differentiates between radial displacement of the fuel pellet at the end and at the center (waist) of the pellet was added to permit a more accurate assessment of the onset of fuel-cladding interaction. The effect of the pellet dish volume on the fuel rod internal pressure calculations was modeled.

As a user-oriented convenience, the input routine was modified to perform a check on the input radial and axial power profile data and to correct automatically any errors which are found.

4.2 Mechanical Response Models

4.2.1 Local Strain Studies. A review of existing finite element structural codes was made to determine their suitability for calculation of local material in fuel pellets and cladding. The MARC code was selected for use, and familiarization with the code was begun.

4.2.2 FRACAS Development. Several modifications were made to the FRACAS subcode. The routine which computes the onset of fuel-cladding interaction was revised to utilize a new iteration scheme, which resulted in a considerable decrease in execution time for power-cooling-mismatch (PCM) problems. An instability in the primary creep calculation scheme was detected and corrected.

4.3 Failure Subcode Development

Research was performed, including a review of the literature, to determine the failure modes appropriate for inclusion into a subcode that will predict fuel rod failure. The logic of the subcode and the model interaction was defined.

4.4 Thermal Conduction Models

Programming of the two-dimensional heat conduction equations into a format suitable for inclusion in the FRAP codes was completed and checkout is underway.

5. LOCA ANALYSIS VERIFICATION

The model verification project description was modified and updated to reflect recent changes in code development and progress in experimental programs and verification efforts. The effort to assess uncertainty of the RELAP4 code has emphasized definition of parameters to be varied in plant analysis and assessment of uncertainties in these parameters.

Sensitivity studies and data comparisons were performed using experiments selected from the Semiscale program to examine slip model correlation, nodalization, and time step control. The selected Semiscale experiments provide a wide variety of thermal-hydraulic responses during the simulated blowdown and ECC injection phases of the tests.

Methods for determining the uncertainty of output from codes such as RELAP4 are being explored to determine which of several candidate methods offers the most cost-effective approach in conducting code uncertainty analyses. A simplified RELAP4 model of the Trojan PWR has been developed, and uncertainties are being propagated through this model to test three candidate statistical techniques – Monte Carlo, Monte Carlo with importance sampling, and response surface.

6. FUEL BEHAVIOR VERIFICATION

Fuel behavior verification is directed toward providing a detailed understanding of the behavior of the light water reactor (LWR) nuclear fuel assemblies during hypothetical off-normal or accident conditions. This project centers upon the verification of analytical models to describe nuclear fuel behavior under diverse conditions. Extensive out-of-pile and in-pile experimentation is required to provide data necessary for this effort, which is accomplished through the efforts of three programs: (a) the Thermal Fuels Behavior Program which conducts the detailed test planning, design, and execution of small cluster and individual fuel rod tests in PBF; (b) the LOFT program, which in conjunction with its integral LWR system tests is involved in the detailed planning, design, and execution of large cluster fuel rod tests in the LOFT facility; and (c) the Reactor Behavior Program, which conducts Halden fuel rod irradiations and postirradiation examination of commercial power reactor fuel. The actual fuel behavior verification efforts encompass evaluating test results relative to defined requirements, providing technical assistance to the Fuel Behavior Branch of the Nuclear Regulatory Commission in the development and coordination of the comprehensive fuel behavior program, and verifying analytical fuel behavior models by comparison of calculations with appropriate experimental data.

Development and Coordination of Fuel Behavior Program

The technical approaches being taken to achieve a detailed understanding of LWR fuel assembly response during off-normal or accident conditions were updated to reflect current experiment requirements. The tasks range from computer code development to integral

in-pile accident simulations. Work was performed in revising requirements relative to PCM, irradiation effects, and LOFT fuel behavior programs. A review of PCM experiment requirements and an analysis of LWR fuel behavior during a PCM each indicate the importance of the maximum stabilized cladding temperature in the postcritical heat flux regime. Section 6.1 presents a portion of the analysis. A series of tests are proposed for PBF in order to study the effects of irradiation and reactor service on fuel rod integrity under PCM conditions. Revised requirements for the irradiation effects experiments eliminate the simulation of cladding irradiation damage as a program objective and incorporate newly available fuel rod materials into the test plan. The study of separate irradiation effects using rods fabricated with fresh fuel (with and without fission product species) and irradiated cladding was retained in the program.

Experimental program analysis for the LOFT facility was performed to help verify the ability to extrapolate fuel behavior test predictions from small test reactors to large power reactors. Fuel rod and subchannel coolant behavior during a LOCA was investigated. One aspect of this investigation, flow redistribution in partially blocked subchannels, is reported in Section 6.6.

A bilateral information exchange was initiated between Japan and the United States, particularly in the area of reactivity-initiated accident (RIA) research. Tentative plans for an integration of the RIA test programs at PBF and at Japan's Nuclear Safety Research Reactor (NSRR) were formulated, and an exchange of engineering personnel was planned. The NSRR is now conducting RIA experiments from cold startup reactor conditions. Planned RIA tests in PBF initially emphasize hot standby reactor conditions.

Halden Program

The IFA-429 instrumented Halden test assembly is designed to study helium sorption and transient fission gas release from PWR type fuel rods. Irradiation was begun in June 1975 and analysis of data from the first three months of operation was performed in preparation for assessing initial IFA-429 test results. Internal rod pressure decreases of up to 5% were observed during this period. Slight fission gas release was observed during an overpower transient at 2,600 MWd/t.

Design and instrument fabrication of test assembly IFA-430 were performed. The IFA-430 test assembly instrumentation measures the resistance within a fuel rod to internal gas flow and the effect on fuel-to-cladding gap conductance of gas composition. Thermal-hydraulic calculations were performed to predict the resistance within the fuel rod to pressure pulses. Multisensor ultrasonic thermometers were developed for the centerline temperature measurements, and a fast response bellows-LVDT type pressure transducer was designed. Also developed were an analytical correction scheme for the irradiation-induced decalibration of tungsten-rhenium thermocouples and a correction for the decalibration of vanadium neutron detectors due to burnup.

IFA-226 and IFA-239 test assemblies were removed from the Halden reactor after achieving peak burnup in excess of 40,000 MWd/t, and data from those assemblies were reduced.

Correlations for Materials Properties

Models were generated of LWR fuel rod properties; in particular, models were generated or improved for fuel restructuring (Section 6.2), zircaloy oxidation (Section 6.3), hydrogen uptake in zircaloy (Section 6.4), and the effects of irradiation and cold work on zircaloy mechanical properties (Section 6.5). A consideration of fuel restructuring is necessary to refine fuel temperature calculations, and the zircaloy cladding modeling tasks are required due to the importance of cladding embrittlement and strength changes with respect to in-pile behavior.

6.1 Analysis of LWR Post-CHF Fuel Behavior During a PCM Accident

J. M. Broughton

6.1.1 Introduction and Summary of Results. An analysis using the transient fuel rod analysis code, FRAP-T, was made of post-CHF (critical heat flux) LWR fuel behavior during postulated power-cooling-mismatch (PCM) conditions. The analysis consisted of a parametric study of a variety of postulated PCM accidents to identify expected modes of post-CHF fuel rod behavior and a study to determine the expected effects of variations in the basic fuel rod design and burnup parameters.

A variety of PCM conditions were created by choosing both a moderate power (16 kW/ft) and a high power (23 kW/ft), and varying the coolant mass flux to obtain a wide range of post-CHF film boiling heat transfer coefficients. The effects of burnup were simulated only to the extent of increasing the cold internal pressure from 375 to 700 psia. The PCM conditions were induced by a linear decrease in coolant flow while maintaining a constant power or a linear increase in rod power while maintaining a constant coolant flow. A wide range of coolant flow rates were examined for both sets of PCM conditions.

The following conclusions were drawn from this analysis:

- (1) Four categories of post-CHF fuel behavior have been identified and are indicated in Table IV-I. Cladding ductility, zirconium-water reaction, fuel melting, and cladding deformation and fuel rod failure modes are the principal phenomena that characterize post-CHF fuel behavior within these categories. (Fuel and cladding temperature, fuel rod internal pressure, and cladding material properties are the controlling physical parameters.)
- (2) For similar cladding temperature transients, high power PCMs result in substantially higher fuel temperatures and consequently more fuel melting than do moderate power PCMs for approximately the same cladding temperatures.
- (3) Four principal modes of post-CHF fuel rod failure have been identified by the analysis: (a) cladding melting; (b) zircaloy oxidation and oxygen embrittlement leading to brittle fracture; (c) cladding ballooning to rupture; and (d) cladding strain to

TABLE IV-I

CATEGORIES OF FUEL BEHAVIOR DURING PCM TRANSIENTS

Category	Phase	Stabilized Cladding Temperature (°F)	Reaction	Ductility	Failure Modes
4	Stabilize in β	>1,800	Severe	High-Low	Cladding and possibly fuel melting; cladding oxidation; deforma- tion to rupture followed by fuel melt[a].
3	Stabilize in α - β transition	1,550 to 1,800[b]	Minimal	Low	Deformation to rupture followed by fuel melt[a].
2	Stabilize in high temp α	1,000 to 1,550	None	High	Deformation to rupture followed by fuel melt[a].
1	Stabilize in low temp α	<1,000	None	Normal	FCMI induced cladding rupture.

