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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
JANUARY - MARCH 1977**

MASTER

June 1977

EG&G Idaho, Inc.

IDAHO NATIONAL ENGINEERING LABORATORY

ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION

IDAHO OPERATIONS OFFICE UNDER CONTRACT EY-76-C-07-1570

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PROGRAMS SPONSORED BY THE NUCLEAR REGULATORY COMMISSION'S
DIVISION OF REACTOR SAFETY RESEARCH, JANUARY - MARCH 1977

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Analysis Development

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ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION
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ABSTRACT

Light water reactor safety research performed January through March 1977 is discussed. A test series to evaluate the effectiveness of different emergency core coolant injection arrangements was completed. A LOFT test series was initiated to investigate the effect of break nozzle geometry on break flow and system response during a 200% double-ended cold leg break test. Two analyses of results of LOFT nonnuclear experiments were made. The intact loop flow resistance on LOFT system responses during loss-of-coolant experiments was evaluated. The second in a series of five tests to evaluate the effects of fuel density, initial gap width, and fill gas composition on gap conductance was performed with four BWR-type rods tested simultaneously. A test was conducted to provide steady state, power ramp, and film boiling operation behavior data on the fuel rods constructed from irradiated and unirradiated cladding and to investigate the influence of simulated fission products on cladding integrity and performance. Cladding ballooning phenomena were also investigated. Results of preliminary postirradiation examinations of irradiation effects, power-cooling mismatch, and gap conductance tests performed previously are reported. A review of FLECHT and Semiscale forced feed and gravity feed reflood heat transfer experiments was performed. A study to predict and compare surface temperature response of Semiscale electrically heated rods with LOFT nuclear rods during a Semiscale blowdown was completed. SPERT and NSRR data were analyzed to evaluate fuel failure modes and the effect of energy deposition for cold-startup conditions. Collection of fuel and fuel rod property data continues from experiments in the Halden Test Program. The postirradiation examination of the IFA-226 mixed-oxide experiment was completed at the Research Establishment RISO in Denmark. A conceptual design of a gamma scanner to accommodate full-length commercial fuel rods was completed. Fabrication of a pulsed eddy current scanner for commercial rods was initiated. Development of the FRAP codes continued. A two-dimensional heat conduction subcode was written for FRAP-S to permit calculation of fuel temperatures when the power generation rate varies circumferentially. FRAP-T models were added to include strain rate effects in the small deformation calculations. The cladding emissivity model of MATPRO was revised to reflect new data, and models of the effect of oxygen on the mechanical properties of zircaloy were generated. A more general version of the RELAP5 Pilot Code (Version 2) was programmed. A version of RELAP4/MOD7 is being developed to include models for best estimate modeling of a BWR during reflood following a hypothetical loss-of-coolant accident. Experiment results from tests conducted at the Marviken Containment Test Facility in Sweden and the Battelle Test Facility in Frankfurt, Germany, were used for verification of computer containment codes.

PREFACE

EG&G Idaho, Inc. (EG&G) performs technical activities in the water reactor safety programs at the Idaho National Engineering Laboratory under the sponsorship of the U.S. Nuclear Regulatory Commission's Division of Reactor Safety Research. The current water reactor safety activities of EG&G Idaho, Inc. are accomplished in four programs: the Semiscale Program, the Loss-of-Fluid Test (LOFT) Experimental 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 experiment 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 the Semiscale Mod-1 test system, which has one intact loop with active components and a broken loop with passive components. These experiments include core reflood and emergency core cooling tests using an electrically heated 40-rod core.

The LOFT Experimental 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 (PWR) and LOCA conditions; (b) evaluation of the performance of PWR engineered safety features, particularly the emergency core cooling system; 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 to conduct loss-of-coolant experiments (LOCES). The test programs 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, off-normal, and accident conditions. The experiment portion of the program is concentrated on testing of single fuel rods and fuel rod clusters under power-cooling mismatch, loss-of-coolant, and reactivity initiated accident conditions. These tests provide in-pile experiment 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 this program provide a basis for improvement of the fuel 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 loss-of-coolant accident, thermal-hydraulic code development, containment analysis development, and fuel behavior and code verification. The program

produces computer codes for predicting the behavior of reactor systems, establishes data requirements for and performs verification of safety assessment codes, and maintains and improves 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 are ANCR-1262 (for April – June 1975), ANCR-1296 (for July – September 1975), ANCR-NUREG-1301 (for October – December 1975), ANCR-NUREG-1315 (for January – March 1976), TREE-NUREG-1004 (for April – June 1976), TREE-NUREG-1017 (for July – September 1976), and TREE-NUREG-1070 (for October – December 1976). Copies of the quarterly reports are available from the Technical Information Center, Energy Research and Development Administration, Oak Ridge, Tennessee.

SUMMARY

A test series to evaluate the effectiveness of different emergency core coolant (ECC) injection arrangements was completed. One of the tests investigated the influence of a vent line between the broken loop hot and cold legs during a 200% double-ended cold leg break with ECC injected into the intact loop and broken loop cold legs. The preliminary analysis of the results of this test indicates the vent line had significant effect on the system thermal-hydraulic response during both the blowdown and reflood phases of the transient.

A LOFT counterpart test series was initiated to investigate the effect of break nozzle geometry on break flow and system response during a 200% double-ended cold leg break test. The nozzles used were geometrically scaled versions of the nozzles to be used in LOFT and differ significantly from the nozzles used until now in the Semiscale Mod-1 system. The type of nozzle used has significant effect on the break flow rates during subcooled blowdown and during the low-quality portion of saturated blowdown.

Two analyses of the results of LOFT nonnuclear experiments were made. The intact loop flow resistance on the LOFT system responses during loss-of-coolant experiments (LOCEs) was evaluated. The analysis concluded that there were no significant effects caused by varying the intact loop flow resistance. The second analysis reported was performed to evaluate the analytical model used for predicting LOFT pressurizer performance during a LOCE. As a result of the analysis, the pressurizer model has been improved. The improved model has increased the accuracy for predicting the early behavior of system densities and pressures during a LOCE.

Two tests, GC 2-2 and IE-5, were conducted in the Power Burst Facility (PBF), and preliminary test results are reported for these tests and for Test PCM-4. Preliminary postirradiation examination results are reported for Tests IE-2, IE-3, IE-5, PCM-3, PCM-4, GC 1-3, and GC 2-1, which were conducted during previous quarters.

In Test GC 2-2, the second in a series of five tests designed to evaluate the effects of fuel density, initial gap width, and fill gas composition on gap conductance, four BWR-type rods were tested simultaneously in the PBF in-pile tube.

A major change in design from Test GC 2-1 was reorientation of the cladding surface thermocouples at the axial flux peak with respect to the off-center fuel pellet thermocouples. For Test GC 2-2, the cladding surface thermocouples were oriented azimuthally 60 degrees from the off-center thermocouples.

The primary objectives of Test IE-5 were to provide steady state, power ramp, and film boiling operation behavior data on the fuel rods constructed from irradiated and unirradiated cladding and to investigate the influence of simulated fission products on cladding integrity and performance. In addition, cladding ballooning phenomena were investigated by testing a rod having an internal pressure during steady state and film boiling operation that was greater than the coolant pressure.

Results of the preliminary postirradiation examination indicate that although the performance of the cladding surface thermocouples was marginal, there was no apparent loss of critical instrumentation. The power ramp produced substantial fuel-cladding interaction.

The objectives of Test PCM-4 were met satisfactorily, except that the desired cladding surface temperatures were exceeded. This was probably due to the error in the calibration of the coolant differential thermocouple pairs and the error in the online thermal-hydraulic computer program used to calculate fuel rod powers.

The in-pile fuel rod behavior data obtained during the test include information on fuel pellet-cladding interaction and its effect on cladding elongation, fuel centerline temperature, cladding surface temperature, and fuel rod internal pressure during normal and post-DNB operating conditions.

Preliminary postirradiation examination of the fuel rods from Test IE-2 indicated that of the two parameters studied, gap size and cladding irradiation, gap size was the dominant one. The rods with larger gaps experienced higher fuel temperatures, onset of pellet-cladding interaction at higher rod powers, less total cladding elongation, increased relaxation of the cladding at high power, greater cladding collapse on the fuel during film boiling, larger pressure increases, and longer film boiling zones.

The rods from Test IE-3 were tested at a ramp rate of 21 kW/m-min between rod average powers of 22 and 61 kW/m. This was the highest ramp rate used in any test of the irradiation effects test series. Despite the irradiation-damaged state of the cladding, the fuel rods operated without pellet-cladding interaction failure during the rapid power ramp. No gross fuel rod failures were observed.

All four test rods from Test PCM-3 remained intact during reactor operation and following reactor shutdown. Black oxide was present on the surface of all fuel rods, and bowing was observed along all four rods. All of the fuel rod samples sectioned for metallographic examination were taken from locations over the approximate region of high temperature test operation between 0.52 and 0.82 metre from the bottom of the fuel stack.

A detailed review of the FLECHT and Semiscale forced feed and gravity feed reflood heat transfer experiments was performed.

As part of the short-term NRC technical assistance efforts, a study to predict and compare the surface temperature response of Semiscale electrically heated rods with LOFT nuclear rods during a Semiscale blowdown was completed. Core thermal-hydraulic calculations were made for each type of rod, using RELAP4.

SPERT and NSRR data were analyzed to evaluate fuel failure modes and the effect of energy deposition for the cold-startup conditions used during these tests.

Collection of fuel and fuel rod property data continues from experiments in the Halden Test Program. The postirradiation examination of the IFA-226 mixed-oxide experiment was completed at the Research Establishment RISO in Denmark.

A conceptual design of a gamma scanner to accommodate full-length commercial fuel rods was completed. Fabrication of a pulsed eddy current scanner for commercial rods was initiated.

Development of the FRAP-S and FRAP-T codes continued. FRAP-S3 programming and preliminary verification were completed, and FRAP-S3 was delivered for independent verification. The independent verification and documentation of FRAP-T3 were completed.

A two-dimensional heat conduction subcode was written for inclusion in FRAP-S to permit calculation of fuel temperatures when the power generation rate varies circumferentially as well as radially. Models were added to FRAP-T to include strain rate effects in the small deformation calculations.

The cladding emissivity model of the material properties subcode (MATPRO) was revised to reflect new data, including the effect of oxide thickness, and models of the effect of oxygen on the mechanical properties of zircaloy were generated.

A more general version of the RELAP5 Pilot Code (Version 2) was programmed. This code is more modular and has a revised solution scheme to permit application to fluid network problems characteristic of reactor systems. A version of RELAP4/MOD7 is being developed to include models for best estimate modeling of a boiling water reactor during reflood following a hypothetical loss-of-coolant accident. Two models, a steam/water separation model and a top quench model, are discussed.

Experiment results from tests conducted at the Marviken Containment Test Facility in Sweden and the Battelle Test Facility in Frankfurt, Germany, were used for verification of operational computer containment codes.

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DIVISION OF REACTOR SAFETY RESEARCH, JANUARY – APRIL 1977**

I. SEMISCALE PROGRAM

D. J. Olson, Program Manager

The alternate emergency core coolant (ECC) injection test series, which was designed to evaluate the effectiveness of various ECC injection arrangements, was completed. One of the tests in this series (Test S-05-5) investigated the influence of a vent line between the broken loop hot and cold legs during a 200% double-ended cold leg break with ECC injected into the intact loop and broken loop cold legs. The preliminary analysis of the results of this test, presented in Section 1, indicates the vent line had significant effect on the system thermal-hydraulic response during both the blowdown and reflood phases of the transient.

The LOFT counterpart test series was initiated with Test S-06-5, which was conducted to investigate the effect of break nozzle geometry on break flow and system response during a 200% double-ended cold leg break test. The nozzles used in this test were geometrically scaled versions of the nozzles to be used in LOFT and differ significantly from the Henry nozzles used until now in the Semiscale Mod-1 system. As discussed in Section 2, the type of nozzle used has significant effect on the break flow rates during subcooled blowdown and during the low-quality portion of saturated blowdown.

**1. PRELIMINARY ANALYSIS OF THE EFFECTS OF UPPER PLENUM
VENTING COMBINED WITH COLD LEG ECC INJECTION**

E. A. Harvego

Alternate ECC injection concepts were investigated during Semiscale Mod-1 Test Series 5 for the case of a simulated 200% double-ended cold leg break loss-of-coolant accident (LOCA). One of the concepts investigated consisted of ECC injection into the cold legs of the intact and broken loops, combined with the use of a vent line between the broken loop hot and cold legs. The vent line allowed venting of the upper plenum region whenever the pressure in the upper plenum exceeded that in the inlet annulus. The additional flow path provided by the vent line was expected to increase the core reflood rate by reducing the resistance to steam flow out of the upper plenum region.

The effect on core and system response of intact loop cold leg ECC injection combined with a vent line was investigated through the direct comparison of results of two tests (Tests S-04-6 and S-05-5). Test S-04-6 was a baseline test conducted with cold leg ECC injection only, whereas Test S-05-5 used cold leg ECC injection with the vent line in place. Apart from the addition of the vent line in Test S-05-5, all other system geometries and operating variables (pump speed, ECC injection locations and rates ...) were virtually

identical in the two tests, with the single exception that the high pressure injection system (HPIS) was disconnected in Test S-05-5. However, since the injection rate of the HPIS is low, the disconnection of the HPIS did not significantly influence the results. Both tests included the introduction of nitrogen into the system for a period of about 25 seconds following exhaustion of the ECC accumulator water.

Figure I-1 shows the location of the Semiscale vent line in Test S-05-5. Although this configuration is not intended to simulate a full-sized pressurized water reactor (PWR) with upper plenum venting, the resistance of the vent line was core area scaled to give an equivalent resistance approximating that of a PWR vent system. In addition to a control valve in the line to set the desired vent line resistance, a check valve was included to prevent flow from the broken loop cold leg to the broken loop hot leg when the pressure in the inlet annulus exceeded the pressure in the upper plenum.

Fig. I-1 Semiscale vent line.

The system and instrumentation used in Test S-05-5 and the data obtained are presented in Reference 1.

The following sections emphasize those areas of system behavior that were substantially affected by the presence of the vent line in Test S-05-5. The results of the test are presented with respect to system behavior during initial blowdown, during intact loop accumulator ECC injection, and following the initiation of nitrogen injection into the intact loop.

1.1 System Response During Initial Blowdown

Comparison of the test results from Test S-05-5 with those from Test S-04-6 indicated the presence of the vent line in Test S-05-5 caused significant changes in overall core thermal

response during the initial blowdown period. With the vent line, a reduction in the initial core cooling effectiveness resulted in initially faster rod temperature rises and higher peak blowdown temperatures at many core locations; but a subsequent improvement in core cooling caused an earlier turnover in the rod temperatures and resulted in overall lower rod temperatures at the end of the blowdown period.

A major contributing factor to the generally higher peak blowdown temperatures in Test S-05-5 relative to Test S-04-6 was an observed difference in departure from nucleate boiling (DNB) behavior between the two tests. With the vent line, early and stable DNB occurred at all elevations in the core which normally experience DNB. No tendency was apparent for delayed DNB or rewet to occur on rods near unheated channels such as had occurred without the vent line. This difference in DNB characteristics between the two tests is shown in Figures I-2, I-3, and I-4 for the 14-, 29-, and 37-inch core elevations, respectively.

The early and sustained DNB behavior and the lower initial core cooling from 0 to 3 seconds after rupture in Test S-05-5 were the result of differences in initial core flow characteristics caused by the presence of the vent line. Immediately after rupture, the pressure differential between the upper plenum and the inlet annulus caused the vent line check valve to open. As a result, substantial flow was established between the broken loop hot leg and cold leg, which altered the flow patterns within the core. Figure I-5 shows that the negative core inlet flow during the first 3 seconds after rupture with the vent line (Test S-05-5) was significantly less than that observed for the same time period without the

Fig. I-2 Comparison of short-term temperature responses at the 14-inch elevation.

Fig. I-3 Comparison of short-term temperature responses at the 29-inch elevation.

Fig. I-4 Comparison of short-term temperature responses at the 37-inch elevation.

Fig. I-5 Core inlet volumetric flow for Tests S-04-6 and S-05-5.

vent line (Test S-04-6). This difference in core flow characteristics caused early and stable DNB to occur in Test S-05-5 at locations which had experienced delayed DNB in Test S-04-6 and, as a result, local peak blowdown temperatures at these rod locations were increased. However, at locations where early DNB occurred in Test S-04-6 (without the vent line), no significant differences in peak blowdown temperatures were observed. Therefore, the maximum core temperature that occurred at locations which experienced early and stable DNB in both tests remained essentially unchanged, about 1450°F in Test S-05-5 and about 1475°F in Test S-04-6.

From 3 to 19 seconds after rupture, a significant improvement in the core cooling effectiveness was observed in Test S-05-5. The temperature responses in Figures I-6, I-7, and I-8 show that temperatures at the 14-, 29-, and 33-inch elevation, on rods which had experienced delayed DNB in Test S-04-6, reached peaks that were higher in Test S-05-5 because of early and sustained DNB. However, the increased core cooling in Test S-05-5 from 3 to 19 seconds after rupture caused 4- to 6-second earlier turnovers and rapid drops in temperature after the initial peaks. The improved core cooling is attributed to the vent line, which reduced the resistance to steam flow from the upper plenum region and allowed a low quality positive core inlet flow to develop about 3 seconds after rupture (Figure I-5). The core inlet flow in Test S-05-5 remained predominantly positive for the remainder of the blowdown period. By contrast, the core inlet flow in Test S-04-6 remained negative throughout most of the initial blowdown period. The relatively short duration of the initial negative core inlet flow in Test S-05-5 apparently did not allow depletion of the lower plenum water. The fact that low quality fluid was supplied to the core from 3 seconds after

Fig. I-6 Comparison of long-term temperature responses at the 14-inch elevation.

Fig. I-7 Comparison of long-term temperature responses at the 29-inch elevation.

Fig. I-8 Comparison of long-term temperature responses at the 33-inch elevation.

rupture in Test S-05-5 is substantiated by the comparison of the core inlet density measurements shown in Figure I-9, which indicate a relatively high density fluid at the core inlet when positive core flow begins. The supply mechanism appears to be entrainment of liquid from the lower plenum. The turnover beginning at about 3 seconds, followed by a gradual drop in the core inlet density, in Test S-05-5 is evidence of the gradual depletion of the lower plenum water. The theory of lower plenum entrainment and carryover into the core, where evaporation occurs, is further supported by evidence of increased steam generation in the core as indicated by an observed increase in volumetric flow from the upper plenum out the broken loop hot leg nozzle from 3 to 19 seconds after rupture. The positive flow of relatively high density fluid at the core inlet for Test S-05-5, as opposed to the negative flow of relatively high temperature steam down the core for Test S-04-6, resulted in the good core cooling in Test S-05-5.

Overall, the effect of the vent line on peak blowdown temperatures in Test S-05-5 was relatively small. Despite locally higher peak blowdown temperatures in Test S-05-5, caused by differences in initial DNB behavior between Tests S-05-5 and S-04-6, the peak blowdown temperature at the core hot spot remained virtually unchanged. The major effect of the vent line on core thermal response during blowdown appears to have occurred after the initial peak in rod cladding temperatures. The improved core cooling from 3 to 19 seconds after rupture resulted in overall lower core temperatures at the end of the initial blowdown period in Test S-05-5, which is significant in that the total stored energy in the core was lower at the start of reflood.

Fig. I-9 Core inlet fluid densities for Tests S-04-6 and S-05-5.

1.2 System Response During Intact Loop Accumulator ECC Injection

Intact loop accumulator injection occurred from about 19 seconds to about 68 seconds after rupture in Test S-05-5 and over a similar period in Test S-04-6. During this interval, the presence of the vent line caused significant increase in the penetration of the downcomer by ECC fluid injected in the cold leg. Evidence of the increased downcomer penetration was provided by the cold leg density measurements, which indicated reduced ECC bypass with the use of the vent line. The increased downcomer penetration was a direct result of reduced countercurrent flow, which in turn was related to the reduced resistance to flow provided by the vent line, out of the upper plenum region. Despite the increased penetration, the downcomer collapsed liquid level, shown in Figure I-10, exhibited essentially the same behavior with or without the vent line. The similarity in downcomer levels, coupled with the differences in ECC bypass, led to the conclusion that more coolant was passed through the core in Test S-05-5 than in Test S-04-6.

Although reflood started at essentially the same time in each of the tests (about 60 seconds after rupture), the vent line allowed the reflood process to proceed much more rapidly in Test S-05-5. The reduced resistance to steam flow out of the upper plenum virtually eliminated the tendency for steam generated in the core to cause the core inlet to be uncovered. As a result, liquid entered the core more rapidly, producing a substantial increase in core liquid level as shown in Figure I-11; the collapsed liquid level increased at about 4 inches per second, indicating a high reflood rate. The rapid reflooding was accompanied by significant steam generation, which was indicated by large positive flows out of the intact loop and broken loop hot legs.

Fig. I-10 Downcomer collapsed liquid levels for Tests S-04-6 and S-05-5.

Fig. I-11 Core and downcomer collapsed liquid levels for Test S-05-5.

The increase in reflood rate in Test S-05-5 resulted in considerably improved core cooling during the reflood period. Figures I-6, I-7, and I-8 showed that quenching of the core in Test S-05-5 occurred shortly after the start of reflood, whereas in Test S-04-6 quenching did not occur until later in the reflood period. In Test S-05-5, the core hot spot quenched 93 seconds after rupture and the last recorded quench occurred 105 seconds after rupture. By comparison, in Test S-04-6, quenching of the core hot spot occurred 242 seconds after rupture and the last recorded quench occurred about 295 seconds after rupture.

The vent line between the broken loop hot and cold legs appears to have produced expected results during reflood. The improved core cooling in Test S-05-5 is largely due to an increase in reflood rate, which resulted in overall lower rod cladding temperatures, and earlier core quenching (compared with Test S-04-6).

1.3 System Response Following the Initiation of Intact Loop Nitrogen Injection

After the accumulator water was exhausted, nitrogen was allowed to flow into the intact loop cold leg for about 25 seconds. The system response during nitrogen injection did not appear to be substantially influenced by the presence of the vent line. Intact loop and broken loop cold leg density measurements indicate that when nitrogen began to flow from the accumulator, about 68 seconds after rupture, the cold legs were substantially cleared of water. At the same time, the collapsed liquid level in the downcomer, which had started to increase about 35 seconds after rupture, began to decrease and continued to decrease throughout the nitrogen injection period. Similar downcomer behavior was also observed in Test S-04-6.

After nitrogen injection terminated, about 93 seconds after rupture, the mass depletion in the core and downcomer was very similar both with and without the vent line. The effects of the mass depletion on the rod temperatures differed, however, because the bulk of the core had quenched before significant depletion took place in Test S-05-5. As a result, no dryout of rods occurred in the upper portions of the core, whereas dryout did occur on some rods in Test S-04-6 as shown in Figure I-12. In general, the fluid behavior in the downcomer during nitrogen injection and the combined core and downcomer mass depletion following nitrogen injection did not appear to affect the core cooling adversely in the latter portions of Test S-05-5.

Fig. I-12 Comparison of long-term temperature responses at the 44-inch elevation.

2. SENSITIVITY OF SYSTEM RESPONSE TO BREAK NOZZLE GEOMETRY

J. M. Cozzuol

Test S-06-5 of the Semiscale Mod-1 LOFT counterpart test series (Test Series 6) was performed to evaluate the effect of break nozzle geometry on the subcooled and low quality saturated break flow rates and to determine the effect of any change in the break flow response on the overall system blowdown response. The break nozzles (LOFT counterpart nozzles) used in Test S-06-5 to represent the 200% offset shear break were geometrically scaled versions of the nozzles used in the LOFT facility and differ significantly from the converging-diverging nozzles (Henry nozzles) used in previous Semiscale tests. The scaling criteria for the Semiscale LOFT counterpart nozzles were based on the ratio of the break area to system volume of the LOFT system and on the ratio of the length-to-diameter dimensions of the LOFT nozzles. For comparisons, Figure I-13a illustrates the Semiscale LOFT counterpart nozzle used in Test S-06-5 and Figure I-13b illustrates the Henry nozzles used in previous Semiscale tests^[a].

[a] The break flow areas for the LOFT counterpart nozzles and the Henry nozzles are the same (0.00262 ft²).

(a) LOFT Counterpart Nozzle

(b) Henry Nozzle

Fig. I-13 Semiscale Mod-4 break nozzles.

The following paragraphs discuss the effect of break nozzle geometry on the break flow response and the corresponding effect of changes in the break flow response on the overall system behavior. To evaluate the effect of break nozzle geometry, data from Test S-06-5 are compared with data from Test S-04-6 of the baseline ECC test series, in which Henry nozzles were used at the break. With the exception of the different break nozzles and an approximately 17% lower hydraulic resistance across the intact loop steam generator for Test S-06-5, all initial conditions and test operating variables were essentially the same for both tests^[a].

Comparison of the break flow response for Tests S-06-5 and S-04-6 indicates that the change from the converging-diverging nozzle to the LOFT counterpart nozzle had significant influence on the break flow rates during blowdown. The mass flow rates at the vessel inlet side of the broken loop for the two tests are compared in Figure I-14. The mass flow rate during the subcooled portion of blowdown for Test S-06-5 (between 0 and about 3 seconds) is substantially lower than the flow rate during the subcooled portion of blowdown for

Fig. I-14 Mass flow rates in the vessel inlet side of the broken loop.

[a] The somewhat lower hydraulic resistance across the intact loop steam generator for Test S-06-5 was due to a reversed orifice plate on the pump suction side of the steam generator. Comparison of blowdown results from Tests S-06-5 and S-04-6 indicates that essentially no change in the intact loop response occurred as a result of the different resistances.

Test S-04-6 (between 0 and about 2.5 seconds). The lower subcooled flow rate for Test S-06-5 increased the transit time for the hot core fluid to reach the break, thus resulting in an additional 0.5-second delay for the fluid at the break to reach the saturation temperature. The fluid temperatures near the vessel inlet side of the break for Tests S-06-5 and S-04-6 are compared in Figure I-15; the differences in time for the fluid at the break to reach the system saturation temperature are evident.

Comparison of the mass flow rates on the vessel outlet side of the break for Tests S-06-5 and S-04-6 indicates that essentially no difference in the flow rates occurs as a result of the different nozzle geometries. Mass flow rates near the vessel outlet side break for the two tests are compared in Figure I-16. (The zero offset prior to rupture for Test S-06-5 is not representative of the actual flow and does not affect the results after rupture.)

The differences in the break flow response on the vessel inlet side during Tests S-06-5 and S-04-6 resulted in substantial differences in the DNB response of the heater rods during blowdown. Table I-I compares the times at which DNB occurred for each rod thermocouple location for the two tests. As indicated in Table I-I, the DNB behavior for a significant number of the rod thermocouple locations between the 0- and 45-inch elevations shifted from delayed DNB for Test S-04-6 to early DNB for Test S-06-5. In Test S-04-6, 34 of 50 thermocouple locations between the 0- and 49-inch elevations exhibited delayed DNB (times to DNB greater than 2 seconds), whereas for Test S-06-5, only 12 of the 50 locations exhibited delayed DNB.

Fig. I-15 Fluid temperatures near the break on the vessel inlet side.

Fig. I-16 Mass flow rates in the vessel outlet side of the broken loop.

TABLE I-I

COMPARISON OF TIMES TO DNB FOR TESTS S-06-5 AND S-04-6

Thermocouple	Time to DNB (sec)		Thermocouple	Time to DNB (sec)	
	Test S-04-6	Test S-06-5		Test S-04-6	Test S-06-5
TH-E3-05	0.160	0.160	TH-H5-32	3.148	0.626
TH-C7-07 ^[a]	0.817	0.713	TH-B3-33	3.287	2.313
TH-F2-07	0.160	0.160	TH-B5-33 ^[a]	3.304	2.139
TH-A4-09 ^[a]	0.748	0.713	TH-E1-33 ^[a]	3.148	0.713
TH-E4-09	0.830	1.043	TH-E2-33	3.635	2.504
TH-F6-09	3.287	0.922	TH-F5-33	3.356	3.948
TH-G3-13	0.939	0.800	TH-G4-33	3.130	0.626
TH-D2-14	0.696	0.643	TH-E6-37	3.669	4.261
TH-D4-14	3.061	0.730	TH-C2-38	3.043	2.609
TH-D6-14	3.182	0.922	TH-D3-39	3.548	2.539
TH-E8-14 ^[a]	0.609	0.556			

TABLE I-I (continued)

Time to DNB (sec)			Time to DNB (sec)		
Thermocouple	Test S-04-6	Test S-06-5	Thermocouple	Test S-04-6	Test S-06-5
TH-F4-14	3.322	0.939	TH-A4-44 ^[a]	3.495	0.643
TH-G5-14	0.678	0.713	TH-F4-44	3.180	No DNB
TH-D7-20 ^[a]	0.678	0.643	TH-E7-45	3.600	No DNB
TH-E3-20	3.200	0.835	TH-E8-45 ^[a]	3.440	No DNB
TH-F5-20	3.269	0.922	TH-C4-53	No DNB	No DNB
TH-D1-21 ^[a]	0.539	0.626	TH-F5-54	No DNB	No DNB
TH-F2-22	3.165	0.783	TH-E4-55	No DNB	1.861
TH-E3-24	3.182	0.765	TH-D8-57 ^[a]	No DNB	No DNB
TH-G5-24	2.974	0.591	TH-D2-61	No DNB	No DNB
TH-D6-25	3.078	0.817			
TH-F2-25	0.835	0.609			
TH-C4-26	3.356	2.852			
TH-F5-26	3.113	0.678			
TH-B5-28 ^[a]	3.235	0.574			
TH-C5-28	3.391	2.939			
TH-E4-28	3.182	0.730			
TH-E6-28	3.304	2.017			
TH-F4-28	3.252	3.861			
TH-A4-29 ^[a]	0.609	0.730			
TH-A5-29 ^[a]	0.556	0.556			
TH-B6-29 ^[a]	0.487	0.661			
TH-D3-29	3.495	0.730			
TH-D4-29	3.461	2.922			
TH-E8-29 ^[a]	0.556	0.556			
TH-G4-29	2.869	0.748			

[a] Thermocouple is on a rod not adjacent to an unpowered rod.

The larger number of rod thermocouple locations that exhibited early DNB for Test S-06-5 (as compared with Test S-04-6) resulted in a lower rate of energy removal from the core heater rods during the first several seconds of blowdown than occurred for Test S-04-6 and as a result, the maximum cladding temperatures for individual rods were higher for Test S-06-5 than for Test S-04-6. Table I-II presents the maximum rod cladding temperatures that occurred at each thermocouple location. Comparison of these temperatures indicates that the maximum cladding temperatures at several locations in the high power zone (between the 20- and 29-inch elevations) were as much as 240°F higher for Test S-06-5 than for Test S-04-6. However, since the core maximum cladding temperature for both tests occurred at a location which exhibited early DNB, the core maximum temperatures were not significantly different for the two tests. A core maximum cladding temperature of 1527°F was obtained on Rod B6 at the 29-inch elevation for Test S-06-5 as compared with a maximum temperature of 1476°F on Rod A5 at the 29-inch elevation for Test S-04-6.

The cause of the differences in the core DNB response between Tests S-06-5 and S-04-6 is not readily apparent from an evaluation of the core hydraulic response during blowdown. The volumetric flow rates near the vessel outlet sides of the intact and broken loops are compared in Figures I-17 and I-18, respectively, and the core inlet volumetric flow

TABLE I-II
COMPARISON OF MAXIMUM ROD CLADDING TEMPERATURES
FOR TESTS S-06-5 AND S-04-6

Thermocouple	Maximum Cladding Temperature (°F)		Thermocouple	Maximum Cladding Temperature (°F)	
	Test S-04-6	Test S-06-5		Test S-04-6	Test S-06-5
TH-E3-D5	819	895	TH-B3-33	1014	1011
TH-C7-07 ^[a]	1053	1115	TH-B5-33 ^[a]	1081	1236
TH-F2-07	918	991	TH-E1-33 ^[a]	1011	1155
TH-A4-09 ^[a]	1134	1177	TH-E2-33	1131	965
TH-E4-09	1093	1108	TH-E5-33	1189	905
TH-E6-09	995	1048	TH-G4-33	1198	950
TH-G3-13	1143	1259	TH-E6-37	999	850
TH-D2-14	1164	1296	TH-C2-38	785	892
TH-D4-14	1088	1140	TH-D3-39	827	824
TH-D6-14	1133	1271	TH-A5-44 ^[a]	786	841
TH-E8-14 ^[a]	1223	1313	TH-F4-44	803	755
TH-F4-14	1133	1258			

TABLE I-II (continued)

<u>Thermocouple</u>	<u>Maximum Cladding Temperature (°F)</u>		<u>Thermocouple</u>	<u>Maximum Cladding Temperature (°F)</u>	
	<u>Test S-04-6</u>	<u>Test S-06-5</u>		<u>Test S-04-6</u>	<u>Test S-06-5</u>
TH-G5-14	1198	1268	TH-E7-45	922	820
TH-D7-20 ^[a]	1280	1297	TH-E8-45 ^[a]	816	814
TH-E3-20	1063	1301	TH-C4-53	654	652
TH-F5-20	1138	1131	TH-F5-54	637	638
TH-D1-21 ^[a]	1459	1487	TH-E4-55	679	678
TH-F2-22	1225	1471	TH-D8-57 ^[a]	699	667
TH-E3-24	1221	1213	TH-D2-61	634	633
TH-G5-24	1308	1498			
TH-D6-25	1267	1342			
TH-F2-25	1226	1220			
TH-C4-26	1220	1254			
TH-F5-26	1266	1083			
TH-B5-28 ^[a]	1289	1503			
TH-C5-28	1247	1294			
TH-C4-28	1223	1374			
TH-E6-28	1232	1163			
TH-F4-28	1232	1033			
TH-A4-29 ^[a]	1232	1501			
TH-A5-29 ^[a]	1476	1514			
TH-B6-29 ^[a]	1427	1527			
TH-D3-29	1192	1018			
TH-D4-29	1185	1112			
TH-E8-29 ^[a]	1335	1481			
TH-G4-29	1282	1091			
TH-H5-32	1244	1294			

[a] Thermocouple is on a rod not adjacent to an unpowered rod.

Fig. I-17 Core inlet volumetric flow rates.