[a] Cladding deformation is possible only after sufficient gaseous fission product accumulation that will increase internal rod pressures during the transient above the system pressure. A cold internal pressure of 700 psia is sufficient.

[b] This temperature range is intended to bound the cladding ductility minimum that occurs during the α - β phase transition.

rupture due to excessive fuel volumetric increase during fuel melting (if the cladding has been sufficiently embrittled).

- (4) The influence of variations in the basic fuel rod design parameters investigated with regard to beginning-of-life post-CHF behavior of a PWR fuel rod is relatively small and less significant than the influence of uncertainty in the film boiling heat transfer coefficient. Changes in the design variables investigated do not influence the phenomenological processes controlling post-CHF fuel rod behavior as presently modeled by the version of the FRAP-T computer code used in these analyses.
- (5) Highly enriched fuel rods required for PBF cluster tests have a significantly reduced thermal time response and decreased fuel centerline temperatures relative to nominally enriched (3 to 20%) fuel rods. (Data reduction and interpretation of individual PBF test rods could be significantly influenced by the effects of enrichment.)
- (6) For end-of-life conditions, cladding deformation (ballooning) during PCM transients is deterred by significantly reducing the as-designed diametral gap (from 8 to 2 mils in the case considered).

6.1.2 Analytical Model. The analysis was performed with FRAP-T, Mod 0002 Exp 004 and MATPRO, version 005.

The difference between coolant flow behavior within a rod bundle and that within an annular geometry dictate that a careful choice of appropriate CHF and film boiling correlations be made in order to obtain an optimum analysis for each geometry. The B&W-2 CHF correlation^[IV-7] and the transition and film boiling correlation which was developed by K. Condie and S. Bengston were chosen for analysis of the rod bundle geometry. The nonuniform axial flux profile correction factor was used with the B&W-2 CHF correlation.

The requirement for single rod test with a flow shroud presents a special problem with respect to the proper selection of CHF and post-CHF heat transfer correlations. Both the CHF and the post-CHF heat transfer coefficient are dependent upon the local void fraction. It has been shown that if one of the walls is not producing steam, and the other wall is, the liquid will concentrate at the cold wall and effectively reduce the quantity of liquid interacting with the hot wall. The W-3 CHF correlation^[IV-8], including both the nonuniform axial flux factor and the cold wall correction factor, and the Groeneveld 5.7 film boiling correlation^[IV-9] were used in the analysis of the single rod PWR PBF test geometries. The Groeneveld 5.7 correlation was developed with data obtained only from an annular geometry.

6.1.3 Predicted Post-CHF Behavior of a 12-foot PWR Fuel Rod During Various PCM Transients. The predicted post-CHF behavior of a 12-foot PWR fuel rod within a cluster geometry of nominal Westinghouse 15 x 15 design subjected to a variety of PCMs is presented. The Westinghouse 15 x 15 design was chosen for the base case because of (a) the operating plants with this design and (b) the fact that this design is subjected to higher linear peak power than the smaller diameter fuel rods projected for use in commercial PWRs.

(1) Coolant Flow Reduction Induced PCM (Moderate Power Level). The predicted post-CHF response of a 12-foot PWR fuel rod to a wide range of flow-reduction-induced PCMs is shown in Figure IV-18. The minimum coolant mass flux during the transient ranged from 3.0×10^5 to 7.0×10^5 lbm/hr-ft². Cold internal fuel rod pressure was nominally 375 psia which is intended to simulate beginning-of-life pressures. The linear power was constant at 16.0 kW/ft.

Fig. IV-18. Peak cladding and peak fuel centerline temperatures versus time after CHF for a 12-foot PWR fuel rod subjected to a variety of flow reduction (moderate power) PCMs.

Depending upon the degree of flow reduction, the post-CHF thermal response of the cladding ranged from very severe (when flow was reduced to 3.0×10^5 lbm/hr-ft²) to essentially no deviation from normal operation (when flow was reduced to 7.0×10^5 lbm/hr-ft²). Cladding collapse was predicted when the cladding surface temperature reached approximately 1,300°F. Fuel temperatures stabilized at or below the fuel melting point.

(2) Comparison of Flow Reduction Versus Power Ramp Induced PCMs. A comparison of predicted category 4 post-CHF fuel behavior between a flow reduction PCM and a power ramp induced PCM is shown in Figure IV-19 for a maximum linear peak power of 16.0 kW/ft.

As shown in Figure IV-19, the points in time at which CHF occurs, and at which the cladding temperature begins to rise rapidly, are offset. If the times of CHF were to coincide, the cladding temperature response would be quite similar. The same observation is true for the post-CHF fuel centerline temperature behavior. However, the pre-CHF fuel centerline temperature response is different, primarily because one fuel rod (dashed line in Figure IV-19) is experiencing a power ramp to 16.0 kW/ft, which causes the fuel temperature to increase with power.

A comparison similar to that shown in Figure IV-19 was also made between a moderate power (16.0 kW/ft) flow reduction PCM and a high power (23.0 kW/ft) ramp induced PCM. Similar behavior is observed except that stabilized fuel temperatures for the high power (23 kW/ft) case are approximately 800°F higher than those for the moderate power (16 kW/ft) case. The higher fuel temperatures predicted in the high power case result in substantial fuel melting at the pellet core, which could in turn induce tangential forces in the cladding from the increased volume of molten UO₂. Similar results were observed when the high and moderate power PCM transient predictions for categories 2 and 3 were compared.

Fig. IV-19 Comparison of category 4 post-CHF fuel rod behavior during a flow reduction and a power ramp PCM.

The tangential forces on the cladding that could result from the phase change expansion of UO_2 fuel during melting could have significant influence on the net effect of FCMI if cladding ductility has been sufficiently reduced prior to fuel melting. Approximately 40 to 50% of the fuel pellet at the pellet core is calculated to melt over a 3-foot length of the fuel rod. If the cladding cannot strain to accommodate the increased fuel volume, then cladding rupture will occur and molten fuel may be expelled out into the coolant. At the predicted high cladding temperatures, zircaloy oxidation and oxygen embrittlement will be extensive if time at temperature is sufficient.

(3) High Internal Pressure (End-of-Life) Post-CHF Fuel Behavior. The analysis of end-of-life PWR fuel rod behavior during a PCM accident is restricted to an evaluation of the effects of increased internal gas pressure because the version of the FRAP-T computer code that was employed does not model other effects of irradiation exposure. A cold internal pressure of 700 psia corresponds to a steady state operating pressure ranging from 1,800 to 2,100 psia (depending primarily upon the axial power profile and linear power rating).

The predicted fuel and cladding temperatures and cladding circumferential strain response during a category 4 reduced flow (moderate power) PCM are shown in Figure IV-20. Both the peak cladding temperature (dashed line) and the cladding temperature at the rupture location (solid line) are plotted. Similar plots of cladding strain history are shown in the bottom graph. The predicted cladding rupture strain is 68% and the resulting increased resistance to heat transfer across the gap induces a significant deviation in both the fuel and the cladding thermal behavior at the rupture location. Cladding temperatures are rapidly cooled to the coolant temperature because of the decreased heat transfer across the gas gap, whereas the fuel temperatures rapidly increase. Complete pellet melt is predicted at the cladding rupture location approximately 13 seconds after rupture.

6.1.4 Effect of Variations in the Basic Fuel Rod Design Parameters. The effect of parametric variations in the basic PWR fuel rod design parameters on post-CHF fuel behavior during both categories 4 and 2 PCM transients has been analyzed. The analysis is applicable to either a coolant flow reduction (moderate power) or a power ramp (high power) induced PCM. The ranges of the design parameters selected were intended to cover possible values that might typically be considered for the design of a large diameter PWR fuel rod. Both low pressure (cold internal pressure of 375 psia) and high pressure (cold internal pressure of 700 psia) fuel rods were analyzed.