Fig. I-18 Intact loop vessel outlet volumetric flow rates.

rates for the two tests are compared in Figure I-19. As indicated, the flow rates near the vessel outlet sides of the intact and broken loops are essentially the same for both tests during the early part of blowdown and thus would not be expected to be influential in causing the observed change in DNB behavior. The somewhat smaller reverse core flow observed in Test S-06-5 (as compared with Test S-04-6) during the first 3 seconds following rupture corresponds directly to the reduced vessel inlet side break flow shown in Figure I-14. Initially, the difference in the core inlet flow between the two tests would not appear to be sufficient to cause the changes in core DNB behavior which occurred. However, results from Test S-05-5 of the alternate ECC injection test series (discussed in Section 1) also indicate that a reduction in the magnitude of the core inlet volumetric flow immediately following rupture can lead to an increase in the number of locations which exhibit early DNB. Test S-05-5 had essentially the same initial conditions as Tests S-04-6 and S-06-5, but a bypass line in the broken loop allowed venting of the vessel upper plenum to the vessel inlet side of the broken loop. The bypass line resulted in a considerable reduction in the magnitude of the core volumetric flow reversal at rupture, which in turn caused more of the thermocouple locations between the 0- and 49-inch elevations in the core to undergo early DNB.

Fig. I-19 Broken loop vessel outlet volumetric flow rates.

To evaluate the effect of the differences in the core flow rates on the detailed core hydraulic response for Tests S-06-5 and S-04-6, calculations are currently being performed using a RELAP4 model of the Semiscale Mod-1 core. These RELAP4 calculations are driven with thermal-hydraulic parameters obtained from the core measurements. Although the accuracy of these calculations is currently being evaluated, the results do indicate that

substantial differences in the fluid thermodynamic properties within the core can occur as a result of small changes in the measured flow rate at the core entrance. The most significant differences in fluid properties appear to occur below the core high power zone, where the quality increases at a significantly faster rate for Test S-06-5 than for Test S-04-6 during the first 0.7 second. The higher average quality during this period for Test S-06-5 is consistent with the larger number of locations which exhibited early DNB (compared with Test S-04-6).

In summary, comparison of results from Tests S-06-5 and S-04-6 indicates that the break nozzle geometry had significant influence on both the break flow rates and the core thermal response during blowdown. The LOFT counterpart nozzle used in Test S-06-5 produced a substantially lower break flow rate during the subcooled blowdown than occurred during Test S-04-6, for which the converging-diverging Henry nozzles were used. The lower break flow rate for Test S-06-5, in turn, resulted in a reduction in the magnitude of the core flow reversal during the early part of blowdown, which caused a larger number of locations between the 0- and 49-inch elevations to undergo early DNB than occurred for Test S-04-6.

3. REFERENCE

1. B. L. Collins, M. L. Patton, Jr., K. E. Sackett, *Experiment Data Report for Semiscale Mod-1 Test S-05-5 (Alternate ECC Injection Test)*, TREE-NUREG-1054 (April 1977).

II. LOFT PROGRAM

L. P. Leach

The results of two analyses using data from LOFT nonnuclear loss-of-coolant experiments (LOCEs) are reported: one analysis was performed to determine what effect high or low flow resistances in the intact loop would have on LOFT system behavior during the blowdown and reflood portions of a LOCE. This analysis included data from LOFT LOCEs L1-2, L1-3, and L1-3A (these experiments were 200% cold leg breaks), and models of the experiments using RELAP4/MOD5, FRAP-T2, and FLOOD4 computer codes. The analysis concluded that intact loop flow resistances had no significant effect on system behavior during the LOCEs.

A second posttest flow analysis was performed to compare the analytically predicted performance of the LOFT pressurizer with its measured performance during a LOCE. For this analysis, data from LOCE L1-3A and from a model of the experiment using the RELAP4/MOD5 computer code were used. As a result of this analysis, the RELAP4 LOFT system model was improved to provide a much better model for predicting pressurizer responses and early behavior of system densities and pressures during a LOCE.

1. EFFECTS OF INTACT LOOP FLOW RESISTANCE ON LOCE PHENOMENA

M. S. Jacoby

The scaling of experiments in the water reactor safety programs (LOFT and Semiscale) has been on a power/volume basis. This scaling philosophy results in good thermal system modeling. In the LOFT and Semiscale facilities, the reactor core length is approximately one-half that of a typical large pressurized water reactor (LPWR) core. Since the volume (power) scaling rule was applied, the resulting core area/system volume for LOFT and Semiscale are twice the value for a large scale reactor. The intact loop flow resistance can be scaled by two methods, core volume or core flow area, which differ by approximately a factor of two. In order to determine the effects of applying the methods of scaling intact loop resistance, tests for LOFT and Semiscale Mod-1 were conducted with intact loop hydraulic resistance as a variable. The results from the LOFT isothermal blowdown tests and the Semiscale reflood tests and the results from RELAP4/MOD5^[1], RE-FRAP (a combination of RELAP4/MOD5 and FRAP-T2^[2]), and FLOOD4^[3] computer models were evaluated to determine the optimum intact loop scaling technique for the LOFT nuclear tests.

1.1 LOFT Isothermal Blowdown Tests

One of the objectives of LOFT LOCEs L1-2^[4], L1-3, and L1-3A^[5] was to determine the effects of intact loop flow resistance. LOCE L1-2 had 90% more intact loop flow resistance (including vessel resistance) than LOCEs L1-3 or L1-3A. LOCE L1-2 had no

emergency core coolant (ECC) flow until late in the blowdown, and LOCE L1-3 had ECC flow injection from only the low- and high-pressure injection systems. LOCE L1-3A had ECC injection at the normal times, but flow was directly into the lower plenum. The data for these tests have been reported in References 6, 7, and 8. The results of the tests are discussed here as time histories.

The history for intact loop hot leg density shown in Figure II-1 is the same for high and low resistance up to the time of ECC injection at 22.5 seconds after blowdown initiation for LOCE L1-3A. The history for intact loop cold leg density is the same except for a peak at 12 seconds (approximately the time the pressurizer was emptied) in the experiment with high flow resistance (LOCE L1-2). The history after this point is identical for both experiments as shown in Figure II-2.

Fig. II-1 Average fluid density in LOFT intact loop hot leg (DE-PC-2).

Fig. II-2 Average fluid density in LOFT intact loop cold leg (DE-PC-1).

The history of intact loop hot leg flow illustrated in Figure II-3 is identical for both tests. The history of core simulator momentum flux shown in Figure II-4 is identical after 3 seconds into the blowdown. The low-resistance case (LOCE L1-3A) has 10% higher momentum flux at 2 seconds after blowdown initiation. The time of reversal is identical.

1.2 Semiscale Mod-1 Tests

1.2.1 Semiscale Mod-1 Isothermal Blowdown Test.

(1) Isothermal Test Description. Tests S-01-2 and S-01-3^[9,10] were conducted as part of a series of tests (Test Group 1) for comparison of the isothermal blowdown response of the Semiscale Mod-1 system with that of the LOFT system.

Fig. II-3 Mass flow-rate per system volume in intact loop hot leg calculated from DE-PC-2 and FE-PC-2.

Fig. II-4 Momentum flux in LOFT reactor vessel core simulator (ME-CS-1).

Test S-01-3 employed an intact loop flow resistance that was low relative to that of Test S-01-2 in order to establish the importance of intact loop resistance on system response during blowdown. The lower intact loop flow resistance for Test S-01-3 was scaled from that of the LOFT system.

(2) Isothermal Test Results. The results of isothermal blowdown Tests S-01-2 and S-01-3 are reported in References 9 and 10. The upper plenum pressure time history shows an initial difference of 0.345 megapascal (50 psi). The difference during the test was never greater than 0.345 megapascal (50 psi). Both high- and low-resistance tests depressurize in a similar manner. The lower plenum fluid temperature time history showed no difference between high and low hydraulic resistance. The intact loop cold leg mass flow time history showed little effect of intact loop hydraulic resistance. The time history of the density measurement in the lower plenum is unsteady in nature. No essential difference could be seen up to the time of ECC injection.

In summary, the Semiscale Mod-1 isothermal blowdown tests showed no effect of intact loop hydraulic resistance during blowdown.

1.2.2 Semiscale Mod-1 Reflood Tests.

(1) Reflood Test Description. The reflood heat transfer test series (Tests S-03-5, S-03-6, S-03-7, and S-03-8) was conducted in the Semiscale Mod-1 experimental program to determine the effect of intact loop flow resistance during reflood. Only Tests S-03-6 and S-03-7 are discussed here.

The core simulator in the Semiscale Mod-1 system consisted of 40 electrically heated rods of typical LPWR fuel rod diameter and axial power profile (peaking factor of 1.58).

Measurements taken in the core, in addition to the heater rod cladding temperatures, included core voltage, power, current, fluid temperature, shunt circuit technique, and voltage.

The core power was controlled during the gravity feed reflood tests to simulate the decay heating process during the reflood portion of a loss-of-coolant accident (LOCA). The American Nuclear Society (ANS) standard decay curve was used for two of the gravity feed reflood tests, and the ANS standard curve +20% was used for the other two tests.

Test S-03-7 was conducted with the initial conditions matched to the initial reflood conditions expected following the depressurization (blowdown) portion of a typical Semiscale Mod-1 integral test.

(2) Reflood Test Results. The results for Tests S-03-6 and S-03-7 are reported in Reference 11. They show no great effect in the measured parameters.

The heat transfer occurring in the core for Tests S-03-6 and S-03-7 was about the same even though the intact loop flow resistance used for Test S-03-6 was 80% higher than the

Test S-03-7 value. The similarity in core heat transfer was demonstrated by the agreement between the measured temperatures at the core hot spot. This agreement is typical of all other elevations in the core, where the differences are well within test repeatability.

The similarity in heat transfer between Tests S-03-6 and S-03-7 appears to occur because of the similarities in the core flow and the integrated mass entering the core.

1.3 LOFT Analytical Results

The blowdown and reflood phases of a LOCA are modeled by separate computer codes. For the LOFT facility, the blowdown phase was modeled by a combination of RELAP4/MOD5^[1] and FRAP-T2^[2] (RE-FRAP). The reflood phase was modeled by FLOOD4^[3]. Blowdown and reflood analyses of a LOFT nuclear LOCE were performed for both the high- and low-resistance values of the intact loop.

The results of these analyses are discussed as time histories. The time histories of the clad surface temperature at the hot spot shown in Figure II-5 are essentially identical up to

Fig. II-5 LOFT RE-FRAP model.

5 seconds into the blowdown. A maximum difference of 283 K (50°F) occurs at approximately 6 seconds after blowdown initiation. This occurs because the low resistance (core area scaling) case has a lower core quality at this time and thus has a higher heat transfer rate. The time histories of flow are similar to other parameters since they show no great effect of intact loop hydraulic resistance.

The results of the FLOOD4 calculations for core-area-scaled and twice-core-area-scaled intact loop resistance are presented as the time histories for the clad hot spot temperature in Figure II-6. The intact loop low-resistance case has only a slightly higher hot spot

Fig. II-6 FLOOD4 calculated intact loop resistances for LOFT 200% cold leg break.

temperature. The difference reaches a maximum of 300 K (80°F) at the time the quench front reaches the hot spot. Since the clad temperature is constantly decreasing, this temperature difference is of no importance.

1.4 Conclusions

The results of analyses with RELAP4/MOD5, FRAP-2T, and FLOOD4 computer codes, together with experiments in LOFT and Semiscale Mod-1 systems, have been analyzed to evaluate the effect of intact loop hydraulic resistance on LOCE behavior. No significant effect has been demonstrated. Therefore, the intact loop flow resistance need not be considered as a variable when planning the test matrices for future LOFT LOCEs.

2. EFFECTS OF RELAP4/MOD5 MODELING OF THE LOFT PRESSURIZER

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During comparisons of the experiment data with the RELAP4 predictions for LOFT LOCE L1-3A^[12], it was found that in the analytical model the pressurizer emptied faster than in the experiment^[8]. Investigations also revealed that when the pressurizer flow was too great, the fluid remained subcooled for an extended period of time almost everywhere in the system. Also, the pressure at the beginning of saturated blowdown was approximately 100 kilopascal (15 psi) too high. Many other phenomena, like the flow reversal in the intact loop hot leg, also were first predicted incorrectly.

An analysis was performed using the RELAP4/MOD5^[1] (Update 2) computer code. As a result of this analysis, a new pressurizer model was developed which provided much better agreement with almost all the experiment data. The new model incorporated the following changes:

- (1) The form-loss coefficient was increased because RELAP4 applies two-phase multipliers only to Fanning friction losses, but not to form losses. This was consistent with the experimental work of D. E. Fitzsimmonds^[13], who found that two-phase multipliers can be significant for the kinds of bends and conditions that exist in the LOFT pressurizer surge line.
- (2) The elevation for the junction between the pressurizer and the surge line was changed and the pressurizer liquid volume was reduced to conform with LOCE L1-3A test conditions.
- (3) The bubble rise model in the pressurizer was changed to consider the high pressure during the emptying transient.
- (4) The contraction coefficient of 0.75 was applied to the critical flow model^[14].

After the new model was developed, it was applied in a new RELAP4 run to predict LOCE L1-3A results. The results of the new run show that not only did the pressurizer discharge rate come into better agreement with the experiment data, but that early blowdown behavior of the system densities (as illustrated in Figures II-7 and II-8) and pressures came into significantly better agreement as well.

Fig. II-7 Comparison of calculated and measured density in LOFT broken loop hot leg.

Fig. II-8 Comparison of calculated and measured density in LOFT intact loop hot leg.

It is concluded that proper modeling of the pressurizer is necessary, especially for the accurate prediction of the early behavior of the system densities and pressures during an isothermal LOCE. In order to predict accurate flow rates through pipes which have significant form losses distributed along their lengths, two-phase multipliers should be applied to the single-phase form-loss coefficients.

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III. THERMAL FUELS BEHAVIOR PROGRAM

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Tests GC 2-2 and IE-5 were conducted in the Power Burst Facility (PBF) reactor. The following sections describe (a) preliminary test results from Tests GC 2-2, PCM-4, and IE-5, and preliminary postirradiation examination results from Tests IE-2, IE-3, IE-5, and PCM-3; (b) efforts in the area of PBF program development, coordination with foreign experimental programs, Nuclear Regulatory Commission technical assistance, Halden fuel behavior research, and postirradiation examination of commercial power reactor fuel; and (c) continued development of the Fuel Rod Analysis Program-Steady State (FRAP-S) and -Transient (FRAP-T) computer codes, improvement of the FRAP subcodes, and changes made in the material properties subcode (MATPRO).

1. PBF TESTING

P. E. MacDonald and W. J. Quapp

Tests GC 2-2 and IE-5 were conducted in the PBF. Postirradiation examinations were performed on fuel rods from Tests IE-2, IE-3, GC 1-3, GC 2-1, PCM-3, PCM-4 (tests conducted in previous quarters), and IE-5.

1.1 Summary of Ongoing Effort

1.1.1 PCM Test Series Activities. The postirradiation examination of the PCM-3 test rods was completed and is presented in Section 1.3 below. A summary of the PCM-4 comparisons of experiment results and FRAP-T3 calculations is presented in Section 1.4. Preparations for future tests in the series (PCM-5, PCM-6, and PCM-7) are under way. These tests of nine-rod clusters are designed to investigate post-DNB (departure from nucleate boiling) fuel rod behavior within a cluster and possible rod-to-rod interactions. Pretest reactor physics and thermal hydraulic calculations were completed to determine the power generation and onset-of-DNB thermal hydraulic conditions for each fuel rod in the nine-rod cluster.

1.1.2 Gap Conductance Test Series Activities. Test GC 2-2 was performed. Data reduction was essentially completed for Test GC 2-1, performed in December 1976, and postirradiation examination of the Test GC 2-1 rods was initiated.

During analysis of the data for Test GC 1-3, it was discovered that some of the cladding surface temperature phase angles, as determined from data reduced on the PDP-15 data system and processed through MAC/RAN III, were in error. The source of the error was traced to the PDP-15 data reduction process. The nature of the error is such that it occurs randomly and is much larger in some cases than in others. A detailed study of the capability for the PDP-15 to provide accurate data reduction of the gap conductance power oscillation data is in progress.

1.1.3 Reactivity-Initiated-Accident (RIA) Test Series Activities. Efforts in several areas have been under way this quarter. In preparation for the upcoming RIA test series, reactor physics calculations to predict the test rod powers for irradiated rods have almost been completed, and similar calculations to determine fuel enrichments for equal test rod power generation in a 16-BWR-rod cluster were begun. Calculations of the coolant behavior during RIA tests recently performed at the Nuclear Safety Research Reactor (NSRR) in Japan were initiated using the COBRA-IV code. Similar COBRA-IV calculations will be performed for the PBF tests using single rods in shrouds. FRAP-T3 calculations to evaluate expected fuel rod behavior during the first four planned RIA tests are being performed. The instrumentation requirements for the first RIA test have been specified.

The background radiation due to previously irradiated fuel rods and planned preconditioning irradiation precludes accurate fission product analysis to determine test rod energy deposition. Steady state calorimetric measurements will provide the only basis for establishing test rod energy deposition. A proposal for verifying the calorimetric methods by means of a power calibration is being reviewed. The power calibration test is proposed to be conducted before the first scheduled RIA test.

1.1.4 Irradiation Effects (IE) Test Series Activities. The last of the planned IE tests (IE-5) was conducted in February. The test contained fresh fuel and used irradiated cladding for three of the four rods. One rod, which was pressurized to 8.3 megapascals at room temperature, burst during the onset of film boiling. Additional test details are provided in Section 1.8.

The postirradiation examinations on the fuel rods from Tests IE-2 and IE-3 progressed on schedule.

1.1.5 Inlet Flow Blockage Test Series Activities. The inlet flow blockage program is an experiment series designed to study the radiative and convective heat transfer and fuel rod behavior in a 25-rod (5 x 5) array after the inlet flow is reduced by a large amount (65 to 98%) by a simulated blockage at the assembly inlet.

Progress on this program has been made in the physics analyses and thermohydraulic calculations. The physics analyses consisted of enrichment calculations for the 25-rod, BWR-design bundle planned for this test series and the design of an axial flux shaper to flatten the axial power distribution over the central portion of the bundle. The enrichment calculations were performed to achieve a radially (rod-to-rod) uniform ($\pm 10\%$) power distribution under normal operation. An evaluation of the power distribution under the fully voided condition was also initiated.

The major activities in thermal hydraulics analyses consisted of a subchannel code evaluation study and the addition of a radiation model to the MOXY/SCORE code.

1.1.6 Loss-of-Coolant Accident (LOCA) Test Series Activities. Preparations are in progress for the first LOCA test to be run in the PBF reactor. The test requirements^[1] for the LOCA program have been developed to determine the behavior of fresh and irradiated

rods under a wide variety of conditions. During blowdown, the rod surface heat transfer coefficient is to be controlled as a test parameter. This control distinguishes the test program from other LOCA-related programs, where the system thermal hydraulic response is simulated by suitably scaling the test hardware to represent a pressurized water reactor (PWR). By indirect control of the heat transfer coefficient, the cladding temperature is varied in relation to the pressure difference across the cladding. It is expected that different mechanical and chemical responses will result from the combinations of cladding temperature and pressure loading.

Control of the fuel rod heat transfer coefficient is accomplished by changing the break area and location (hot or cold leg) by manipulation of blowdown valves during the test.

The RELAP4 code has been used to determine a preliminary blowdown valve sequencing to optimize the correspondence between the predicted heat transfer coefficient time history and the target heat transfer coefficient curve specified for Test LOC-11. The onset of critical heat flux (CHF) is predicted from current analyses to occur about 1/2 second later than desired. After 6 seconds, the comparison between target and calculation is good. Blowdown to a pressure of 0.34 megapascal occurs in 30 seconds. A sensitivity study to evaluate the influence of heat transfer correlations, code model assumptions, and valve sequencing is in progress.

A study was also completed to determine the probability of cladding failure due to cladding embrittlement during posttest quenching. Failure due to embrittlement is not expected to occur for any blowdown or blowdown/heatup test presently planned. A 20-second delay of the quench initiation will be required for a blowdown-only test to minimize the thermal shock on loop components. The effect of the delayed quench (i.e., additional heatup) is estimated to result in an additional oxide penetration of about 0.05 millimetre above the predicted 0.1 millimetre oxide for no delay.

1.2 Gap Conductance Test GC 2-2 Preliminary Results

D. T. Sparks and R. W. Garner

This section describes preliminary results of Gap Conductance Test 2-2 (GC 2-2). The test was completed on February 10, 1977. Test GC 2-2 is the second of a series of five tests designed to experimentally evaluate the power oscillation method for determining gap conductance and to determine the effects of initial gap size, fill gas composition, and fuel density on the thermal performance of an LWR fuel rod. The purpose of these tests is to assure that the oscillation method will correctly measure the gap conductance of a fresh or burned LWR fuel rod of varying initial gap size, fill gas composition, and fuel density^[2]. The test series was developed on the basis of a 3 x 3 fractional factorial design.

1.2.1 Experiment Design and Conduct. Test GC 2-2 was performed with four BWR-type fuel rods, each contained in a flow shroud (19.3 millimetre ID) and located within the PBF in-pile tube. Test fuel rod designations were GC 522-1, 522-2, 522-3, and 522-4. Test instrumentation was provided to (a) evaluate the relationship between PBF core power and test fuel rod power, (b) estimate steady state gap conductance values using both centerline and off-center internal pellet temperature measurements, and (c) estimate test fuel rod gap conductance values by the power oscillation method.

Test GC 2-2 was performed in essentially the same manner and under the same test conditions as Test GC 2-1^[3], with variations in fuel rod design as described in Reference 2. A major change in experiment design from Test GC 2-1 was reorientation of the cladding surface thermocouples at the axial flux peak with respect to the off-center fuel pellet thermocouples. For Test GC 2-2, the cladding surface thermocouples were oriented azimuthally 60 degrees from the off-center thermocouples.

Test GC 2-2 consisted of three parts. (a) steady state operation (power calibration) over a range of PBF core power levels during which each of the four test rod power densities was determined and calibration constants between the test rod power densities and core power were obtained, (b) a preconditioning period during which rod powers were cycled between a high and a low steady state power (from data obtained, fuel pellet cracking and rearrangement effects on gap conductance values will be determined), and (c) a power oscillation period during which the power was oscillated about several nominal power levels at a variety of oscillation magnitudes and periods.

1.2.2 Experiment Results. Steady state values for test rod power densities, fuel centerline and off-centerline temperatures, and cladding surface temperatures were obtained during all three periods of operation (power calibration, preconditioning, and prior to each power oscillation condition). The data obtained during the power calibration part of the test is presented in Table III-I. Data from the preconditioning period is presented in Table III-II.

Based on experience gained during the performance of Test GC 2-1, it was expected that considerably higher fuel temperatures would be observed in the xenon-filled rod (Rod GC 522-1) than in the other rods. In addition, because of the small (0.1-millimetre) initial diametral gap in the argon-filled rod (Rod GC 522-2), it was expected that the fuel temperatures, as a function of test rod power density, would be lower than the temperatures observed in the argon-filled rod during Test GC 2-1.

Figure III-1 is a composite plot of the fuel centerline temperatures of all four rods as a function of power density. The data in Figure III-1 cover all three periods of operation and illustrate the reproducibility of the centerline temperature measurements. The plots also show that indeed the xenon-filled rod is consistently the hottest rod and that the small gap in Rod GC 522-2 (the argon-filled rod) results in reduced centerline temperatures for this rod. Although helium has a high thermal conductivity compared with argon, the wide gap in Rod GC 522-4 resulted in higher centerline temperatures than in Rod GC 522-2. The data in Table III-I also indicate that at a power density of 51 kW/m during the power calibration, the centerline thermocouple in Rod GC 522-1 (xenon-filled, 0.23-millimetre gap) exceeded

TABLE III-I

PRELIMINARY ON-LINE STEADY STATE DATA FROM THE POWER CALIBRATION PHASE OF
GAP CONDUCTANCE TEST GC 2-2
539 K INLET TEMPERATURE

	Core Power (MW)	Rod Peak Power (kW/m)	Centerline Fuel Temperature (K)	Off-Center Fuel Temperature (K) [a]			Cladding Temperature (K)			Coolant Flow (cm ³ /s)	Coolant ΔT (K)
				0°	120°	240°	60°	180°	300°		
Rod GC 522-1	3.1	14.6	1716	1448	1455	1367	559	558	559	184	12.7
	4.7	21.6	1950	1554	1583	1469	561	558	560	295	12.0
	6.2	27.7	2129	1634	1666	1534	563	558	561	400	11.5
	7.9	34.2	2266	1774	1778	1649	565	558	564	497	11.5
	9.4	38.6	2337	1910	1904	1790	565	558	564	585	11.0
	10.9	45.0	2337	2294	2059	1983	570	558	567	679	11.1
	12.5	51.1	2610	2258	2170	2109	571	558	568	774	11.1
	9.4	39.3	3009[b]	2258	2170	2109	570	558	568	585	11.2
	6.2	26.2	2773[b]	1948	1918	1815	568	558	561	400	10.9
Rod GC 522-2	3.1	14.8	1052	946	873	854	559	559	560	201	11.9
	4.7	22.5	1215	998	945	923	559	559	562	331	11.2
	6.2	28.8	1343	1004	979	973	560	559	563	453	10.6
	7.9	36.5	1499	1019	1026	1033	561	560	564	560	10.9
	9.4	41.2	1635	1057	1067	1093	563	560	563	667	10.4
	10.9	45.5	1801	1144	1125	1186	564	564	563	772	9.9
	12.5	50.5	2017	1225	1199	1275	564	565	564	881	9.7
	9.4	40.2	1698	1128	1106	1144	564	563	[b]	661	10.2
	6.2	27.3	1406	1066	1064	1066	561	561	562	455	10.01
Rod GC 522-3	3.1	14.5	886	[c]	733	801	559	558	560	189	12.2
	4.7	21.4	1036	[c]	807	876	559	558	560	309	11.4
	6.2	27.6	1170	[c]	864	939	560	557	560	422	10.8
	7.9	34.6	1333	[c]	939	1007	561	558	562	523	11.0
	9.4	39.7	1470	[c]	1000	1068	562	559	563	617	10.8
	10.9	47.6	1667	[c]	1064	1153	566	562	566	724	11.1
	12.5	52.9	1862	[c]	1142	1221	569	563	568	819	10.9
	9.4	38.4	1490	[c]	1020	1094	563	560	563	617	10.5
	6.2	25.6	1162	[c]	874	957	561	558	561	418	10.2
Rod GC 522-4	3.1	12.2	1063	[d]	[d]	934	558	559	558	211	9.4
	4.7	17.4	1281	[d]	[d]	1058	560	559	557	334	8.6
	6.2	23.0	1484	[d]	[d]	1149	561	559	557	460	8.4
	7.9	28.5	1706	[d]	[d]	1198	564	560	558	567	8.4
	9.4	32.4	1864	[d]	[d]	1227	565	561	558	668	8.2
	10.9	37.5	2072	[d]	[d]	1266	569	563	560	781	8.1
	12.5	42.3	2178	[d]	[d]	1338	570	565	563	884	8.1
	9.4	32.1	1932	[d]	[d]	1331	567	561	559	668	8.1
	6.2	21.5	1463	[d]	[d]	1146	550	559	558	459	7.9

[a] Uncorrected for perturbation effects.

[b] Exceeds capability of thermocouple.

[c] Not operating properly.

[d] Rod GC 522-4 did not contain these thermocouples.

TABLE III-II

PRELIMINARY ON-LINE STEADY STATE DATA FROM THE PRECONDITIONING PHASE OF
GAP CONDUCTANCE TEST GC 2-2
539 K INLET TEMPERATURE

	Core Power (MW)	Rod Peak Power (kW/rod)	Centerline Fuel Temperature (K)	Off-Center Fuel Temperature (K) ^[a]			Cladding Temperature (K)			Coolant Flow (cm ³ /s)	Coolant ΔT (K)
				0°	120°	240°	60°	180°	300°		
Rod GC 522-1	3.1	15.0	1791	1701	1592	1473	558	557	557	256	9.6
	7.9	34.5	2577 ^[b]	2107	1886	1796	574	558	563	497	11.6
	3.1	14.5	1746	1638	1568	1542	561	557	558	252	9.4
	7.9	34.2	2754 ^[b]	2028	1851	1806	574	558	563	497	11.5
	3.1	14.3	1689	1619	1515	1550	551	557	558	257	9.1
	7.9	33.7	2498	2041	1792	1732	573	559	564	513	11.0
Rod GC 522-2	3.1	15.5	1079	938	907	895	560	558	557	289	8.8
	7.9	35.5	1559	1085	1093	1083	562	562	563	560	10.6
	3.1	15.0	1074	952	929	891	560	558	558	286	8.6
	7.9	35.4	1562	1080	1086	1104	562	563	563	566	10.5
	3.1	14.9	1079	910	926	891	559	558	558	290	8.4
	7.9	35.1	1562	1080	1086	1083	562	562	563	585	10.0
Rod GC 522-3	3.1	13.8	870	[c]	738	779	559	557	559	265	8.5
	7.9	33.1	1340	[c]	961	1077	561	560	562	523	10.6
	3.1	13.7	869	[c]	735	1076	559	557	559	263	8.5
	7.9	32.7	1340	[c]	963	1028	561	560	563	527	10.4
	3.1	13.8	870	[c]	735	778	559	557	559	267	8.4
	7.9	33.0	1338	[c]	961	1026	561	560	568	542	10.2
Rod GC 522-4	3.1	12.2	1042	[d]	[d]	906	559	558	556	290	6.9
	7.9	28.2	1712	[d]	[d]	1221	565	560	558	567	8.4
	3.1	10.9	1037	[d]	[d]	888	559	558	557	289	6.8
	7.9	28.3	1716	[d]	[d]	1212	566	560	559	573	8.3
	3.1	10.9	1035	[d]	[d]	772	560	559	557	267	6.7
	7.9	37.8	1701	[d]	[d]	1217	565	560	558	590	7.9

[a] Uncorrected for perturbation effects.

[b] Exceeds capability of thermocouple.

[c] Failed.

[d] Rod GC 522-4 did not contain these thermocouples.

Fig. III-1 Composite plot of fuel rod centerline temperatures as functions of rod power density throughout Test GC 2-2.

its reliable temperature limit of approximately 2600 K. The data obtained from this thermocouple throughout the remainder of the test are suspect, although at later times, and particularly at lower power levels, the data appeared to be of the expected magnitudes. Values that exceeded the limit of the thermocouple are not shown in Figure III-1.

Additional comparisons between data obtained for similar test rods during Tests GC 2-1 and GC 2-2 are shown in Figure III-2. The effect of initial gap width for helium-filled rods is shown in Figure III-2, where centerline temperatures for Rods GC 502 (0.1-millimetre gap) and GC 503 (0.23-millimetre gap) from Test GC 2-1 are compared with Rods GC 522-3 (0.1-millimetre gap) and GC 522-4 (0.35-millimetre gap) from Test GC 2-2. The reproducibility of centerline temperatures for Rods GC 502 and GC 522-3 is shown to be excellent.

During the oscillation portion of the test, some steady state on-line data were obtained on the four test rods at each of several power levels. At the various power densities, the internal and cladding surface temperature oscillations were observed closely in an attempt to detect changes in the waveshape that may be indicative of gap closure, and possibly gap closure and opening, as predicted by the FRAP-T2 calculations described in Reference 3. The first indication of a possible change in waveshape of any of the observed thermocouples occurred at a power density of 27.6 kW/m (oscillation amplitude of $\pm 20\%$, 60 seconds per cycle period) in Rod GC 522-3 on the cladding surface thermocouple at the

Fig. III-2 Comparison of fuel centerline temperatures for helium-filled rods used in Tests GC 2-1 and GC 2-2 as functions of test rod power showing effect of initial gap width.

60-degree orientation. Close observation of the cladding surface thermocouple indicates that a very slight change in waveshape may also have occurred on the cladding thermocouple at the 300-degree orientation at the same oscillation conditions. At these conditions, the thermocouples on the other three test rods were also observed, but no indications of waveshape changes were detected.

Detailed analysis of the data recorded on FM tape from Tests GC 2-1 and GC 2-2 will provide more information with respect to the lowest power density at which waveshape changes occurred. The occurrence of waveshape changes, which may be indicative of gap closure, depends on the heat transfer medium (fill gas) in the test rods and the initial gap width as well as the power density of the rod.

1.2.3 Conclusions. A large amount of on-line steady state and power oscillation data was obtained, from which preliminary estimates of test rod behavior and gap conductance values have been evaluated^[4].

The data from the four different design rods provide a wide variety of baseline data for comparison with data obtained from previous gap conductance tests (GC 1-3 and GC 2-1) and future tests (GC 2-3, 2-4, and 2-5). Preliminary comparisons of experiment data from Test GC 2-2 with data from Test GC 2-1 on similar design test rods show excellent reproducibility. Further analyses are required to compare the power oscillation and steady state gap conductance values and to evaluate the significance of cladding surface temperature oscillation waveshape changes.

1.3 Power-Cooling-Mismatch Test PCM-3 Postirradiation Examination Test Results S. L. Seiffert

The objective of Test PCM-3 was to observe post-DNB fuel behavior for fuel rods operating at typical PWR rod powers (51 kW/m). Maximum post-DNB cladding temperatures in the range from 1100 to 1240 K were sought so the crystalline structure of the zircaloy cladding would be in the ($\alpha + \beta$) two-phase transition region. The objective of the previous test, Test PCM-2, was to obtain maximum cladding temperatures from 810 to 1115 K so the cladding would remain in the α -phase^[5]. The preliminary test results of Test PCM-3 were reported previously^[6]. The ductility of zirconium cladding is a function of temperature (and associated crystalline structure), with a minimum ductility occurring in the $\alpha + \beta$ transition region.

1.3.1 Posttest Examination. Following PCM testing, the test shrouds were disassembled and four test fuel rods (UTA-0011, UTA-0013, A-0015, and A-0021) were visually examined. All four test rods remained intact during reactor operation and following reactor shutdown. Black oxide was present on the surface of all fuel rods. Rods A-0015 and A-0021 exhibited areas of spalled surface oxide and collapse (waisting) at fuel pellet interfaces. Bowing was observed along all four rods. Rods UTA-0011, A-0015, and A-0021 were chosen for destructive examination. Rod A-0021 fractured at one of the fuel pellet interfacial collapse regions 0.49 metre from the bottom of the rod prior to destructive examination.

(1) Metallographic Examination. The high temperature zone along the fuel rods was selected for metallographic examination. From thermocouple test data and supplementary test results for Rods A-0015 and A-0021, the maximum temperature region was determined to extend along the fuel rods between 0.43 and 0.76 metre, measured from the bottom of the fuel stack.