Variations in the design parameters over the range investigated were found to have less influence on the post-CHF fuel rod thermal behavior than the uncertainty in the post-CHF film boiling heat transfer coefficient. However, two design parameters, cladding wall thickness and fuel cladding gap, were found to significantly influence cladding deformation behavior at high fuel rod internal pressures. A reduction in cladding wall thickness from 24 to 18 mils was found to be sufficient to induce cladding deformation leading to rupture under alpha-phase conditions. A small initial diametral fuel-to-cladding gap (2 mils) was found to prevent deformation to rupture, because the gap closure early in the transient forces all the fill gas into the relatively large and cool upper plenum.

$G_f = 0.3 \times 10^6 \text{ lb}_m/\text{hr} - \text{ft}^2$, Final Value of Flow after Flow Reduction
 Power = 16 kW/ft
 Pressure = 700 psia

Fig. IV-20 Fuel and cladding temperature and cladding circumferential strain response during a category 1 reduced flow (moderate power) PCM for a 12-foot PWR fuel rod.

6.2 A Model for Reactor Fuel Restructuring

C. S. Olsen

During irradiation of UO_2 and $\text{UO}_2\text{-PuO}_2$ fuels, equiaxed grain growth occurs near the outer periphery of the fuel, columnar grains form near the center of the fuel, and a central hole develops from pore migration in the columnar grain growth region^[IV-10]. The

as-sintered structure remains at the periphery of the fuel where temperatures are lower than those necessary for noticeable equiaxed grain growth to occur. A model was developed to calculate equiaxed grain growth as a function of temperature, initial grain size, and time, and also to calculate the formation of the columnar grain zone as a function of time.

The restructuring model consists of the following equations. Equiaxed grain growth is given by

$$D^4 - D_o^4 = 6.18 \times 10^{13} t \exp (-92,400/RT) \quad (IV-2)$$

where

D = grain size (microns)

D_o = initial grain size (microns)

R = 1.987 cal/mole-K

T = temperature (kelvin)

t = time (hours)

The columnar grain formation is characterized by a threshold temperature FTCRIT so that if the fuel temperature at a particular location exceeds the threshold temperature, columnar grains with a density of 98% total density (TD) are assumed to form instantaneously at that point.

FTCRIT is defined by the equation

$$1/FTCRIT = 3.435 \times 10^{-5} \log_{10} t + 4.69 \times 10^{-4} \quad (IV-3)$$

where

t = time (hours)

FTCRIT = threshold temperature (kelvin).

Discussions of equiaxed grain growth and columnar grain formation are followed by presentation of a restructuring model representing UO_2 and UO_2 - PuO_2 equiaxed grain growth and columnar grain formation.

6.2.1 UO_2 and (UPu) O_2 Equiaxed Grain Growth. The driving force for equiaxed grain growth is the decrease in surface free energy brought about by the decrease in the number of grains and, correspondingly, the total surface area.

Kingery and Francois^[IV-11] developed a model that considers the retarding effect of pores located on grain boundaries or more commonly at grain intersections. Nichols^[IV-12] extended the analysis of Kingery and Francois and showed that the results implicitly assumed a vapor transport mechanism with the pressure in the pore remaining equal to the equilibrium pressure $2\gamma/r$, where γ is the surface tension and r is the pore radius.

Nichols showed that for a constant pore pressure which is not a function of pore radius equiaxed grain grown by the vapor transport mechanism is expressed by

$$D^4 - D_0^4 = kt e^{-Q/RT} \quad (IV-4)$$

Equation (IV-4) was evaluated using experimental data (from References IV-13, IV-14, IV-15, and IV-16) for UO_2 grain growth to determine the constants k and Q . The average value of Q was 92,400 cal/mole and the resultant expression is

$$D^4 - D_0^4 = 6.18 \times 10^{13} t \exp (-92,400/RT) \quad (IV-5)$$

Equation (IV-5) results in a standard deviation from its data base of 4.8 μm . The predictions of this equation are compared with the data in Figure IV-21.

6.2.2 Columnar Grain Growth. The formation of columnar grains in UO_2 or UO_2 - PuO_2 mixed oxide fuels were attributed to the migration of lenticular pores up a temperature gradient (References IV-12, IV-17, IV-18, IV-19, and IV-20). Various theoretical models (References IV-21 through IV-26) for the rates of pore migration have been proposed that depend upon a particular mechanism of mass transfer (surface diffusion, volume diffusion, or vaporization-condensation mechanisms with either constant pore pressure or pore pressure in equilibrium with the surface tension).

The relative importance of the various mechanisms of mass transport depends upon the pore size and temperature. Surface diffusion dominates at small pore sizes for all temperatures. At larger pore sizes, vapor transport or volume diffusion may dominate. The critical radius between surface diffusion and vapor transport has been evaluated by Speight^[IV-27] to be about 1 μm at 2,000 K. Bubbles with radii larger than 1 μm would migrate by vapor transport and surface diffusion would dominate at smaller radii. Volume diffusion or vapor transport with constant pore pressure also become more important at larger pore sizes, and whichever mechanism dominates depends upon the particular material.

Oldfield and Markworth^[IV-28] developed a model for the formation of columnar grains based upon pore migration by vapor transport and the formation of steps by layer nucleation or by the development of screw dislocations. This process is analogous to a zone-melting situation in which the hot surface of the bubble is "melting" (actually evaporating into a gaseous "solution") and then crystallizing upon the cool surface. Thus all growth takes place at layer edges so that eventually one might expect the vapor atoms to grow out, leaving a plane, close-packed surface in contact with the vapor. This behavior is expressed in the Bravais law that a crystal is bounded by its slowest growing surfaces. In a few cases with other materials, rapid growth was observed on apparently close-packed

Fig. IV-21 Comparison of Equation (IV-5) with experimental data for UO_2 grain growth (recommended equation for UO_2 equiaxed grain growth).

(without layer edges) planes. However, a surface may contain many screw dislocations which provide a continuous supply of layer edges at which growth can take place. This process has been amply confirmed by the phenomenon of silicon carbide growing from the vapor.

Lenticular pores contain impurities so that the smooth surface bounding the pore becomes unstable beyond a certain growth velocity or impurity concentration, and steep-walled grooves develop into which the impurities are rejected. These grooves form at the ends of the lenticular void forming a trail of spherical bubbles in the boundary between two adjacent columnar grains. This rejection of bubbles into the grain boundaries was experimentally observed (Figure IV-22) as reported by Nichols^[IV-21].

Experimental data is separated into two categories: (a) pore migration in single crystals, and (b) pore migration at columnar grain boundaries. The initial migration of pores at grain boundaries leads directly to the formation of columnar grains, after which pore migration therein involves fission gas release and fuel swelling. Although the pore migration in single crystals is not directly applicable to columnar grain formation, the results from these measurements may provide information of the migration mechanism since the same mechanism should operate for pores located in single crystals or at the grain boundaries. Very little experimental information is available on pore migration rates, so that experimentally it is very difficult to determine the important mechanisms.

Fig. IV-22 Lenticular void in ThO₂ - 5% UO₂ fuel showing injection of spherical bubbles into grain boundaries.

The results from experimental measurements of pore migration rates are summarized in Table IV-II. As a whole, the conclusions reached by various authors leave no consensus as to the mechanism of pore migration. The contributing factors to this lack of consensus are (a) different pore sizes, (b) imprecise estimates of the temperature gradients, (c) inaccurate temperature measurements, (d) interaction of grain boundaries and pores in retarding pore migration, and (e) assumption of pore pressure and size relation migration rates. However, some general conclusions can be drawn. The measurements of Williamson and Cornell^[IV-29] clearly indicate a surface diffusion mechanism when the pore sizes are less than about 0.5 μm . However, for pore sizes larger than about 1 μm , Ronchi and Sari^[IV-33] suggest a vapor transport mechanism with a constant pore pressure as the rate controlling mechanism, and the results from Michels and Poeppel^[IV-32] and Buescher and Meyer^[IV-34] do not preclude this mechanism. At very small pore sizes (25 to 104 $\text{\AA}$), volume diffusion appears to be the rate controlling mechanism rather than a mechanism that competes with vapor transport at large pore sizes.

The formation of columnar grains results from pore migration by a vapor transport mechanism and a subsequent crystal growth process where the rate controlling mechanism is the crystal growth. Conservatively low measurements of the columnar grain boundary velocity show the rates to be between 0.01 $\mu\text{m}/\text{sec}$ to 1.0 $\mu\text{m}/\text{sec}$ for temperatures between 1,900 and 2,450°C^[IV-31]. For a given fuel radius of 0.2 inch, a time between 1.4 hours and 141 hours is required for columnar grain formation, which correspond to the fastest and slowest rates of columnar grain formation. Thus the formation of columnar grains occurs quite rapidly once the threshold temperature is exceeded.