The fuel rods were epoxy encapsulated, sectioned, and mounted for metallographic examination. The metallographical samples were polished and etched (5 millilitres HNO_3 , 2 millilitres HF , 15 millilitres lactic acid, 5 millilitres H_2O), and the zircaloy cladding was examined for oxide and oxygen-stabilized alpha-zircaloy layer thicknesses and internal microstructure.

(2) Cladding Examination. Fuel Rod UTA-0011 was an instrumented rod with cladding surface thermocouples, a centerline thermocouple, and supplementary instrumentation. All the fuel rod samples sectioned for metallographic examination were taken from locations over the approximate region of high temperature test operation between 0.52 and 0.82 metre from the bottom of the fuel stack. The cladding microstructure present in these samples ranged from the as-received, stress-relieved zircaloy material, through stress-relieved and preequiaxed microstructures, to mixed preequiaxed and recrystallized alpha phase zircaloy microstructure.

The cladding microstructures observed in the maximum temperature zone for the samples from the other two fuel rods, A-0015 and A-0021, were prior beta phase material bounded on the outer and inner surfaces by layers (or bands) of oxygen-stabilized alpha phase zircaloy.

The fracturing in Rod A-0021 was examined metallographically. Prior beta phase material containing transformed structure was evident. The outer and inner surface bands of oxygen-stabilized alpha varied in development. Alpha-zircaloy incursions into the transformed prior beta phase material extended only a short distance toward the cladding center for both the outer and inner surface formations. The fracture surface at the end location of the cladding fragment was uneven.

The prior beta phase microstructure observed in the cladding at the opposite fracture surface was mottled and poorly developed. The transformed structure in this region adjacent to the fracture was granular in appearance. The outer surface layer of oxygen-stabilized alpha phase zircaloy was thicker than the inner surface layer, but alpha phase incursion into the prior beta phase material was limited at both the outer and inner surface. A surface layer of metal-water-reaction-formed oxide (ZrO_2) was present at both the outer and inner cladding surface.

(3) Cladding Temperature Estimates. Variations in surface oxide buildup (thickness) and zircaloy microstructure indicate the variety of temperatures experienced by the cladding at different locations along the fuel rod. Estimates of cladding surface temperature were made by two methods: (a) relating the combined thickness of ZrO_2 and alpha phase zircaloy layers on the outside surface of the cladding with the time and temperature of exposure and (b) evaluating the cladding microstructure.

Calculated temperatures, using Powel's equation^[7], were compared with calculated temperatures using Cathcart's correlation^[8] for the sum of ZrO_2 and $\alpha\text{-Zr}$ layer thickness.

Cladding temperature estimates based on microstructure were made using structural characteristics such as the relative amounts of alpha phase material, the coarseness of the transformed alpha zircaloy in prior beta phase zircaloy, the amount of distribution of oxygen-stabilized alpha phase zircaloy, the presence and size of prior beta phase zircaloy grains, and the relative development of transformed structure in the prior beta phase material.

The calculated temperatures were in agreement with temperatures estimated from microstructures. Cladding surface temperatures up to 1430 K have been calculated from the combined ZrO_2 plus $\alpha\text{-Zr}$ layer thickness measured in Rod A-0015, and cladding surface temperatures up to 1450 K have been calculated for Rod A-0021. These temperatures agree with the > 1240 K temperature determined from the cladding microstructure. Cladding microstructures present in Rod UTA-0011 indicate cladding temperatures < 1100 K below the temperature range desired during testing.

Thermocouple performance in Rod A-0021 was evaluated by comparing maximum test temperature thermocouple data with calculated temperatures for the same location. A maximum test temperature of 1080 K was recorded for the thermocouple located at the 0.58-metre, 180-degree orientation.

Temperatures calculated from the ZrO_2 plus alpha phase zircaloy layers were 1340, 1210, and 1360 K for angular orientations of 90, 180 and 270 degrees, respectively, for the transverse section from Rod A-0021 at 0.58 metre. Higher temperatures were found 90 to 180 degrees from the thermocouples weld attachments. A noticeable circumferential variation in the prior beta phase microstructure, observed in traversing from the cooler side adjacent to an attached surface thermocouple junction to the opposite, higher temperature side, verifies the existence of a circumferential temperature gradient. Such a temperature gradient indicates that the thermocouple sheath acts as a cooling fin in the vicinity of the surface junction attachment, reducing the cladding surface temperature.

Embrittlement of the cladding in the high temperature film boiling zone by internal and external oxidation from $\text{UO}_2\text{-Zr}$ and $\text{Zr-H}_2\text{O}$ reaction, respectively, was evaluated from the estimated cladding temperatures and test exposure time in film boiling using critical oxidation time criteria^[7,9]. The fuel rods were correctly predicted not to fail during the test. The reason Rod A-0021 fractured while handling during the postirradiation effects examination is not completely clear. The rod should not have failed. Supplementary examination of the fracture will be presented in a later report.

(4) Metallographic Examination. The metallographic examination results for fuel Rods UTA-0011, A-0015, and A-0021 are as follows:

(a) Surface Oxide Spalling and Cladding Collapse. The region of surface oxide spalling and cladding collapse was nearly the same for Rods A-0015 and A-0021, extending from 0.43 to 0.76 metre, as measured from the bottom of the fuel stack. Rod A-0015 exhibited a greater amount of surface oxidation and less severe cladding collapse at fuel pellet interfaces than did Rod A-0021. Rod A-0021 showed severe collapse at pellet interfaces and little or no surface reoxidation in the region of spalled surface oxide. (Rod A-0015 was initially prepressurized to 3.79 megapascals and Rod A-0021 to 2.59 megapascals.) The collapse in these rods occurred prior to significant surface oxidation. Rod UTA-0011 exhibited an uneven region of collapse extending between approximately 0.52 and 0.82 metre from the bottom of the fuel stack. No collapse into fuel pellet interfaces was observed.

(b) Cladding Microstructure. Cladding microstructure indicated that the high temperature zone of Rods A-0015 and A-0021 exceeded the $\alpha + \beta$ to β phase transformation temperature of about 1240 K. Bands of oxygen-stabilized α -Zr occurred on both the outside and inside of the cladding, bounding larger grains of prior beta phase material. Coarsening of the α -Zr incursions and martensitic transformed structures in the prior beta phase material occurred in the temperature gradient observed around the cladding circumference. The thermocouple sheaths acted as cooling fins, which reduced the cladding surface temperature near points of attachment to the cladding, resulting in circumferential and axial temperature gradients. The source of oxygen on the inside was from UO_2 fuel in contact with the cladding. Cladding microstructure examined in Rod UTA-0011 ranged from stress-relieved material to recrystallized alpha phase zircaloy microstructure associated with cladding temperatures below the $(\alpha + \beta)$ two-phase transformation temperature near 1100 K.

(c) Cladding Surface Temperature. Cladding surface temperatures up to 1450 K from Rod A-0021 and 1430 K for Rod A-0015 have been calculated from ZrO_2 and oxygen-stabilized alpha phase zircaloy layer thicknesses. These calculated temperatures are in good agreement with temperatures estimated from microstructure and confirm that these fuel rods operated considerably above the $(\alpha + \beta)$ two-phase to beta phase transition temperature. Rod UTA-0011 operated at temperatures below the $(\alpha + \beta)$ two-phase transition temperature near 1100 K. Comparison of temperatures calculated from Pawel's correlation and Cathcart's correlation, based on the combined layer thicknesses of ZrO_2 and α -Zr, indicated reasonable agreement.

(d) Oxygen Embrittlement Evaluation. Embrittlement of the cladding in the high temperature film boiling zone by internal and external oxidation from UO_2 -Zr reaction, respectively, correctly predicted that the four fuel rods should not fail during testing. The subsequent fracture of Rod A-0021 is being investigated.

1.4 Power-Cooling-Mismatch Test PCM-4 Test Results

G. B. Peeler and Z. R. Martinson

Preliminary results of Test PCM-4 were reported previously^[3].

1.4.1 Test Results. Tables III-III and III-IV present a summary of experiment results obtained during the preconditioning and DNB testing phase of operation.

1.4.2 Comparison of Calculated and Measured Fuel Rod Behavior. A comparison of the calculated and measured fuel rod behavior (cladding elongation, fuel centerline temperature, cladding surface temperature, and fuel rod internal pressure) determined during the steady state power calibration, preconditioning, and DNB cycle phases is discussed in the following subsections.

1.4.3 Power Calibration Phase. Steady state measurements were taken at 12 different fuel rod peak powers during the power calibration phase, from approximately 2 to 69 kW/m, as the fuel rod power was increased. The power was held constant for about 10 minutes at each step.

TABLE III-IV
SUMMARY OF DNB TEST RESULTS FOR TEST PCM-4

DNB Cycle	Fuel Rod	Fuel Rod Peak Power (kw/m)	DNB Occurrence	Coolant Mass Flux at Onset of DNB ₂ (kg/s·m ²)	Minimum Coolant Mass Flux (kg/s·m ²)	Time in Film Boiling ^[a] (s)	Time After DNB (s)	Maximum Indicated Cladding Surface Temperature (K)	Location of Maximum Cladding Temperature (m)	Maximum Indicated Fuel Centerline Temperature (K)	Cladding Elongation After DNB (%)	Increase in Rod Internal Pressure After DNB (MPa)
Fuel	UTA-0014	74	Yes	2091	2114	0	75	660	0.483	[c]	0.0	-
Rod	UTA-0015	70	Yes	2093	2049	0	45	729	0.635	[c]	0.090	-
Aging	UTA-0016	71	Yes	2091	2062	0	25	660	0.686	2225	0.031	-
	A-0017	73	Yes	2073	2039	0	45	[b]	[b]	[b]	0.051	-
	UTA-0014	71	No	[d]	1962	0	0	631	0.584	[c]	0	-
	UTA-0015	68	Yes	2043	1932	0	35	731	0.635	2000	0.052	-
	UTA-0016	68	No	[d]	1929	0	0	633	0.584	2350	0	-
	A-0017	70	Yes	2021	1906	0	245	[b]	[b]	[b]	0.027	-
2	UTA-0014	71	No	[d]	1952	0	0	632	0.584	[c]	0	-
	UTA-0015	68	Yes	1971	1899	0	140	703	0.635	2000	0.097	-
	UTA-0016	68	No	[d]	1906	0	0	633	0.584	2340	0	-
	A-0017	70	Yes	2021	1894	0	150	[b]	[b]	[b]	0.048	-
3	UTA-0014	61	No	[d]	1772	0	0	630	0.584	[c]	0	-
	UTA-0015	58	Yes	1777	1716	120	180	738	0.686	1720	0.009	-
	UTA-0016	59	No	[d]	1721	0	0	631	0.584	2110	0	-
	A-0017	60	No	[d]	1704	0	0	[b]	[b]	[b]	0	-
4	UTA-0014	71	Yes	1852	1658	115	160	930	0.584	[c]	0.116	0.24
	UTA-0015	67	Yes	2027	1610	160	280	1100	0.686	[c]	0.185	0.40
	UTA-0016	68	Yes	1848	1617	130	170	1005	0.584	2800	0.197	0.38
	A-0017	70	Yes	2044	1582	[b]	290	[b]	[b]	[b]	0.175	[b]

[a] Time that indicated cladding surface temperature exceeded 730 K.

[b] Fuel rod not instrumented.

[c] Instrumentation not functioning properly or erratic.

[d] Fuel rod did not indicate DNB.

TABLE III-III
APPROXIMATE STEADY STATE TEST RESULTS OBTAINED DURING
TEST PCM-4 PRECONDITIONING PHASE

Preconditioning Cycle	Fuel Rod	Fuel Rod Peak Power (kW/m)	Average Coolant ₂ Mass Flux (kg/s·m ²)	Coolant Inlet Temperature (K)	Coolant Temperature Rise (K)	Maximum Cladding Elongation (mm)	Change in Fuel Rod Internal Pressure (MPa)	Fuel Centerline Temperature (K)	Cladding Surface Temperature (K)					
									0.483-m 0°	0.504-m 90°	0.605-m 90°	0.606-m 180°	0.787-m 270°	0.889-m 270°
1	UTA-0014	44	2287	599.5	15.5	0.033	1.0	[a]	616	625	[b]	620	617	[b]
	UTA-0015	42	2200	599.5	15.0	0.033	2.91	1550	619	[b]	622	619	[b]	620
	UTA-0016	42	2241	599.5	15.0	0.050	1.87	1625	621	626	[b]	624	622	[b]
	A-0017	43	2206	599.5	15.4	[a]	[b]	[b]	[b]	[b]	[b]	[b]	[b]	[b]
2	UTA-0014	44	2241	599.5	13.7	0.023	0.84	[a]	615	624	[b]	620	617	[b]
	UTA-0015	42	2160	599.5	13.2	0.031	2.49	1560	619	[b]	622	619	[b]	620
	UTA-0016	42	2200	599.5	13.2	0.037	1.43	1640	621	626	[b]	624	621	[b]
	A-0017	43	2171	599.5	13.7	[a]	[b]	[b]	[b]	[b]	[b]	[b]	[b]	[b]
3	UTA-0014	44	2183	599.7	13.7	0.035	0.84	[a]	615	623	[b]	619	616	[b]
	UTA-0015	42	2091	599.7	13.5	0.031	2.50	1590	624	[b]	622	619	[b]	618
	UTA-0016	42	2131	599.7	13.6	0.025	1.44	1650	622	626	[b]	623	620	[b]
	A-0017	43	2102	599.7	14.0	[a]	[b]	[b]	[b]	[b]	[b]	[b]	[b]	[b]
4	UTA-0014	44	2166	599.7	15.0	0.027	1.0	[a]	615	620	[b]	619	617	[b]
	UTA-0015	42	2073	599.7	15.0	0.036	2.90	1600	619	[b]	623	620	[a]	619
	UTA-0016	42	2114	599.7	15.1	0.044	1.79	1660	622	627	[b]	624	622	[b]
	A-0017	43	2085	599.7	15.2	[a]	[b]	[b]	[b]	[b]	[b]	[b]	[b]	[b]

[a] Not functioning properly, erratic, or both.

[b] Fuel rod not instrumented.

(1) Cladding Surface Temperature. The measured cladding surface temperature data taken during the power calibration phase for the three instrumented fuel rods were compared with corresponding FRAP-T calculations. Generally speaking, there was good agreement between the measured and calculated values of the cladding surface temperature. Following the FRAP-T3 prediction of a change in heat transfer regime from forced convection to nucleate boiling, the calculated value of cladding surface temperature increased only slightly with increasing rod power, as would be expected. The cladding surface thermocouples continued to exhibit a linear dependence on local power above the FRAP-T3 predicted power level for transition from forced convection to nucleate boiling. This indicates a slight discrepancy in the prediction and occurrence of nucleate boiling at a given location, a perturbation in local fluid conditions due to the presence of the cladding surface thermocouples and instrument wiring, or both. However, the discrepancy is small and is within the experimental uncertainty. This trend has also been observed in all previous PCM tests during the forced convection to nucleate boiling transition and is not considered a modeling or experiment problem. Finally, the local power level at the prediction of the transition from forced convection to nucleate boiling decreased with increasing elevation for all the instrumented fuel rods, as would be expected.

(2) Fuel Centerline Temperature. Fuel centerline temperature measurements were made during the power calibration phase for the three instrumented rods. The fuel temperatures calculated with FRAP-T3 using the Ross and Stoute gap conductance model agreed very well with the measurements, considering that during the test it was discovered that the fuel centerline thermocouple leads had been reversed and an on-line correction made, which has not been analytically evaluated.

(3) Cladding Elongation. Cladding axial elongation during the power calibration was measured for all the fuel rods. The FRAP-T3 calculated cladding elongation was compared with the measured values. The measured cladding elongation during the power increase was greater than the calculated values for fuel rod average powers exceeding 20 kW/m. The cladding evidently elongated according to the zircaloy thermal expansion until the fuel and cladding came in hard contact. During the first nuclear operation of a fuel rod, the hard contact between the fuel and cladding usually occurs at a power considerably less than that calculated to close the gap by differential thermal expansion of the UO_2 and cladding. After the fuel and cladding come in hard contact, the cladding is forced to follow the greater thermal expansion of the fuel unless slippage occurs between the fuel stack and the cladding. Slippage and fuel creep at elevated temperatures allow the cladding to gradually shorten if the rod power is essentially constant. At power calibration steps corresponding to fuel rod average powers of approximately 25, 29, 35, 45, and 50 kW/m, the effect of fuel creep and the resultant decrease in cladding elongation were observed as a spread in the experiment data.

(4) Power Reduction. A comparison of transient fuel centerline measurements and calculations during a reactor scram that occurred during the power calibration phase was made. FRAP-T3 calculations were made using two different gap conductance models: (a) the annular gap conductance model developed by Ross and Stoute and (b) the cracked-pellet gap conductance model developed by MacDonald and Broughton. Since the

measurements were made during the power calibration phase, the fuel pellets should not have been cracked significantly. It was expected that the calculations, using the modified Ross and Stoute gap conductance model, and the measurements would agree; however, considering the fuel centerline thermocouple lead problem mentioned previously, it is uncertain as to which model correctly predicts the data.

1.4.4 Preconditioning Phase. Time-dependent measurements were taken during the four preconditioning phases; however, the power increases and decreases during the preconditioning phase were varied slowly enough that quasi-equilibrium conditions existed. As such, steady state comparisons of the calculated and measured parameters could be made. These comparisons are discussed in the following sections.

(1) Cladding Surface Temperatures. A comparison of the measured and calculated values of the cladding surface temperature for fuel Rod UTA-0016 during preconditioning Cycles 1 through 4 was made. Very little noticeable change in the cladding surface temperature was observed as a function of preconditioning cycle. Similar results were obtained for fuel Rods UTA-0014 and UTA-0015. There was good agreement between the measured and calculated values, and the trends during the transition from forced convection to nucleate boiling were similar to those presented in the power calibration phase.

(2) Fuel Centerline Temperature. A comparison of the measured and calculated values of the fuel centerline temperature for fuel Rod UTA-0016 during preconditioning Cycles 1 through 4 was made. Very little noticeable change in the fuel temperature was observed as a function of preconditioning cycle. There was good agreement between the measured and calculated values. Also, there was good agreement between the preconditioning and power calibration results.

(3) Cladding Elongation. A comparison of the measured and calculated values of cladding axial elongation for fuel Rod UTA-0016 during preconditioning Cycles 1 through 4 was made. This comparison indicated a noticeable change in cladding elongation as a function of preconditioning cycle. In particular, there was little change in cladding elongation behavior following the third preconditioning cycle. Similar results have been observed previously and indicate that most of the significant fuel cracking, relocation, and mechanical interaction occurs during the initial preconditioning cycles. The results for Rods UTA-0014 and UTA-0015 during preconditioning Cycle 4 were similar to those for Rod UTA-0016. Therefore, it can be concluded that all of the test rods had reached a state of equilibrium relative to power cycling (for a maximum fuel rod average power of ~ 30 kW/m) prior to the DNB testing phase.

Finally, good agreement was obtained between the measured and calculated values, especially after a state of equilibrium relative to power cycling (for a maximum fuel rod average power of ~ 30 kW/m) had been obtained. Once the power level exceeded that of the preconditioning cycles, there was a noticeable increase in the measured cladding elongation.

Furthermore, as the power increased above approximately 50 kW/m, there was a rapid increase in cladding elongation versus power, which was attributed to hard pellet-cladding interaction.

1.4.5 Onset of DNB. Calculations were performed with FRAP-T3 to determine the coolant mass-flux necessary to attain DNB for the measured test conditions and as-built dimensions for Rod UTA-0015 during the cladding surface aging phase and the four DNB cycles and for Rod UTA-0016 during the cladding surface aging phase and DNB Cycle 4. Westinghouse critical heat flux (CHF) correlation W-3^[10], in conjunction with the nonuniform axial heating factor and the cold-wall correction factor, and Babcock & Wilcox CHF correlation B&W-2^[11] were used to calculate test conditions to attain DNB for Rod UTA-0015. The W-3 correlation was used for Rod UTA-0016. In general, poor agreement between calculated and measured values was obtained.

1.4.6 Post-DNB Testing. DNB Cycle 4 was the only DNB cycle of the test in which the fuel and cladding temperatures were allowed to reach quasi-equilibrium values before reducing reactor power. FRAP-T3 calculations were made for the instrumented fuel rods. Considerable difficulty in predicting measured post-DNB parameters was experienced using the CHF correlations presently available in the FRAP-T3 code. As a result, the post-DNB calculations were performed by forcing FRAP-T3 to assume CHF conditions existed over the cladding collapse region. The magnitude of the calculated results compared favorably with the measured parameters, even though the calculated time interval of CHF did not agree with the measured time interval for all of the fuel rods.

(1) Cladding Surface Temperature. Posttest metallurgical examination will be required before final comparisons can be made with calculated cladding temperatures. On the basis of previous PCM test results, actual post-DNB maximum cladding surface temperatures are approximately 100 to 300 K higher than corresponding thermocouple measurements.

In general, considering the known difference between measured and actual maximum cladding temperatures, the calculated cladding temperature increase agrees well with the thermocouple-indicated temperature increase.

(2) Fuel Centerline Temperature. Comparisons of the calculated and measured fuel centerline temperature were not made, because the fuel centerline instrumentation had failed or was performing erratically during this portion of the test.

(3) Cladding Elongation. The agreement between the calculated and measured results for Rod UTA-0016 was much better than for Rods UTA-0014 and UTA-0015. This is because the correct CHF time interval was predicted for Rod UTA-0016.

(4) Cladding Collapse. The outside diameter of each fuel rod was measured to determine the amount and extent of cladding collapse. Measurements were made at orientations of 45 and 135 degrees. To simplify the comparison between calculated and

measured results, the measurements at the two angular locations were averaged. The FRAP-T3 calculations were forced into CHF over the visually determined region of CHF. As a result, the FRAP-T3 calculations agree with the measurements as to the axial extent of cladding collapse; however, the magnitude of the radial collapse is underpredicted. The agreement between calculated and measured results was better for Rod UTA-0015 than for Rod UTA-0016.

(5) Fuel Rod Internal Pressure. Due to an observed hysteresis effect and resultant zero shift, none of the internal pressure transducer measurements are considered valid for absolute pressure readings. However, the transducers are considered adequate to indicate relative changes in internal pressure after DNB occurred. Therefore, both the measured and calculated values were normalized to zero prior to the measured or calculated time of CHF occurrence. Since FRAP-T3 predicted higher cladding temperatures than were indicated by the thermocouple readings, it was expected that the calculated increase in internal pressure would also be larger than indicated. However, in general, the FRAP-T3 calculated values are less than the indicated values, even when the correct CHF time interval was predicted. This discrepancy has not been explained, although instrumentation response and modeling simplifications may be likely candidates.

1.4.7 Discussion and Conclusions. The objectives of Test PCM-4 were met satisfactorily, except that the desired cladding surface temperatures were exceeded. This is probably because each phase of the test was conducted at a higher power level than desired due to the error in the calibration of the coolant differential thermocouple pairs used to measure the coolant temperature rise and the error in the on-line thermal-hydraulic computer program used to calculate fuel rod powers. The in-pile fuel rod behavior data obtained during the test include information on fuel pellet-cladding interaction and its effect on cladding elongation, fuel centerline temperature, cladding surface temperature, and fuel rod internal pressure during normal and post-DNB operating conditions. In addition, FRAP-T3 calculations were compared with the measured fuel rod behavior. The instrumentation used to monitor the in-pile fuel rod behavior were evaluated. The conclusions derived from the test are discussed in further detail in the following paragraphs.

(1) Test Conditions at Onset of DNB. Although all of the fuel rods indicated DNB during the fuel rod cladding surface phase, it was concluded that this was not a premature DNB occurrence, since the surface aging had probably been obtained during the preconditioning phase. Fuel Rods UTA-0014 and UTA-0016 did not reach DNB conditions in any of the first three DNB cycles. The data indicate that the externally mounted cladding thermocouples did not alter the thermal-hydraulic conditions required to reach DNB. The measured conditions at the onset of DNB for the noninstrumented rod, A-0017, were essentially the same as those measured for the instrumented rod, UTA-0015.

(2) Post-DNB Fuel Behavior. Maximum post-DNB cladding temperatures in the range of 180 to 1255 K were sought. Based on the cladding thermocouple response,

including systematic cladding surface fin cooling effect, and preliminary postirradiation effects results, this objective was exceeded. Apparently all the fuel rods experienced cladding surface temperatures in the beta phase (temperatures > 1255 K).

All fuel rods reached DNB conditions during the fourth DNB cycle. Fuel Rods UTA-0014 and UTA-0016 reached DNB conditions at approximately 10% lower flow than that observed for fuel Rods UTA-0015 and A-0017 and at a flow rate less than the minimum flow rate observed in the first two DNB cycles. Although the minimum observed flow for fuel Rods UTA-0014 and UTA-0016 during DNB Cycle 3 was lower than that for DNB Cycle 4, the fuel rods did not reach DNB conditions, probably due to the reduced power in DNB Cycle 3 (60 kW/m versus 69 kW/m for the other DNB cycles).

1.5 Irradiation Effects Test IE-2 Test Results

C. M. Allison

Test IE-2, the fourth test in the Irradiation Effects (IE) Test Series, was conducted on October 7, 8, and 9, 1976. The test configuration, test conduct, and preliminary results were discussed previously^[3].

The objective of Test IE-2 was to investigate the effects of different initial diametral gap and cladding irradiation on fuel rod behavior during film boiling or power-cooling-mismatch (PCM) operation. The test used four 0.97-metre-long, PWR-type fuel rods which were designated IE-011, IE-012, IE-013, and IE-014. Rods IE-011 and IE-013 had small gaps of 0.10 millimetre and irradiated and unirradiated cladding, respectively. Rods IE-012 and IE-014 had large gaps of 0.34 millimetre and irradiated and unirradiated cladding, respectively. Each fuel rod was contained in its own coolant flow shroud.

The test consisted of four phases:

- (1) A preconditioning phase, in which the fuel rods were subjected to a series of power cycles with a peak power of 30 kW/m
- (2) A power ramp phase, where the fuel rods were increased in peak power from 24 kW/m up to 68 kW/m at a rate of 3 kW/m per minute
- (3) A high power phase, where the average rod peak power was held constant at 68 kW/m, individual rod mass flux was held constant at $4880 \text{ kg/s}\cdot\text{m}^2$, and coolant inlet temperature was held constant at 606 K for 1 hour

- (4) A flow reduction phase, where the surface heat transfer on the rods changed from nucleate boiling to stable film boiling following reduction of the inlet flowrate to induce a power-cooling-mismatch (PCM).

1.5.1 Presentation of Test Results. The experiment results presented in this section have been divided into three parts: pre-PCM data, PCM data, and results from a preliminary postirradiation visual examination. The pre-PCM data were taken from the preconditioning, power ramp, and high power phases, while the PCM data were taken from the transient following the DNB phase.

The pre-PCM data were taken from parts of the test (Ramps 1 through 5 plus the power ramp, as shown in Figure III-3), where the fuel rods were in thermodynamic equilibrium. The changes in rod power, inlet temperature, and flow rate were slow enough that any thermal lag in fuel rod temperature was negligible. The fuel rods were not in mechanical equilibrium due to slowly changing phenomena such as fuel creep and slip between fuel and cladding. This effect is less pronounced at fuel rod peak powers less than 30 kW/m but was quite evident during the power ramp up to and for 1 hour at 68 kW/m. For this reason, the mechanical data, such as cladding elongation, are more properly termed quasi-equilibrium.

Quasi-equilibrium cladding elongation data for Rods IE-011 and IE-012, are shown in Figure III-4^[a] in terms of change in cladding strain, $\epsilon - \epsilon_0$, where ϵ is the cladding strain during the test and ϵ_0 is the cladding strain at zero power, 606 K temperature, and time zero in Figure III-3. Strain has been calculated as

$$\epsilon - \epsilon_0 = \frac{\Delta L - \Delta L_0}{L} \quad (1)$$

where

ΔL = cladding growth from cold conditions at 300 K

ΔL_0 = cladding growth at zero power, etc.

L = cladding cold length (970 millimetres).

[a] The measurement was not available on Rod IE-013 or Rod IE-014 due to instrument failure.

Fig. III-3 Experiment conduct — inlet temperature, coolant flow rate, rod power, and reactor power for Test IE-2.

Fig. III-4 Change in cladding strain for Rods IE-011 and IE-013 during pre-PCM phase of Test IE-2.

The change in strain, $\epsilon - \epsilon_0$, versus power for Rod IE-011 (0.10-millimetre gap) was consistently greater than the $\epsilon - \epsilon_0$ of Rod IE-012 (0.34-millimetre gap) during both “soft” and “hard” pellet-cladding interaction^[a]. Hard pellet-cladding interaction resulting in an increase of the slope of strain versus power, was initiated at lower average rod powers on Rod IE-011 than on the other two rods. The differences in soft pellet-cladding interaction were also evident (see Figures III-5 and III-6). On Rod IE-011 for the first power ramp, $\epsilon - \epsilon_0$ increased at a slope equal to that which occurred during later cycles after significant pellet-cladding interaction occurred. Even during the later cycles, limited or soft pellet-cladding interaction occurred even at approximately zero power. A calculation of the slope of $\epsilon - \epsilon_0$ versus power indicated that at powers slightly above zero power, the slope was 150% of that expected by thermal expansion alone. There is no obvious occurrence of any pellet-cladding interaction on Rod IE-012 at powers less than 18 kW/m, even for the first power ramp.

Slight hysteresis-type behavior can also be seen between power increases and decreases. However, there do not appear to be any significant differences between the large- and small-gapped rods.

[a] “Hard” PCI, where lockup between the fuel and the cladding occurs, is referenced to “soft” PCI, where there are isolated points of interaction due to pellet fragments and misaligned pellets which do not result in lockup.

Fig. III-5 Change in cladding strain for Rod IE-011 during power cycles.

Fig. III-6 Change in cladding strain for Rod IE-012 during power cycles.

The differences in behavior as a function of gap size were also observed during the ramp to and hold at 68 kW/m (Figure III-7). Cladding strain on Rods IE-011 and IE-012 decreased prior to the termination of the ramp. The slope of $\epsilon - \epsilon_0$ versus power passed through zero at an average fuel rod power of 46 and 36 kW/m for Rods IE-001 and IE-012, respectively. The rod power at which the elongation rate becomes zero is the point where the fuel begins to creep and deform with additional power increases.

Fig. III-7 Normalized change in cladding strain for Rods IE-011 and IE-012 during power ramp and hold.

Rod internal pressure quasi-equilibrium data for all four rods are shown in Figure III-8. These data have been corrected for instrument drift. To minimize the uncertainties associated with the correction and the slight differences in the number of moles of fill gas, rod pressure has been presented in terms of fractional pressure change

$$\text{fractional pressure change} = \frac{P - P_0}{P_0} \quad (2)$$

where

P = rod internal pressure

P_0 = rod internal pressure at the start of the test;
time equal to zero on Figure III-3.

Fig. III-8 Fractional change in rod internal pressure for all four rods during pre-PCM phase of Test IE-2.

Effects of initial gap size are apparent for the rod pressure data. Rods IE-011 and IE-013 showed lesser increases in internal pressure as a function of power than did Rods IE-012 and IE-014. This occurs because the average gas temperatures on Rods IE-012 and IE-014 are higher as a result of having a larger fraction of the gas within the fuel-cladding gap.

No changes in fuel rod internal pressure were found due to continued cycling. Therefore, fuel cracking and other changes in the fuel due to thermal cycling had little effect on rod internal pressure. No significant changes in rod internal pressure were noted during the steady operation at 68 kW/m.

The PCM transient was initiated by a flow reduction at a constant power of 68 kW/m. Immediately prior to the change, all four rods were in nucleate boiling conditions. As the

flow decreased, DNB occurred, with the heat transfer going through a transition regime and reaching stable film boiling within 20 seconds. DNB was indicated by a large increase in cladding elongation, centerline temperature, rod internal pressure, and cladding surface temperatures (for part of the cladding thermocouple elevations).

After 1 minute in film boiling on Rod IE-011, the cladding surface temperature at the 0.61-metre elevation and 0-degree orientation on Rod IE-014 dropped sharply, indicating that the vapor layer collapsed over this part of the rod. The region of the vapor collapse must have been small, since no corresponding decrease in cladding elongation on this rod was seen. After 1-1/2 minutes of film boiling on Rod IE-011, the shutdown started where the rod power was decreased and the flow increased. Power was decreased at a rate of 1.4 kW/m per second from 60 kW/m down to 19 kW/m and at a rate of 0.2 kW/m per second down to 0 kW/M. None of the rods failed during or following the film boiling operation.

The effect of different initial diametral gap sizes on rod behavior was readily apparent during this transient. There were large differences in the cladding strain of the rods during the transition to and during film boiling. Rod IE-011 (0.10-millimetre gap) showed an increase of approximately 20 times that of Rod IE-012 (0.34-millimetre gap). Internal pressures of Rods IE-012 and IE-014 showed larger increases than Rods IE-011 and IE-013. Centerline temperatures cannot be compared, since the centerline thermocouple on Rod IE-011 was the only one to survive DNB.

1.5.2 Preliminary Postirradiation Examination Results. After the completion of Test IE-2, the test train hardware was disassembled in the PBF canal and the four rods, in their flow shrouds, were shipped to the hot cells. A visual examination of the rods indicated that all four rods were intact. The film boiling zones were visually distinguishable by cladding collapse into pellet-to-pellet interfaces, cladding oxidation, and oxide spalling. Detailed observations on each rod are presented below.

The film boiling zone on Rod IE-011 extended from the 0.546- to 0.679-metre location^[a]. Oxide spalling and cladding collapse into pellet-to-pellet interfaces occurred throughout the film boiling zone but were more pronounced on the 180-degree axis of the rod. The rod was intact, with no obvious cladding failures.

The film boiling zone on Rod IE-012 extended from the 0.542- to 0.678-metre rod elevations. The rod was intact, with no obvious cladding perforations. Cladding collapse into pellet-to-pellet interfaces occurred over the entire zone but was less pronounced than observed on Rod IE-011. Rod IE-012 contained one fuel pellet with a diameter of 8.113 millimetres, slightly smaller than the nominal diameter of 8.390 millimetres. Cladding

[a] All length measurements are referenced from the bottom of the fuel rod. The fuel column starts at 1.27 millimetres above the bottom of the rod.

collapse onto this pellet was clearly distinguishable at the 0.584- to 0.597-metre rod elevation. This elevation is approximately 7 millimetres higher than the elevation at which the pellet was loaded, which might indicate the axial growth of the fuel column below this pellet was 7 millimetres from cold conditions to film boiling conditions. Neutrographs of Rod IE-012 disclosed a 5-millimetre gap between pellets at the 0.567-metre elevation and a 2-millimetre gap due to a transverse crack in the fuel pellet immediately below the small diameter pellet. No definite indications of cladding collapse into these gaps were observed during the visual examination of the cladding. No oxide spalling occurred on the 0-degree axis of the rod. Black oxide was present in both the film boiling zone and the rest of the rod. Oxide spalling did occur on the 180-degree axis of the rod; however, bare zircaloy base metal was not exposed. Rather, a dark gray film was present on the spalled areas.