TABLE IV-II

SUMMARY OF PORE MIGRATION EXPERIMENTS

Investigation	Material Type	Temperature	Pore Size	Comments on Mechanism
Williamson and Cornell ^[IV-29]	UC ₂ single crystal	1,250-1,450°C	50-250 Å	Inferred surface diffusion mechanism.
Gulden ^[IV-30]		1,400-1,500°C	25-140 Å	Volume diffusion for $r > 37 \text{ Å}$. Unknown for $r < 37 \text{ Å}$.
Oldfield and Brown ^[IV-31]	Polycrystalline UO ₂	1,955-2,410°C	0.2-2 μm	Ledge nucleation impeded motion.
Michels and Poeppel ^[IV-32]	Polycrystalline (U, Pu) O ₂	1,585-1,850°C	1-10 μm	Size dependence undetermined but authors indicated surface diffusion with a possibility of vapor-transport.
Ronchi and Sari ^[IV-33]	Polycrystalline UG ₂ and (U, Pu) O ₂	1,600-2,600°C	not given	Vapor transport mechanism.
Buäscher and Meyer ^[IV-34]	UG ₂ single crystal	1,940-2,020°C	0.01-1.0 μm	Rates independent of pore size; authors proposed an impeded surface diffusion mechanism, but results could fit vapor transport with constant pore pressure.

Since the rates of columnar grain movement are very ill-defined, the constants in theoretical models are difficult to evaluate. However, these models do predict that a threshold temperature would exist for the formation of columnar grains and that this temperature would be a function of time, decreasing with increasing time. Christensen^[IV-19] measured this threshold temperature as a function of time and derived the following relationship

$$1/T = 3.435 \times 10^{-5} \log_{10} t + 4.69 \times 10^{-4} \quad (\text{IV-6})$$

where

T = temperature (K)

t = time (hours).

In the formation of columnar grains, most of the sintering porosity moves towards the fuel center, but some of this porosity is left in the boundaries between columnar grains. A density of 98% TD was assumed^[IV-35] for the density of the columnar grain zone, and this value is also assumed here.

The equiaxed grain growth equation is assumed to apply over the entire fuel region from the fuel surface to the radius of columnar grain formation. However, in regions of low fuel temperature, such as near the pellet surface, essentially no change in grain structure is calculated.

6.3 A Model for Cladding Oxidation During Steady State Reactor Operation

D. L. Hagrman

A description is presented of the development of an analytical expression for the weight increase of zircaloy cladding due to oxidation during typical reactor operation at temperatures of 250 to 400°C.

Cladding oxidation occurs in two stages depending on the oxide thickness and, to some extent, on the temperature of the oxide. For thin oxides the rate of oxidation is controlled by the entire oxide layer. When the oxide becomes thicker, cracking of the outer portions of the oxide layer occurs, and further oxidation is controlled by an intact inner layer. The transition between stages is described in terms of the weight of oxygen absorbed per unit surface area at transition, which is given by

$$X_{\text{TRAN}} = 123 \exp \left(\frac{-790}{T} \right) \quad (\text{IV-7})$$

where

X_{TRAN} = weight of oxygen absorbed at transition point (milligrams per square decimeter), typically 30 mg/dm² or 0.008 mil thick

T = temperature of oxide-metal interface (K).

Values of the total weight of oxygen which has been absorbed by the cladding are given by Equations (IV-8) and (IV-9) for pretransition and posttransition oxide films. For pretransition oxide films:

$$X_{\text{PRE}} = \left[19.90 \times 10^{12} A \exp\left(\frac{-15,660}{T}\right) t + X_o^3 \right]^{1/3} \quad (\text{IV-8})$$

For posttransition oxide films:

$$X_{\text{POST}} = 23.0 \times 10^8 A \exp\left(\frac{-14,400}{T}\right) t - 215 \exp\left(\frac{-1110}{T}\right) + 1.16 \times 10^{-4} \exp\left(\frac{+1260}{T}\right) X_o^3 + X_{\text{TRAN}} \quad (\text{IV-9})$$

where

X_{PRE} = total weight of oxygen which has been absorbed by the zircaloy surface when a pretransition oxide film exists (mg/dm²)

X_{POST} = total weight of oxygen which has been absorbed by the zirconium surface when the oxide film is in the posttransition state (mg/dm²)

X_o = initial weight of the cladding oxide layer expressed in mg/dm² of oxygen which has crossed the oxide surface. This term can be approximated as $X_o = 0$ for etched cladding, but it becomes important if extensive prefilming is carried out or if oxidation is carried out in several steps which occur at different temperatures or in different coolant chemistries.

t = time at temperature (days)

A = a parameter describing the enhancement of the oxidation rate of the cladding in the reactor environment. Typical reactor chemistries and ionizing flux levels result in a factor of $A = 3$ for PWRs and 10 for BWRs. However, the factor is apparently a function of temperature as discussed in Section 6.3.3.

The approach and general physical representation used to model oxidation are summarized in Section 6.3.1. Section 6.3.2 develops the basic out-of-pile model, and Section 6.3.3 generalizes the basic model to a representation for in-pile environments. The model is compared to in-pile data in Section 6.3.4, and Section 6.3.5 outlines an approach for time-dependent problems within the temperature range of 250 to 400°C.

6.3.1 Physical Representation Used in Model. It is generally agreed^[IV-36, IV-37] that oxidation of zirconium alloys by water in the temperature range from 250 to 400°C proceeds by the migration of oxygen vacancies from the oxide-metal surface through the oxide layer to the oxide coolant surface (and that accompanying migration of oxygen is in the opposite direction). The vacancies at the metal-oxide surface are generated by the large chemical affinity of zirconium for oxygen, which causes oxygen ions to be neutralized and to assume interstitial positions in the underlying metal. Although the rate of oxidation is controlled in part by vacancy migration, it should be noted that the process of transfer of oxygen from the coolant to the metal is not complete until the vacancy is annihilated by an oxygen ion at the oxide-coolant surface. Thus, the suggestion that in-pile oxidation rates are affected by the supply of oxygen ions or atoms at the surface is consistent with oxidation rate control by vacancy diffusion. The net flux of oxygen atoms into the metal is equal to the product of the vacancy flux times the probability that a vacancy will be annihilated by an oxygen atom, and that probability is affected by the supply of oxygen ions whenever there is a competing mode of annihilation of vacancies.

Detailed mechanisms for the posttransition time dependence of oxide growth and for the pre- and posttransition have not been established in the literature^[IV-38, IV-39]. Proposed mechanisms are discussed in conjunction with the models developed in Sections 6.3.2 and 6.3.3, which follow.

6.3.2 Out-of-Pile Basis for Oxidation Model. An out-of-pile model is considered in this report because well defined data^[IV-36] on oxidation as a function of time have been published for out-of-pile corrosion only. The model for pretransition oxidation is discussed, and the posttransition oxidation model is next developed. Constants for the pre- and posttransition oxidation models are evaluated by comparing the models with out-of-pile empirical relations obtained from Reference IV-37.

Principal features of the oxidation models are:

- (1) The pretransition oxidation rate is time dependent and inversely proportional to the square of the (uniform) oxide thickness.
- (2) The posttransition oxidation rate is constant and inversely proportional to the average square of the (nonuniform) thickness of an intact inner oxide layer, which varies from zero to the transition thickness. Figure IV-23 presents a schematic of the posttransition model of the oxide film.

It should be noted that the posttransition oxidation rate predicted by (2) above is not the same as the result obtained by taking the time-averaged pretransition oxidation rate from

repetitive cycles. Moreover, the difference between the predicted oxidation rates is considerably larger than the uncertainty reported in the measured rates discussed in Section 6.3.2(3) below.

(1) Pretransition Oxidation.

Oxidation of materials which form a protective oxide layer is frequently found to conform to the assumption that the rate-determining process is the diffusion of oxygen atoms across the oxide. In these metals the flux of atoms arriving at the metal, and thus the rate of oxidation, is determined by the concentration gradient across the oxide. Thus, for constant concentrations on either surface of the oxide^[IV-40]

Fig. IV-23 Schematic of posttransition oxide showing an intact rate-determining layer of varying thickness, with a cracked oxide layer at the oxide-coolant interface.

$$\frac{dy}{dt'} = \frac{k}{y} \quad (\text{IV-10})$$

where

y = oxide thickness

t' = time

k = a constant.

Integration of this equation from $y \approx 0$ at $t' = 0$ to $y = x$ at $t' = t$ yields a "parabolic" time dependence, $X = \sqrt{2kt}$, which is frequently observed experimentally.