Rod IE-013 was intact, with no obvious cladding perforation. The film boiling zone extended from the 0.521- to 0.676-metre rod elevation. Cladding collapse into pellet-to-pellet interfaces, which occurred throughout the zone, appeared to be more pronounced on the 180-degree axis of the rod. In the film boiling zone, most of the oxide had spalled from the rod surface, exposing bare zircaloy base metal. The Type S thermocouples, which had a spaded junction brazed into a groove in the cladding, appeared to be in excellent condition. The junctions apparently remained firmly attached in the cladding groove.

Rod IE-014 was intact, with no obvious cladding perforations. The film boiling zone extended from the 0.505- to 0.676-metre rod elevation. Cladding collapse into pellet-to-pellet interfaces occurred throughout the zone, with the collapse more pronounced on the 180-degree side of the rod. Extensive oxide spalling occurred, exposing zircaloy base metal. All cladding surface thermocouples appeared to be in excellent condition and remained firmly attached in their grooves in the cladding.

For Rods IE-012, IE-013, and IE-014, the cladding uniformly collapsed throughout the film boiling zone. The diameter of Rod IE-011 may have increased slightly through the film boiling zone. As expected, the observed collapse strains for Rods IE-012 and IE-014 (initial diametral gaps of 0.34 millimetre) were greater than for Rods IE-011 and IE-013 (initial diametral gaps of 0.10 millimetre).

1.5.3 Discussion and Conclusions. Of the two parameters studied in the test, gap size and cladding irradiation, gap size was the dominant parameter. In general, several features were noted about the effects of gap size. The rods with larger gaps experienced the following:

- (1) Higher fuel temperatures because of low gap conductances
- (2) The onset of pellet-cladding interaction at higher rod powers because of the larger available volume for fuel expansion

- (3) Less total cladding elongation during both power cycling and film boiling because of reduced levels of pellet-cladding interaction
- (4) Increased relaxation of the cladding at high power due to fuel creep, slip, or both, because of the larger gap
- (5) Greater cladding collapse on the fuel during film boiling
- (6) Larger pressure increases with power increases due to larger number of moles of fill gas at increased gas temperature, and greater decrease in void volume following collapse during film boiling.

The effect of cladding irradiation was minimal based on the measurements that were made and the way the test was conducted. Cladding irradiation differences should not be noticeable on any of the measurements except for cladding elongation. Cladding elongation data may have been affected by the cladding mechanical properties due to neutron irradiation but due to instrument failure this comparison could not be made.

Neutron irradiation produces sizable increases in yield strength (over a factor of two for fluences of 10^{21}) but does not have an appreciable effect on elastic behavior of the cladding. At temperatures > 650 but < 700 K, irradiation effects will rapidly anneal out. Thus, the effect of cladding irradiation would be limited to test conditions where plastic deformation is expected for unirradiated cladding and cladding temperatures which remain below 650 K. The former condition was not the case for most of Test IE-2. During the power cycles, no plastic deformation was indicated in any of the four rods, up to the time of the high power ramp. Plastic deformation would be expected as a result of that ramp but since the power did not go to zero prior to film boiling, no measurements were obtained. Cladding temperatures remained well below 650 K (one of the welded thermocouples mounted in a groove on Rod IE-014 indicated 630 K during the hold at 68 kW/m, but this was well above the other measured temperatures), so that no significant annealing took place until film boiling occurred.

1.6 Irradiation Effects Test IE-3, Preliminary Postirradiation Examination Results

D. W. Croucher and S. A. Ploger

Irradiation Effects Test 3 (IE-3) was the third programmatic test of the Irradiation Effects Test Series. Preliminary results from this test were reported previously^[3]. This section summarizes the results of the preliminary postirradiation examination performed on the four preirradiated, PWR-type fuel rods from the Saxton Reactor tested in Test IE-3.

The test train was disassembled in the PBF canal, and the rods were shipped to the hot cells in their flow shrouds. The rods were withdrawn intact from their flow shrouds. The results of the visual and dimensional characterization examinations of the four rods are provided.

1.6.1 Visual Examination Results

(1) Rod IE-015. The film boiling zone on Rod IE-015 was characterized by oxide formations and oxide spalling and extended from the 0.489- to 0.692-metre rod elevation^[a]. Near the upper film boiling boundary, fuel relocation and breakup apparently occurred. The cladding collapsed into spaces between and around pellet fragments. Although an internal pressure transducer indicated a loss of cladding integrity during Test IE-3, no evidence of a cladding perforation was noted in this or any other region of the rod. No evidence of cladding collapse into pellet interfaces was observed outside the region of fuel relocation.

Most of the fuel relocation in Rods IE-015 and IE-016, discussed in the next section, probably occurred during adaptation of the fuel rods for testing in PBF. The previously irradiated Saxton fuel rods used for Rods IE-015 and IE-016 had fuel column lengths of approximately 0.69 metre. Fresh fuel pellets were added to the top of the fuel stack to extend the fuel column to the standard length used in the irradiation effects test series of approximately 0.89 metre. However, there appeared to be some relocation of the original fuel stack, which probably occurred during handling. As a result, less fresh fuel was added to extend the fuel stack to 0.89 metre than originally anticipated. This evidence suggests that at least part of the fuel relocation may have occurred in both Rods IE-015 and IE-016 during hot cell handling and prior to PBF testing.

In the film boiling zone, several surface appearances that could be correlated with oxide spalling were noted. Where no apparent oxide spalling had occurred, the oxide had a "silvery" appearance. Where partial oxide spalling had occurred, the remaining oxide layer had a dark gray color.

(2) Rod IE-016. The film boiling zone extended approximately between the 0.470- and 0.712-metre rod elevations. At the 0.67-metre elevation, near the top of the previously irradiated fuel column, the cladding had collapsed into apparently wide gaps between fuel pellets. Cladding collapse also occurred into a wide gap between fuel pellets at about the 0.61-metre rod elevation. As previously discussed, fuel relocation and the resultant gaps between pellets probably occurred during hot cell handling and adaptation of the rod for PBF testing. No evidence of cladding perforation was observed along the length of the rod. Other than those regions where fuel relocation occurred, cladding collapse into pellet interfaces was not observed.

[a] Distances were measured from the bottom of the rod.

(3) Rod IE-017. The film boiling zone appeared to extend between the 0.460- and 0.680-metre rod elevations. Cladding collapse into pellet interfaces was noted at several locations at the 0-degree rod orientation but was not noted at the 180-degree orientation. No cladding perforations were noted at any location along the rod.

(4) Rod IE-018. The film boiling zone appeared to extend between the 0.452- and 0.670-metre elevations. The thermocouples brazed to the cladding surface appeared to be in excellent condition and remained firmly attached to the cladding surface.

Rod IE-018 had a cosine-shaped bow extending in the 270-degree direction. A bend also occurred in the rod in the 180-degree direction at about the 0.81-metre elevation. Wear marks were found on the cladding surface at the 0.62-metre rod elevation, indicating that the rod was in contact with the shroud. The appearance of the wear spot and absence of any apparent local heating would suggest that the bowing and rod-shroud contact occurred after termination of the test. The wear probably occurred during posttest loop operation.

1.6.2 Posttest Diameter Measurement. Diameter measurements were made on the four rods at two perpendicular orientations. Measurements were taken with a micrometer at 50-millimetre increments outside the film boiling zone and at 12-millimetre increments within the film boiling zone.

The measurements indicated significant swelling through the film boiling zones of Rods IE-015, IE-017, and IE-018 similar to the swelling observed on the rods from Test IE-1^[12], in which previously irradiated rods were also tested. The swelling has been hypothesized to be due to fuel swelling induced by fission gas bubbles migrating and coalescing at grain boundaries and due to the volume change in fuel melting. It is not clear from the diameter measurements whether swelling or collapse, or both, occurred on Rod IE-016.

1.6.3 Discussion. Fuel rod behavior during a power ramp and fuel swelling during film boiling were of particular interest in Test IE-3. The rods were tested at a ramp rate of 21 kW/m-min between rod average powers of 22 and 61 kW/m. This was the highest ramp rate used in any test of the irradiation effects test series. The irradiated rods had burnups ranging from 8550 to 15,860 MWd/t, similar to the rods tested in Test IE-1.

It is concluded that, despite the irradiation damaged state of the cladding, the fuel rods operated without producing a pellet-cladding interaction failure during the rapid power ramp. The cladding swelling observed on Rods IE-015, IE-017, and IE-018 was 1.6, 2.1, and 4.4%, respectively. It is not clear from the diameter measurements whether swelling or collapse, or both, occurred on Rod IE-016. The measured swelling is thought to be due to a combination of fission gas bubble migration and coalescence, fuel thermal expansion, and the volume change on fuel melting. Cladding swelling observed in Test IE-1 was similar in magnitude to the swelling in the Test IE-3 rods.

No gross fuel rod failures were observed. The integrity of the cladding on Rod IE-015 was lost, while the remaining three rods remained intact. Fission product release into the test loop coolant was small, so the failure was judged to be a pinhole leak. Preliminary postirradiation examination could not detect the failure.

1.7 Irradiation Effects Test IE-5, Preliminary Test Results

D. W. Croucher, S. A. Ploger, A. S. Mehner

Test IE-5, the fourth programmatic test conducted in the irradiation effects^[17] test series, was completed January 26, 1977. The objectives of Test IE-5 were to provide steady state, power ramp, and film boiling operation behavior data on fuel rods constructed from irradiated and unirradiated cladding and to investigate the influence of simulated fission products on cladding integrity and performance. In addition, cladding ballooning phenomena were investigated by testing a rod having an internal pressure greater than the coolant pressure during steady state and film boiling operation. Preliminary results obtained from Test IE-5 instrumentation and postirradiation examinations are reviewed in this section.

1.7.1 Test Configuration. Test IE-5 was performed with four PWR-type fuel rods designated IE-019, IE-020, IE-021, and IE-022. Rods IE-019 and IE-020 were made from zircaloy-4 cladding tubes used to fill the open lattice positions in the extended burnup tests in the Saxton reactor. Cladding used for Rod IE-021 was taken from a fuel rod irradiated in the Saxton reactor to an average burnup of 7930 MWd/t. Rod IE-022 was manufactured from unirradiated zircaloy cladding. The nominal cladding outer diameter for all rods was 9.93 millimetres.

All four rods were filled with unirradiated 12.5 wt% enriched, UO_2 fuel pellets of a dished-end design and 94% of theoretical density. Each third pellet in the fuel stack of Rods IE-020, IE-021, and IE-022 contained chemical additions to simulate oxygen activity and accumulations of cesium, tellurium, and iodine fission products in fuel with a burnup of about 18,000 MWd/t. The nominal fuel-cladding gap in Rods IE-019, IE-020, and IE-022 was 0.09 millimetre (the smallest of the irradiation effects test series) and 0.14 millimetre in Rod IE-021. The fuel stack length for all rods was nominally 0.88 metre. Rod IE-019 was pressurized at room temperature to 8.3 megapascals with a 77.7% helium, 22.3% argon gas mixture. The remaining rods were pressurized with the same mixture to 2.6 megapascals at room temperature prior to the test.

Each rod was instrumented with devices to measure fuel centerline temperature, internal pressure, fuel rod elongation, and cladding surface temperature. The rods were located inside separate instrumented flow shrouds within the PBF in-pile tube. A turbine flowmeter at the base of each shroud to measure the fuel rod coolant flow rate and thermocouples mounted on each shroud to measure the coolant temperature rise provided the means for thermal-hydraulic calculation of each fuel rod power.

1.7.2 Test Conduct and Results. Test IE-5, conducted as shown in Figure III-9[18], included (a) a preconditioning period consisting of power calibration and gap conductance testing cycles, (b) a power increase, and (c) a flow reduction to induce film boiling on the rods.

Fig. III-9 Nominal operating conditions during Test IE-5.

(1) Preconditioning Period. The first series of power calibration cycles consisted of 16 steps at all possible combinations of two levels of flow rate (nominally 505 and 760 cm³/s), inlet temperature (nominally 583 and 606 K), and average fuel rod peak linear heat rating (nominally 30 and 39 kW/m). The ratio of each fuel rod power to the average power of all four rods was 0.94, 1.03, 0.97, and 1.07 for Rods IE-019, IE-020, IE-021, and IE-022, respectively. The gap conductance segment of the preconditioning period consisted of

power oscillations of $\pm 20\%$ at 15 and 25 kW/m for two cycles at a flow rate of $380 \text{ cm}^3/\text{s}$ and an inlet temperature of 555 K. Following the gap conductance testing, two additional cycles of preconditioning were completed. This period consisted of two power ramps from zero to 34 kW/m and back to zero at a nominal flow rate of $1000 \text{ cm}^3/\text{s}$. The ramp rate during this phase of the test was 0.3 kW/m-min and the inlet temperature was held at a nominal value of 606 K.

(2) Power Increases. Following the preconditioning period, the fuel rod power was increased at a ramp rate of 3.4 kW/m-min to provide an average fuel rod power of 67 kW/m. Once steady state was obtained at this power level, a calculation of fuel rod power using coolant flow and temperature differential indicated that the fuel rods had peak powers of 62.3, 68.6, 64.3, and 71.2 kW/m for Rods IE-019, IE-020, IE-021, and IE-022, respectively. The fuel rod power, coolant inlet temperature, and coolant flow were held constant for approximately 1 hour.

(3) Flow Reduction. The flow was reduced in $50 \text{ cm}^3/\text{s}$ steps of 1 minute duration. At a flow rate of $380 \text{ cm}^3/\text{s}$, an increase in rod elongation on Rod IE-022 indicated the onset of film boiling. A subsequent flow reduction to $260 \text{ cm}^3/\text{s}$ produced film boiling on the remaining rods. Fifteen seconds later, a final step flow reduction to $220 \text{ cm}^3/\text{s}$ was performed. The flow was held constant for about 1 minute, after which the reactor was shut down.

Preliminary data during the flow reduction portion of the test for Rod IE-019, typical of the other rods, are shown in Figure III-10^[18]. The onset of film boiling was determined with good agreement between the rod instruments. The decrease in fuel centerline temperature during the film boiling transient on Rod IE-019 was most likely due to shunting of the thermocouple wires in a lower temperature region of the fuel stack. The temperature (2460 K) indicated by this device at the time of malfunction was at the upper limit of its temperature measurement capability.

The fuel rod internal pressure data on Rod IE-019 indicated a rod leak 5 to 10 seconds after film boiling was initiated on the fuel rod. The reduction in fuel rod internal pressure which occurred just after DNB and prior to the rod leak on Rod IE-019 was probably a result of cladding ballooning. Coincident with the pressure decrease, there was a reduction in the rate of rod growth and an apparent collapse of the vapor film at the 0.645-metre elevation^[a], as indicated by the cladding surface temperature data. The reduction in rod growth rate and decrease in cladding surface temperature very likely resulted from a rewetting of the cladding surface as the heat flux at the ballooned section of the fuel rod was temporarily reduced as the fuel-cladding gap increased. Following this

[a] All distances were measured from the bottom of the fuel rod.

Fig. III-10 Rod IE-019 strip chart data showing data traces for fuel rod internal pressure, cladding elongation, cladding surface temperature, fuel centerline temperature, and coolant flow rate during the flow reduction portion of Test IE-5.

event, there was a step reduction in the fuel rod internal pressure to the coolant system pressure and with the recurrence of film boiling at the 0.645-metre thermocouple location. As these events progressed, the increasing fuel rod elongation data indicated that film boiling continued to propagate axially along the fuel stack. The coolant flow data did not indicate that ballooning of the fuel rod was sufficient to reduce the flow through the flow shroud (the fuel rod outside diameter was 9.93 millimetres and the flow shroud inside diameter was 16.3 millimetres).

The fuel rod pressure data for Rods IE-020, IF-021, and IE-022 indicated that the rods did not fail during the approximate 1 minute of film boiling or on initial quench as the reactor was shut down. Approximately 1.5 minutes after reactor shutdown, the fuel rod internal pressure on Rod IE-022, the highest power fuel rod and first rod to exceed CHF, indicated a sharp increase. Simultaneous with this pressure increase was a 0.4-millimetre increase in cladding length. These increases are indications of cladding failure.

The insertion of simulated fission products into fuel rods did not enhance the cladding stress corrosion sufficiently to cause cladding failure due to pellet-cladding interactions during the test. The effect of these fission products on the cladding during the 32 hours of nuclear operation will be assessed during the postirradiation examination of the fuel rods.

1.7.3 Preliminary Postirradiation Examinations. Preliminary postirradiation examination on Rods IE-019, IE-020, IE-021, and IE-022 from Test IE-5 has been completed. The test train was disassembled in the PBF canal. The four rods were shipped in their respective flow shrouds to the hot cells and were withdrawn from the channels intact. Visual examinations and diametral measurements of the rods are discussed below.