A slight generalization of the derivation of "parabolic" oxidation produces a result consistent with the measured time-dependence of zircaloy corrosion. As discussed in Section 6.3.1 above, vacancy migration is one factor in the rate controlling mechanism. If the vacancies have a limited lifetime, the flux of vacancies arriving at the oxide-coolant surface will be proportional to the inverse of the time ($t_{\text{diffusion}}$) required for the vacancies to diffuse to the oxide fluid surface. Since the square of the average diffusion distance is proportional to the diffusion time ($y^2 \propto t_{\text{diffusion}}$), the net flux of vacancies arriving at the oxide-fluid surface should be proportional to the inverse of the square of the oxide thickness which the vacancies must cross.

From the physical arguments of the last paragraph, the vacancy lifetime-limited rate of oxide growth should be $\frac{dy}{dt} = c/y^2$ where c is not a function of time or oxide

thickness. Integration of the rate equation from $y = X_0$ at $t' = 0$ to $y = X$ at $t' = t$ gives $X = (3ct + X_0^3)^{1/3}$.

The $t^{1/3}$ dependence of the oxide layer was obtained directly from the assumption that the oxide growth is proportional to the inverse squared oxide thickness without specific reference to the vacancy-migration picture. If the concept of a temperature-dependent vacancy concentration at the metal-oxide surface is introduced, the constant c can be replaced with a temperature dependent form, $c = R \exp(-T_0/T)$, where R and T_0 are constants and T is the temperature of the oxide-metal surface.

(2) Posttransition Oxidation. The method adopted to model posttransition oxidation is to view posttransition oxidation as controlled by a series of pretransition modes which are separated by local mechanical failure and cracking of the oxide film. This method is adopted because it successfully relates pretransition and posttransition oxidation rates (out-of-pile).

If the representation of local mechanical failure is correct, the rate-controlling oxide layer should rupture at a thickness approximately equal to the transition thickness, but at a time determined by location conditions. After a few cycles, the local thickness of the intact oxide layers will be distributed at random between 0 and the transition thickness X_{TRAN} . The average rate (which is what is observed in most experiments) will be the average of the local rates as determined by the thickness of the intact layer. In terms of the pretransition oxidation model developed in (1) above

$$\left(\frac{dX}{dt} \right)_{\text{ave. over surface}} = \left(\frac{R \exp^{-T_0/T}}{X^2} \right)_{\text{ave. over surface}} = \left(\frac{R \exp^{-T_0/T}}{(X^2)_{\text{ave. over surface}}} \right) = \left(\frac{3 \cdot R \exp^{-T_0/T}}{X_{\text{TRAN}}^2} \right) \quad (\text{IV-11})$$

(3) Evaluation of R and T_0 and Comparison with Published Results. Reference IV-37 has published empirical relations based on out-of-pile data. These relations are listed below as published:

$$\text{pretransition oxidation} = (27.1 \pm 0.8) 10^3 t^{1/3} \exp \frac{-5,220}{T} \quad (\text{IV-12})$$

$$\text{posttransition oxidation} = (23.0 \pm 0.7) 10^8 t \exp \frac{-14,400}{T} \quad (\text{IV-13})$$

$$\text{weight gain at transition} = (123 \pm 4) \exp \frac{-790}{T} \quad (\text{IV-14})$$

where

T = temperature in K

t = time in days.

If these equations are identified with the more general results derived in Sections (1) and (2) above, the empirical formulas can be fit by the results derived with a single set of parameters (namely, $T_o = 15,660$ K, $R = 6.94$, and $X_{TRAN} = 123 \exp \frac{-790}{T}$) to within less than the $\pm 4\%$ error reported in Reference IV-37.

6.3.3 Generalization to an In-Pile Model. Prediction of in-pile corrosion is complicated because important variables (local temperature and reactor chemistry) are not always reported and because data on the time dependence of corrosion is limited. If in-pile corrosion enhancement is due to irradiation damage of the oxide layer, as has been suggested [IV-41, IV-42], a new mechanism must be added to the present model. On the other hand, if the observed enhancements result from an increased supply of atomic oxygen or oxygen ions, as suggested by others [IV-43], a simple change of the rate constant already in the out-of-pile rate equation will describe the increased oxidation observed in reactors. Changes in oxidation due to in-pile operation are incorporated into the present model with an enhancement factor A which describes a multiplicative in-pile enhancement of the out-of-pile oxidation rate due to an increased supply of oxygen ions.

Rate equations for in-pile oxidation are, for the pretransition regime,

$$\frac{dX_{PRE}}{dt} = \frac{AR \exp (-T_o/T)}{X^2} \quad (IV-15)$$

and for the posttransition regime

$$\frac{dX_{POST}}{dt} = \frac{3AR \exp (-T_o/T)}{X_{TRAN}^2} \quad (IV-16)$$

The integrated forms of these equations are

$$X_{PRE} = \left[3 AR t \exp (-T_o/T) + X_o^3 \right]^{1/3} \quad (IV-17)$$

and

$$X_{POST} = \frac{3 AR (t - t_{TRAN}) \exp (-T_o/T)}{X_{TRAN}^2} + X_{TRAN} \quad (IV-18)$$

A result (and a good test of the theory if time-dependent, in-reactor oxidation data become available) is the fact that the pretransition oxide film thickness at a given time is merely $A^{1/3}$ as thick as it would have been without the in-pile enhancement. Since the posttransition oxidation is linear in time, both the rate and change in oxide thickness at a particular time are enhanced by a factor A .

Estimation of corrosion enhancement is difficult in that an adequate data base for a careful prediction of oxidation enhancements in reactor environments is not available, in spite of several past studies which have concentrated on the effects of dissolved oxygen^[IV-44,IV-45], fast neutron flux^[IV-45], fast neutron fluence^[IV-46] and gamma irradiation^[IV-43]. As an interim measure, this model correlates the oxidation enhancement with only two of the more important variables – temperature and reactor environment.

(1) Corrosion Enhancement in BWR Environments. Several authors^[IV-42,IV-43,IV-47] report that oxidation enhancement increases rapidly with decreasing temperatures in BWR environments. Values of A proposed by Asher et al^[IV-42] and Videm^[IV-47] are indicated in Figure IV-24. An average value of A ~19 (indicated by the open triangle in Figure IV-24) was developed from data presented by Megerth et al^[IV-48] and Williamson^[IV-49]. (The Megerth and Williamson data were obtained with pH7 water with 30 ppm oxygen content.) It is recommended that the straight line between the points in Figure IV-47 be used to estimate the enhancement in a typical BWR environment; an analytical expression corresponding to this straight line has been programmed into the subcode which describes cladding oxidation.

(2) Corrosion Enhancement in PWR Environments. Figure IV-24 also shows the results of a fit of Equation (IV-9) to values of oxide thickness reported by Hillner^[IV-50] which requires an average value of A = 3.8. A similar fit of the equation to values reported from Saxton PWR rods^[IV-51,IV-52] resulted in a value of A ~ 2.2. These values are also plotted in Figure IV-24. The relatively small value of A in PWR environments (which do not contain dissolved oxygen in the bulk coolant) is consistent with the picture of enhanced oxygen atom and ion supply rates due to ionization of dissolved oxygen. As in the case of PWR environments, the straight line between the points in Figure IV-24 is used by the cladding oxidation subcode to estimate the enhancement in a typical PWR environment.

6.3.4 Comparison with In-pile Data. Figures IV-25, IV-26, and IV-27 compare the average value predictions of the model developed herein with the values reported for individual samples. There is considerable scatter in the data from individual rods, with maximum measured values of oxide thickness as large as twice the average values. In some cases, such as the Shippingport data^[IV-50] of Figure IV-25, variations are consistent with the idea that temperature variations are responsible. In other cases, such as the Saxton data of Figure IV-26, variations are not explained solely by temperature variation, and the cause is probably related to local variations in quality or chemistry caused by nucleate boiling. Similarly, variations in coolant quality along the BWR rods could contribute to the large scatter in the BWR data^[IV-48] of Figure IV-25, IV-26, and IV-27. Figures IV-26 and IV-27 refer to PWRs with relatively low oxidation rate enhancements. However, the temperature is higher in the case of Figure IV-26, producing a shorter pretransition period due to more rapid oxidation. Figure IV-27 refers to BWRs having low temperatures but very large oxidation enhancement factors (18 in this case). This enhancement overwhelms the temperature effect and results in an even shorter pretransition period so that the relatively rapid oxidation rate of the posttransition regime is predicted to start early in the BWR reactor.

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Fig. IV-24 Estimates of enhancements over out-of-pile oxidation rates when cladding is irradiated in typical BWR and PWR environments.

Fig. IV-25 Comparison of predicted weight increase due to oxidation with base data from average values of six Zircaloy-2 rods in the Shippingport PWR environment at 277°C.

6.3.5 An Approach for Time-Dependent Problems. The basic equation which is used in Section 6.3.2 for the purpose of developing expressions for the amount of oxidation as a function of time is a rate equation

$$\frac{dX}{dt} = \frac{AR e^{-T_0/T}}{X^2} \quad (IV-19)$$

Fig. IV-26 Comparison of predicted weight increase due to oxidation with base data from Zircaloy-4 rods in the Saxton PWR at 340°C.

In order to integrate this equation, it was assumed that A , R , and T are constants. If the reactor environment or the cladding temperature (A or T) vary with time, the rate equation must be reintegrated, considering the time dependence of A or T . Problems which involve a few steps of the reactor between otherwise constant values of T and A can be approached by dividing the overall problem into a number of time sequences in which A and T are constant and then using the oxide thickness at the end of one time interval as the initial oxide thickness of the next interval. It should be recalled, however, that the model is developed in the temperature range of 250 to 400°C.

6.4 A Model for Cladding Hydrogen Uptake

D. L. Hagman

This section describes the development of an analytical expression for the average weight fraction of hydrogen in zircaloy during typical reactor operation at temperatures of 250 to 400°C. The model is closely coupled to the model for cladding oxidation discussed in Section 6.3.

Fig. IV-27 Comparison of predicted weight increase due to oxidation with base data from Zircaloy-2 rods irradiated in the Vallecitos and Dresden BWRs at 286°C.

The average weight fraction of hydrogen in zircaloy cladding during steady state condition is

$$H = H_o + H_l + H_c \quad (IV-20)$$

where

- H = net weight fraction of hydrogen in the cladding (parts per million).
- H_O = initial concentration of hydrogen in the cladding due to impurities introduced during manufacturing and autoclaving (parts per million). Typical values are 8 to 30 ppm^[IV-48,IV-50].
- H_1 = concentration of hydrogen in the cladding due to out-gassing of water absorbed by the fuel (ppm).
- H_C = concentration of hydrogen in the cladding due to absorption of hydrogen from the coolant (ppm).

H_O is an input parameter, and H_1 can be determined by assuming that all of the water vapor absorbed by the fuel is released to the cladding. The primary consideration in determining H is the determination of a value for H_C . Analytical expressions for H_C are divided into two parts, Equation (IV-20a) for oxide films thinner than the transition thickness^[a] and Equation (IV-20b) for oxide films which have grown beyond the transition thickness. In these equations, the H variables have been converted from mg/dm² of hydrogen across the cladding surface to units of average parts per million by weight in the cladding.

$$H_C = \left[\frac{0.0154}{\tau} \right] \left[\frac{B}{8A} \right] \left[X_{PRE} - X_O \right] = H_{CPRE} \quad (IV-20a)$$

$$H_C = H_{TRAN} + \left[\frac{0.0154}{\tau} \right] \left[\frac{2.27B}{8A} \right] \left[X_{POST} - X_{TRAN} \right] = H_{CPOST} \quad (IV-20b)$$

where

- H_C = weight fraction of hydrogen added to the cladding from the coolant (ppm by weight)
- H_{TRAN} = value of H_C at the transition weight^[a] of the oxide film (ppm by weight)
- τ = initial cladding thickness (meters)

[a] Oxide film growth is discussed in conjunction with the description of cladding oxidation. The terms pre- and posttransition refer to two different stages in the growth of the oxide film. A transition between the two stages occurs when the oxide film has added approximately 30 mg of oxide per dm² of oxide surface.

B	=	fraction of hydrogen liberated by the reaction with the coolant which is absorbed during pretransition corrosion (discussed in Section 6.4.3 below). In this model, B includes any additional hydrogen input due to sources other than the expected out-of-pile data for different alloys and environments. Values of B proposed for Zircaloy-2 are 0.48 in a PWR environment and 0.29 in a BWR environment. For Zircaloy-4, B = 0.12 in a PWR environment. Although data are not available for Zircaloy-4 in a BWR environment, the value B = 0.12 is suggested as a reasonable estimate for this unlikely case.
A	=	a parameter describing the enhancement of the oxidation rate of the cladding in the reactor environment. The parameter is discussed in conjunction with the description of cladding oxidation.
X_O	=	initial weight of the cladding oxide layer expressed in mg/dm^2 of oxygen which has crossed the oxide surface. This term can be approximated as $X_O = 0$ for etched cladding but becomes important if extensive prefilming is carried out or if oxidation is carried out in several steps which occur at different temperatures or in different coolant chemistries.
X_{PRE}	=	total weight of oxygen which has been absorbed by the zircaloy surface when a pretransition oxide film exists (mg/dm^2).
X_{TRAN}	=	total weight of oxygen absorbed at transition point (mg/dm^2), typically $30 \text{ mg}/\text{dm}^2$ or 0.08 mil thick.
X_{POST}	=	total weight of oxygen which has been absorbed by the zirconium surface when the oxide film is in the posttransition state (mg/dm^2).

Expressions for X_{PRE} , X_{TRAN} , and X_{POST} can be calculated using the expressions developed for cladding oxidation. X_O is part of the required input information but may usually be taken to be zero since typical prefilming values are $1 \text{ mg}/\text{dm}^2$ and typical end of life values are hundreds of mg/dm^2 .

The approach and general physical representation used to model hydrogen uptake are summarized in Section 6.4.1. Section 6.4.2 presents a development of the basic out-of-pile model, and Section 6.4.3 generalizes the basic model to a representation for in-pile environments. The model is compared to in-pile data in Section 6.4.4.

6.4.1 Background and Approach. It is generally agreed that oxidation of zirconium alloys by water in the temperature range from 250 to 400°C proceeds by the migration of

oxygen vacancies in the oxide layer^[IV-36,IV-37]. Charge and physical size considerations imply that the mechanism of introduction of hydrogen into the zirconium metal through an oxide film is by entry of neutral hydrogen atoms into oxygen vacancies in the lattice (H_2 is too large and H^+ is too positive). The constant ratio of hydrogen to oxygen (the "pickup fraction") which is added to the metal is explained as having been determined by the competition between possible subsequent reactions of the atomic hydrogen created by corrosion (the atomic hydrogen can combine to form a gas or enter into a surface vacancy in the oxide lattice).

In this representation, the close relationship between the hydrogen weight gain and the oxygen weight gain from the coolant is viewed as a consequence of the fact that the oxygen and hydrogen usually come from a common source (the water molecule) and are transported to the metal by a common carrier (oxygen vacancies). The hydrogen pickup fraction is determined by the composition of the coolant-oxide surface. In particular, it is suspected that nickel oxide from the nickel in Zircaloy-2 absorbs atomic hydrogen at the surface of the oxide of this alloy and thereby enhances the fractional hydrogen uptake for Zircaloy-2.

6.4.2 Out-of-Pile Basis for Model. The in-pile model is based primarily on out-of-pile data because well characterized data on hydrogen uptake as a function of time have only been published for out-of-pile corrosion. At least two plausible suggestions for a hydrogen uptake model can be presented from the representation discussed in Section 6.4.1 above. According to both of these suggestions, the relevant dependent variable is the ratio of the corrosion-liberated hydrogen to oxygen which is absorbed by the metal. It is the independent variables which differ in the models. Brief summaries of the two models and a third less probable model are presented below.

(1) Simple Probabilistic Hydrogen Pickup Model. The fraction of released hydrogen which is absorbed by the oxide surface is assumed to be proportional to the rate of appearance of oxide vacancies at the oxide-coolant interface. In the discussion of the cladding oxidation model it is shown that the vacancies appear at a rate proportional to the inverse of the square of the oxygen weight gain during the pretransition phase of oxidation. During the posttransition phase of oxidation, the surface-averaged rate of appearance of oxide vacancies is constant and proportional to three times the inverse of the square of the weight of the oxide layer at transition. This model would ignore any details of the surface chemistry involved in the absorption of atomic hydrogen by the oxide vacancies.

(2) Surface Controlled Hydrogen Pickup Model. For this model the fraction of released hydrogen which is absorbed by the oxide surface is a constant determined by the metallurgy of the oxide surface. The representation assumes that the effect of surface absorption of atomic hydrogen is dominant in the capture of hydrogen by the oxide film's outer surface.

(3) Diffusion Controlled Hydrogen Pickup Model. It is also conceivable that the time rate of hydrogen input into the metal is controlled by some as yet unconsidered independent diffusion process. In the case of diffusion-controlled hydrogen uptake, the net time rate of hydrogen pickup is proportional to the inverse thickness of the oxide layer.

The rate equations implied by the representations of the three models are summarized in Table IV-III. Pretransition expressions were formulated simply by writing down the mathematical equivalents of the above descriptions. Posttransition expressions for the hydrogen pickup fraction were derived by replacing powers of X (proportional to the oxide thickness) in the pretransition expressions with powers of $\bar{X}$ averaged over a rate-determining oxide thickness that randomly varies from zero to the transition thickness of the oxide film.

TABLE IV-III

RATE EQUATIONS FOR HYDROGEN UPTAKE

Pretransition Rates	Posttransition Rates
1. Simple probabilistic pickup fraction determination	
$\frac{dH}{dX} = \frac{G}{\bar{X}^2}$	$\frac{dH}{dX} = \frac{G}{\bar{X}^2} = \frac{3G}{X_{\text{TRAN}}^2}$
2. Surface-controlled pickup fraction determination	
$\frac{dH}{dX} = P$	$\frac{dH}{dX} = P$
3. Diffusion-controlled time rate	
$\frac{dH}{dX} = \frac{Q}{\bar{X}}$	$\frac{dH}{dX} = \frac{Q}{\bar{X}} = \frac{2Q}{X_{\text{TRAN}}}$
<p>H = hydrogen weight gain (mg/dm²)</p> <p>X = oxygen weight gain (mg/dm², corresponds to oxide thickness)</p> <p>t = time at temperature</p> <p>X_{TRAN} = the transition weight of the oxide layer (mg/dm²)</p> <p>$\bar{X}^2$ = the average of X² with values of X distributed at random between 0 and the transition thickness X_{TRAN} (mg/dm²)</p> <p>$\bar{X}$ = the space average of X with values of X distributed at random between 0 and the transition thickness (mg/dm²)</p> <p>G,P,Q = constants</p>	

When the three very different expressions for hydrogen uptake obtained with these models were integrated and compared with the pretransition data presented in Reference IV-36, the pretransition data for Zircaloy-2 and Zircaloy-4 were found to conform best to the assumption that the rate is surface-controlled. The surface-controlled model is therefore used.

Comparison of experimental pretransition and posttransition hydrogen pickup fractions for Zircaloy-2 show that the posttransition rate is about twice the pretransition rate [IV-36]. Although the simple surface-controlled hydrogen pickup model of Table IV-III predicts equal pre- and posttransition pickup fractions, the model can be improved to account for the increased posttransition pickup. The hydrogen pickup rate is surface limited; thus, the increase in surface area represented by the nonuniform surface of the rate-determining oxide layer of the posttransition stage will increase the net absorption probability when the smooth surface of the pretransition regime is replaced by the rough posttransition surface. If this representation is correct, pretransition and posttransition values of the constant hydrogen pickup rate will have nearly the same ratio in all alloys as the values 0.75 and 0.33 reported in Reference IV-37 for Zircaloy-2.

The resultant expressions for the fractional pickup of hydrogen are for pretransition

$$\frac{dH}{dX} = \frac{B}{8} \quad (\text{IV-21a})$$

and for posttransition

$$\frac{dH}{dX} = \frac{2.27B}{8} \quad (\text{IV-21b})$$

where B is determined by the oxide surface metallurgy of the particular alloy (i.e., the presence or absence of nickel which absorbs atomic hydrogen, for example) and 8 accounts for the different weights of hydrogen and oxygen in water so that $\frac{dH}{dx} = 1/8$ for complete pickup.

6.4.3 Generalization to an In-Pile Model. Prediction of in-pile corrosion is complicated because important variables (local temperature and reactor chemistry) are not always reported and because data on the time dependence of corrosion is limited. Enhancement of hydrogen uptake fractions by the reactor environment is treated by determining the value of the pickup fraction B for each reactor environment, but changes in the rate of hydrogen picked up which are caused by changes in the oxidation rate are described with the parameter A, which has been discussed in conjunction with the oxidation model. Thus, two parameters are specified to describe the two separate processes involved in determining the total rate of hydrogen uptake.

The basic equations for the fraction of hydrogen pickup with respect to the amount of oxygen pickup (dH/dX) have been discussed in Section 6.4.2 [Equations (IV-21a) and (IV-21b)]. Those equations referenced out-of-pile oxidation; for in-pile pickup the enhancement factor A must again be considered. It is presumed that the effect which enhances the in-reactor oxidation rate does not enhance the rate of hydrogen uptake. Thus, the enhancement of the oxidation rate by a factor A will decrease the fractional hydrogen uptake by a factor $\frac{1}{A}$.

The rate equations for in-pile oxidation and for fractional pickup of hydrogen are summarized in Equations (IV-22a) and IV-22b) for both pre- and posttransition regimes.

For the pretransition in-pile regime

$$\frac{dH}{dX} = \frac{B}{8A} \quad (IV-22a)$$

For the posttransition in-pile regime

$$\frac{dH}{dX} = \frac{2.27 B}{8A} \quad (IV-22b)$$

The integrated form of these equations is given in Equations (IV-23a) and (IV-23b).

For the pretransition in-pile region

$$H = H_O + \frac{B}{8A} (X_{PRE} - X_O) \quad (IV-23a)$$

and for the posttransition in-pile regime

$$H = H_{TRAN} + \frac{2.27B}{8A} (X_{POST} - X_{TRAN}) \quad (IV-23b)$$

The subscripts refer to the point of evaluation: X_O is oxygen weight at time zero; the subscript PRE implies evaluation during a pretransition oxide flux; TRAN refers to the transition point; POST refers to the posttransition regime.

An out-of-pile value of the parameter B has been determined in Reference IV-37 (from unpublished data) to be $B = 0.33$ for Zircaloy-2. For Zircaloy-4, a value of $B = 0.12$ was obtained from Figure 12 of Reference IV-36. The result is consistent with a value of 10% recommended by Reference IV-37.

When values of B were fit to the average hydrogen pickup values for the Zircaloy-4 rods in the Saxton reactor^[IV-51,IV-52], an average value of $B = 0.104 \pm 0.04$ was obtained. Thus, the out-of-pile determined value of $B = 0.12$ is apparently adequate for Zircaloy-4 rods in PWRs. Values of B obtained by fitting the Zircaloy-2 PWR hydrogen pickup reported in Reference IV-50 were $B = 0.488 \pm 0.07$ while a fit to the BWR hydrogen pickup

data on the Zircaloy-2 rods of Reference IV-49 produced values of $B = 0.29 \pm 0.06$. Since the PWR environment has an overpressure of hydrogen and it is known that hydrogen overpressures enhance the out-of-pile pickup fraction^[IV-36], it is suggested that the difference in PWR and BWR values for B with Zircaloy-2 is an effect of the different environments.

6.4.4 Comparison with In-Pile Data. Figures IV-28 through IV-30 compare the average value predictions of the model developed in this report with the values reported for individual samples. As suggested by Reference IV-47, maximum values of 1.5 to 2 times the average value of hydrogen pickup are to be expected, due to changes in flux and possibly in local chemistry, at various positions along the rod axis.

Note that the pretransition region varies considerably in time in Figures IV-28, IV-29, and IV-30. Figures IV-28 and IV-29 refer to PWRs with relatively low oxidation rate enhancements. However, the temperature is higher in the case of Figure IV-29, producing a shorter pretransition period due to more rapid oxidation and hydrogen pickup. Figure IV-30 refers to BWRs, having low temperatures but very large oxidation enhancement factors (18 in this case). This enhancement overwhelms the temperature effect and results in an even shorter pretransition period so that the relatively rapid hydrogen pickup rate of the posttransition regime is predicted to start early in the BWR.

Fig. IV-28 Comparison of increase in hydrogen predicted by hydrogen pickup model with base data from six Shippingport Zircaloy-2 rods (assuming an initial 25 ppm hydrogen) in a PWR environment at 277°C.

6.5 Irradiation and Cold Work Effects on Zircaloy Cladding Mechanical Properties

D. L. Hagrman

Preliminary models have been prepared for the effects of cold work and irradiation on cladding plastic stress-strain relations.

In the stress-strain models the relation

$$\sigma = K\epsilon^n \quad (\text{IV-24})$$

Fig. IV-29 Comparison of increase in hydrogen predicted by hydrogen pickup model with base data from Saxton Zircaloy-4 rods (assuming an initial 25 or 8 ppm hydrogen, depending on the manufacturer) in a PWR at 340°C.

Fig. IV-30 Comparison of increase in hydrogen predicted by hydrogen pickup model with base data from Zircaloy-2 rods irradiated in the Vallecitos and Dresden BWRs at 286°C.

is used where σ is the true stress, K is the strength coefficient, ϵ is the true strain, and n is the strain hardening coefficient. Expressions for the effects of cold work and irradiation on K and n are given here. The effects on the ultimate true stress σ_u may be determined from the relationship $\sigma_u = Kn^n$.

For cladding which has remained at temperatures below 500°C, the strength coefficient K is dependent only on the amount of cold work and is described by

$$K = AK (1 + 0.546 \text{ COLDW}) \quad (\text{IV-25})$$

where

AK = temperature-dependent strength coefficient for annealed cladding (MN/m^2)

COLDW = cold work (fraction of cross sectional area reduction).

For cladding which has remained below 275°C the strain-hardening exponent n is expressed as

$$n = (AN)YZ \quad (\text{IV-26})$$

where

AN = temperature dependent strain hardening coefficient for annealed cladding

Y = effect of cold work on n

Z = effect of irradiation on n as a function of the fluence. This effect is also dependent upon the cold work condition.

The analytical expressions for Y and Z are

$$Y = 0.847 \exp(-39.2 \text{ COLDW}) + 0.229 (\text{COLDW})^2 - 0.0916 \text{ COLDW} + 0.153 \quad (\text{IV-27})$$

$$Z = \exp \left[-(\Phi)^{1/3} / (3.73 \times 10^7 + 2 \times 10^8 \text{ COLDW}) \right] \quad (\text{IV-28})$$

where

COLDW = cold work (fraction of cross sectional area reduction)

Φ = fast neutron fluence (n/m^2).

Graphs of the analytical expressions for K and n and the data^[IV-53] from which the expressions were derived are shown in Figures IV-31 and IV-32.

The model incorporates the gross effects of annealing. Cold work is assumed to anneal in the temperature range from 500 to 575°C. Irradiation-caused changes are assumed to anneal between 275 and 375°C. For temperatures greater than 575°C, the value of n at 575°C (i.e., 0.0279) is assumed, and K is taken to be AK.

6.6 Analysis of Flow Redistribution in Partially Blocked Subchannels of the LOFT Core K. R. Perkins

In order to determine whether subchannel blockage in the LOFT reactor core will have significant effects on adjacent fuel behavior during a loss-of-coolant test, a three-dimensional analysis has been performed using SCORE-EVET^[IV-54]. The four subchannels adjacent to the center rod in several array sizes were assumed to be blocked by varying amounts under normal PWR operating conditions. The results are summarized in Table IV-IV. Flow reduction in the blocked channel varies nearly linearly with the blockage size and also increases with increasing array size. The first observation indicates that larger blockages would not drastically change the results, since there appears to be a direct relationship between flow rate and blockage size. The latter observation indicates that, as expected, flow redistribution will be more severe in large arrays than in smaller arrays, but the calculations further indicate that a 25-rod bundle should be large enough to show flow redistribution effects if ballooning occurs.

Fig. IV-31 Data and least squares fit to strength coefficients as a function of cold work and irradiation at room temperature.

Fig. IV-32 Data and analytical functions for strain hardening coefficient as a function of cold work and irradiation at room temperature.

TABLE IV-IV

VELOCITY REDUCTION AT NORMAL PWR OPERATING CONDITIONS IN A
BLOCKED SUBCHANNEL FOR VARIOUS ARRAY SIZES

Rod Bundle Array Size	Velocity Reduction with No Flow Area Blockage[a] (%)	Velocity Reduction at 16% Area Blockage (%)	Velocity Reduction at 31% Area Blockage (%)
1[a]	0	-19[b]	-45[b]
25	0	3	7
49	0	5	9
81	0	6	10

[a] An area reduction of 0% and an array size of 1 is considered implicitly since there is no flow diversion for either case.

[b] The negative sign indicates that an increase in velocity occurs rather than a decrease.

Although the velocity reductions in a blocked subchannel as listed in Table IV-IV are significant, the effects on fuel behavior are not likely to be severe. The maximum reduction of 10% is expected to reduce the critical heat flux in the ballooned region by about 4% while the post-CHF HTC would be reduced by about 8%. These heat transfer effects would occur on both the ballooned rod and on adjacent rods at the axial location of ballooning.

It is also of interest to determine whether the heat transfer effect downstream from a ballooned rod will be more severe than the effects in the blocked region. Since the mass flow of coolant through a blocked subchannel is reduced somewhat more than the flow area reduction, it is expected that there will be substantial reduction in velocity immediately downstream from the blockage (as the flow area is increased back to normal, the velocity is reduced unless there is a large transverse influx of coolant). The calculations indicated that there was only about a 3% reduction in the velocity at the volume immediately downstream from the blockage, but this was apparently due to the large volume lengths (6.6 in.). In order to investigate the rate of velocity recovery, additional calculations were performed for 31% blockage in a 49-rod array with a volume length of 0.4125 inch. These calculations indicate that maximum velocity reduction downstream from the blocked subchannel is about 8% and that the velocity recovers to within 1% of the undisturbed velocity within one inch of the blockage location. This calculation implies that for blockages up to 31% there will be no stagnation region behind the blockage and that the velocity will immediately recover to its undisturbed value. Since the CHF and post-CHF heat transfer are directly related to the flow velocity, the heat transfer effects in regions adjacent to the blockage are expected to be minimal. Thus, failure propagation via hydrodynamic effects is not expected to occur for blockage up to 31%.

The range of blockage investigated (0 to 31%) does not encompass the entire range of blockages expected to occur during a LOCA^[a]. Attempts were also made to calculate flow redistribution around a 62% blockage (the blockage corresponding to adjacent rods which are ballooned to the point of just touching). However, the SCORE-EVET calculations generated incomprehensibly large transverse velocities (as high as 10 ft/sec) in the blocked region. While this solution cannot be physically real, it may indicate the presence of a real (rather than purely numerical) instability such as flow separation. If flow separation does not occur for large subchannel blockage, the velocity recovery downstream from the blockage will be substantially reduced. In fact, there is some experimental evidence that subchannel flow reductions do occur downstream from large flow blockage. Vegter et al^[IV-58] measured air flowing in a scale model of a liquid-metal fast breeder reactor assembly. For this scale model there were no significant downstream velocity reductions for subchannel blockages up to 20% (indicating no flow separation and complete velocity recovery downstream from the blocked region). However, for a 50% subchannel blockage there was a 20% reduction in the downstream velocity (indicating at least some flow separation induced by the blocked region).

In summary, the SCORE-EVET calculations indicate that flow redistribution will be significant in both PBF and LOFT for moderate subchannel blockages to 31%. However, the influence of such flow redistribution on adjacent rods and downstream locations is apparently minimal because of rapid flow recovery behind the blocked region. For more severe blockages (50% or more) some experimental evidence^[IV-58] and instabilities in the SCORE-EVET solution indicate that some flow separation may occur. This in turn may cause significant reductions in the downstream velocity and thus provide a mechanism to reduce heat transfer coefficients and possibly cause failure propagation.

7. INDUSTRY COOPERATIVE PROGRAM

A new task has been initiated in support of the BWR blowdown heat transfer and emergency core cooling experimental program to be conducted at General Electric. This is a new test program, designed to provide experimental data for BWR code verification through the blowdown and emergency core cooling time periods. The experiments will be conducted in a modified version of the Two Loop Test Apparatus (TLTA).

The objectives of the new task are to establish a baseline BWR LOCA response and to compare it with anticipated TLTA performance. Further objectives include assessment of TLTA response typicality, potential scaling distortions, and TLTA modifications which may improve the simulation of a BWR.

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- [a] Multirod burst tests using electrically heated cladding samples (Reference IV-55) have indicated average subchannel blockages of from 21 to 84% while fuel rod failure tests in TREAT (References IV-56 and IV-57) showed subchannel blockages of from 48 to 90%. Other fuel behavior calculations indicate sufficient radial strain for 100% blockage during some LOCEs with high pressure fuel rods, but rod-to-rod contact (unmodeled) may induce rupture before a complete blockage actually occurs.

During the BWR LOCA, some thermal-hydraulic conditions not expected in the PWR LOCA may occur, such as entrainment of core spray by rising steam, radiation heat transfer, dispersed flow, and nonequilibrium effects. The initial portion of this study is therefore directed toward a thorough review of the BWR LOCA phenomena expected, the current code limitations, and anticipated code improvements in the near and far term. The purpose of this review is to develop an analytical plan where future studies are scoped and additional code development requirements are identified in a time frame consistent with the planned experimental schedule.

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