(1) Rod IE-019. Rod IE-019 was initially pressurized to 8.3 megapascals to cause cladding ballooning during the film boiling operation period of the test. The examination showed that the rod did increase in diameter in the film boiling zone, with maximum diameter increase occurring just below the thermocouples located at the 0.645-metre elevation. No evidence of a cladding burst or failure was observed in the ballooned region of the rod. Loss of rod integrity, as indicated by the abrupt pressure change during film boiling operation, apparently occurred outside the film boiling zone; however, no failure location was found. No evidence of cladding collapse was found. During dimensional characterization, the rod fractured in the region of maximum ballooning. Fuel melting occurred almost out to the outer surface of the pellet, and in one isolated location, molten fuel appeared to have solidified on the surface of a pellet. Cladding ballooning was not uniform around the circumference, but appeared to be maximum in the plane of the cladding surface thermocouples (45 to 225 degrees).

The film boiling zone was characterized by surface oxide formation and oxide spalling extending between the 0.462- and 0.680-metre elevations. Nearly all the oxide spalled to

expose base metal between the 0.462- and 0.532-metre elevations on the 0-degree orientation of the rod and between the 0.462- and 0.525-metre elevations on the 180-degree orientation of the rod. From about the 0.532- to the 0.632-metre elevation, the oxide had a "silvery" appearance. In this region, small areas of the silvery appearing oxide had spalled to expose a gray inner layer of oxide.

Rod IE-019 had two thermocouples brazed to the rod surface at the 0.645-metre elevation. The thermocouples appear to be in excellent condition and firmly attached to the rod. Only the thermocouple at the 45-degree orientation responded to film boiling.

(2) Rod IE-020. Examination of Rod IE-020 revealed that cladding collapse into pellet interfaces was clearly evident. Instrumentation during the test did not indicate cladding failure, and no evidence of failure was found during visual examination.

Rod IE-020 had two thermocouples brazed to the cladding surface at the 0.643-metre elevation. The thermocouple at the 45-degree orientation had fractured just above the junction and had failed to operate throughout the test. The thermocouple at the 225-degree orientation operated well during the test and was intact.

(3) Rod IE-021. On Rod IE-021, cladding collapse had occurred into pellet interfaces on the film boiling zone. Instrumentation during the test did not indicate cladding failure, and no evidence of failure was observed during visual examination.

Rod IE-021 also had two thermocouples brazed to the surface of the cladding at the 0.643-metre elevation. Both of the thermocouples were detached from the rod. One failed to operate during the entire test; the second operated erratically during film boiling.

(4) Rod IE-022. Following the test, an internal pressure increase was detected, indicating rod failure. Rod IE-022 was intact with no obvious cladding perforations. The most likely region of failure for this rod would be in the groove in the cladding where the thermocouples were attached.

Four spaded cladding surface thermocouples were brazed and then laser-welded in grooves in the cladding. The thermocouples remained firmly attached in the grooves in the cladding. One thermocouple had fractured about 60 millimetres above the junction location, apparently during the ramp to high power. Only one of the remaining three thermocouples measured temperatures during film boiling.

(5) Diametral Measurements. Diametral measurements were made along the length of each rod at two perpendicular orientations. Measurements were made with a micrometer at 50-millimetre increments above the film boiling zone and at 12-millimetre increments within the film boiling zone.

Rod IE-019 was ballooned through the film boiling zone, with a maximum balloon of approximately 26% occurring in the 45- to 225-degree plane at about the 0.632-metre elevation. It appeared that ballooning and, thus, film boiling extended between the 0.450- and 0.860-metre elevations.

Rods IE-020, IE-021, and IE-022 showed indications of diametral increases through the film boiling zone and cladding collapse onto the fuel column and into pellet interfaces. Collapse of the cladding probably occurred at the onset of film boiling. Subsequent rod diameter increases were probably due to the volume change upon fuel melting and the small initial, as-fabricated gap.

1.7.4 Discussion. Although the performance of the cladding surface thermocouples was marginal, Test IE-5 was completed successfully, with no apparent loss of critical instrumentation.

The power ramp produced substantial fuel-cladding interaction. Although simulated fission products were present in Rods IE-020, IE-021, and IE-022, and all rods except IE-022 had been fabricated from irradiated cladding, no pellet-cladding interaction failure occurred.

Two fuel rod failures occurred during the test. The pressure transducer on Rod IE-019 indicated cladding ballooning and failure just after the initiation of film boiling. Flow measurements indicated that the ballooning was insufficient to reduce the flow rate. Rod IE-022 failed approximately 90 seconds after reactor shutdown. Pinhole cladding failures were suspected for both rods, since the loop fission product detection system did not respond until depressurization of the in-pile loop. Postirradiation examination to date has not yet found the failure location on either rod.

2. PROGRAM DEVELOPMENT AND EVALUATION

P. E. MacDonald and W. J. Quapp

Efforts were concentrated in the areas of PBF program development, coordination with foreign experimental programs, Nuclear Regulatory Commission (NRC) technical assistance, analysis of PBF test results, Halden fuel behavior research, and postirradiation examination of commercial power reactor fuel. The scope and objectives of these efforts are described in Reference 19.

This section contains a summary of the various thermal fuels behavior development and evaluation activities (Section 2.1); and results from the postirradiation examination of the mixed-oxide test rods irradiated in the Halden instrumented fuel assembly (IFA) 226 (Section 2.2).

2.1 Summary of Ongoing Efforts

Technical liaison with the Italian Government was initiated with discussions in Ispra and Rome concerning the use of the ESSOR reactor (a test reactor at the Ispra Research Center) for LOCA testing with bundled rods. A detailed review of the FLECHT and Semiscale forced feed and gravity feed reflood heat transfer experiments was performed. Parametric effects of specific operating parameters were evaluated, and those parameters which most significantly influence LOCA reflood conditions were identified. Reflood experiment requirements for fuel behavior tests in the SARA loop of ESSOR were specified.

As part of the short-term NRC technical assistance efforts, a study to predict and compare the surface temperature response of Semiscale electrically heated rods to LOFT nuclear rods during a Semiscale blowdown was completed. Core thermal-hydraulic calculations, using RELAP4, were made for each type of rod. The results of these calculations were used as coolant boundary conditions for nuclear fuel rod predictions with FRAP-T. Both the RELAP4 and FRAP-T calculations indicated that the nuclear rods may heat up more rapidly than electrical rods. The nuclear rod peak temperatures were equal to or greater than the electrical rod, depending on the cold-state internal pressure of the nuclear rod.

The effects of irradiation and reactor service on fuel rod integrity during transient power-cooling-mismatch (PCM) conditions are being studied in PBF by in-reactor tests of previously irradiated fuel rods. A revised Irradiation Effects (IE) Experiment Requirements Document was issued. The document describes program planning for the study of irradiation effects on fuel rod integrity. These effects and previous PCM experience are discussed and a slightly revised test plan is presented, based on USNRC and German review comments.

SPERT and NSRR data were analyzed to evaluate fuel failure modes and the effect of energy deposition for the cold-startup conditions used during these tests. Failure modes and thresholds were defined as a function of energy deposition at the pellet surface. Effects of fuel rod design parameters, irradiation history, and water-logging on fuel rod thermal and mechanical behavior were evaluated. Presently, FRAP-T3 analyses are being performed for the SPERT, NSRR, and BWR-6 fuel rods for RIA energy insertions of 200 to 300 cal/g. After these analyses have been performed, the presently planned RIA experimental program will be reviewed and recommendations made concerning both planned and future RIA tests.

A study was made of all the PCM tests conducted to date in terms of "what was learned/confirmed." The areas covered include pre-CHF (critical heat flux) behavior, thermal-hydraulic boundary conditions, integral fuel rod thermal and mechanical behavior, zircaloy/ UO_2 metallurgical behavior, and evaluation of relationship of PBF test results to light water reactor fuel behavior. Reactor operating history plots were prepared for Test PCM-3 and all previous PCM tests.

Collection of fuel and fuel rod property data continues from experiments in the Halden Test Program. The postirradiation examination of the IFA-226 mixed-oxide experiment was completed at the Research Establishment RISO in Denmark during this quarter. The results of the examination are presented in Section 2.2 of this report. Test data from the IFA-226 experiment (to 30,000 MWd/t) were analyzed and compared with computer code calculations^[20].

The IFA-429 fission gas release and helium sorption experiment, currently in the reactor at Halden, Norway, continued to operate to an average burnup of approximately 9000 MWd/t and to produce test data on fuel temperatures, rod pressures, and rod powers. Measured fuel rod temperatures of approximately 1373 K and measured pressures of approximately 5.4 megapascals were as expected^[21].

The upcoming axial gas flow and fuel conductivity experiment (IFA-430) is scheduled to be tested and reactor-loaded in the summer of 1977. During the last quarter the fuel pellets for this experiment, which had been ultrasonically drilled for temperature instrumentation, were shipped from the INEL to the Halden Project in Norway. Fabrication of experiment hardware continued.

A study was completed on the feasibility of using an on-line thermocouple decalibration system. The system proposed would entail the use of a miniprocessing computer to constantly correct thermocouple signals for measured decalibrations due to the reactor environment.

The commercial power reactor postirradiation effects (PIE) program continued in several areas. Two boiling water reactor (BWR) bundles from Peach Bottom 2 had been identified by visual, ultrasonic, and eddy current testing, as containing pellet-cladding interaction fuel failures of particular interest. Shipment of these bundles from Peach Bottom to INEL was initiated during this quarter.

A conceptual design of a gamma scanner to accommodate full-length commercial fuel rods was completed. Fabrication of a pulsed eddy current scanner for commercial rods was initiated.

Axial gas flow experiments using irradiated pressurized water reactor (PWR) fuel rods were completed during fiscal year 1976. Analyses were performed this quarter to compare the effective gaps measured metallographically with those calculated from measured gas flow rates. The results will be used to improve the gas flow models employed in various fuel performance codes.

2.2 IFA-226 Postirradiation Examination Results

2.2.1 Introduction. Analysis of fuel behavior during an accident depends critically on knowledge of fuel behavior during normal (steady state) operation. The temperature

distributions of the fuel pellets, the internal gas pressure and composition, the amount of restructuring and cracking of the pellets, and the mechanical strains and irradiation damage in the cladding are of interest. All of these variables are affected by the rod design variables at beginning-of-life and, subsequently, by the degree of burnup and the power history.

Data were obtained and analyzed during the steady state irradiation of mixed-oxide fuel rods in the IFA-226 in the Halden Boiling Water Reactor (HBWR) in Halden, Norway. Cladding elongation, fuel centerline temperature, and rod internal pressure were measured as functions of power and time for rods with various initial pellet-to-cladding gap sizes and fuel densities. The purpose of the IFA-226 experiment was to facilitate development of analytical models describing

- (1) Fuel pellet-cladding mechanical interactions as a function of power, burnup, fuel density, and initial pellet-cladding diametral gap
- (2) Fuel density, pellet-cladding gap, and temperature effects on fission gas release and rod internal pressure
- (3) Pellet-to-cladding gap, fuel density, and irradiation effects on the temperature of mixed-oxide fuels
- (4) Fuel rod axial strains as functions of power and burnup.

IFA-226 was designed, fabricated, and initially irradiated in the HBWR by Nuclear Fuel Services, Incorporated (NFS) of Rockville, Maryland. In 1974, IFA-226 and all of the relevant fabrication and irradiation data were purchased from NFS by Aerojet Nuclear Company (work now being performed by EG&G, Idaho, Inc.). The subsequent irradiation and postirradiation examinations of this assembly have been directed by EG&G Idaho, Inc., as part of the USNRC's participation in the OECD^[a] Halden Reactor Project.

Results from the postirradiation examination of the IFA-226 rods are presented in this section. The examinations were performed at the hot cells at the Research Establishment RISO in Roskilde, Denmark.

2.2.2 Assembly and Rod Data. IFA-226 consisted of a vertically stacked train of two clusters of six mixed-oxide fuel rods each. Each cluster was about 915 millimetres in length and 70 millimetres in diameter. The rods within each cluster were arranged in a hexagonal lattice. A diagram of IFA-226 is presented in Figure III-11.

[a] Organization for Economic and Cooperative Development.

Fig. III-11 Diagram of Instrumented Fuel Assembly 226.

The test fuel was fabricated from recycled plutonium oxide powder, which was mechanically mixed with depleted (0.20 wt% U-235) uranium oxide powder. This mixed-oxide material (9.5 wt% PuO₂) was pressed, sintered, and ground into pellet form and clad with zircaloy-4. The final pellet and rod dimensions, except for length, are typical of those of PWR reload fuel. The fuel rod data are shown in Table III-V. All fabrication data are given in Reference 19.

2.2.3 Profilometry. Profilometry was performed on all 12 rods along six generating lines. The probe setup for profilometry has sapphire rods of 1-millimetre diameter, coupled to a differential transformer; the contact load of the sapphire is 35 grams. The differential transformer was calibrated against polished, stainless steel standards before each rod measurement. The accuracy of the setup was earlier determined on polished, stainless steel to be 0.5 to 1 micrometre. The measurements were taken digitally at locations 0.3 millimetre apart and recorded on magnetic tape. The results are shown in Table III-VI.

2.2.4 Gamma Scanning. The gamma scanning was performed on 11 of the 12 rods with a nominally 0.5-millimetre tungsten collimator, giving an effective slit width of 0.54 millimetre. The rods were run past the slit by a stepping motor with a speed, when averaged over a run and a stop period, of 1.2 millimetres per minute. The radiation of the rod was detected by a nitrogen-cooled Ge/Li crystal. Each of the isotopes, Ru-106/Rh, Cs-132, Zr-95/Nb, and Ce-144/Pr, at energies of 512, 662, 756 to 765, and 696 keV, were registered by two single-channel analyzers. The first one was set on the photo peak, the second on the background before the peak. The gross activity was registered by a single-channel analyzer. The results are shown in Tables III-VII, III-VIII, and III-IX.

2.2.5 Piercing and Gas Analysis. The rods were pierced in the plenum area close to the top end plug. The piercing area was sealed by an O-ring, and a mechanical penetration was made. The fission and filler gases were extracted and their volume determined.

After completing extraction, the rod was backfilled with helium at a pressure of 1 atmosphere. The helium was extracted and the free volume of the rod thereby measured. As the dead volume of the piercing head is less than 0.5 cm³, the accuracy of the free volume determination is better than 2%.

The pressure in the rod (in hot-cell condition) was calculated on this basis. The accuracy of this is better than 2.5%, corresponding to one standard deviation.

Gas from each rod was filled into four glass bulbs, of which two were used for the mass-spectrometric analysis and two for gas-chromatography.

Through mass-spectrometric analysis, the relative distribution of H₂, He, N₂+CO, O₂, A, CO₂, CH₄, and isotopes of Kr and Xe was found. The ratio between N₂ and CO was determined by gas-chromatography. The results are shown in Table III-X.

TABLE III-V
FUEL ROD DATA FOR IFA-226

Upper Cluster	Rod	Cladding ^[a]		Fuel ^[b]			Gap ^[d] (mm)	Instrumentation		
		OD (mm)	ID (mm)	Density ^[c] (% theoretical)	Diameter (mm)	Stack Length (mm)		Fuel Centerline ^[e] Thermocouple	Pressure Transducer	Elongation Sensor
Upper Cluster	AA	10.69	9.51	92.06	9.30	653	0.21	X	X	
	AB	10.64	9.52	91.50	9.30	650	0.21		X	
	AC	10.66	9.51	90.58	9.30	650	0.21		X	
	AD	10.65	9.52	95.10	9.30	663	0.21		X	
	AE	10.68	9.51	91.64	9.26	655	0.25	X	X	
	AG	10.68	9.50	94.33	9.40	663	0.10			X
Lower Cluster	AH	10.69	9.49	94.46	9.40	607	0.09			X
	AJ	10.67	9.50	94.43	9.35	610	0.14			X
	AK	10.66	9.51	95.94	9.31	607	0.20	X		X
	At thermo- couple location	10.66	9.51	96.10	9.30		0.21			
	AM	10.68	9.50	94.52	9.30	607	0.20			X
	AC	10.66	9.52	92.11	9.30	615	0.21	X		X
	At thermo- couple location	10.66	9.51	92.04	9.30		0.21			
	B-5	10.67	9.51	94.18	9.26	612	0.25			

[a] All cladding made of zircaloy-4.

[b] Fuel pellet length = 15.0 mm
Dish depth = 0.33 mm
Dish shoulder = 1.73 mm
Dish spherical radius = 6.79 mm.

[c] The values listed here are measured geometric densities (that is, weight divided by volume). The density of a limited number of fuel samples was also measured by immersion techniques. The immersion techniques (true) densities were found to be about 3/4% higher at all density levels.

[d] Backfill helium pressure = 1.01×10^4 N/m².

[e] Centerline thermocouple hole diameter = 1.73 mm.

TABLE III-VI
EVALUATION OF THE PROFILOMETRY

Rod Number	Changes in Outer Diameter	Ridging	Remarks
AA-1	[a]	Slight tendency	Ridging not rotationally symmetric
AB-2	10 to 15 μm increase	Slight tendency	Ovality maximum 40 μm
AC-3	15 μm increase	Slight tendency; height, 2.5 to 5 μm	
AD-4	[a]	Slight tendency; height, 2.5 to 5 μm	Ovality maximum 30 μm
AE-5	15 μm increase	Only at pellets around thermocouple	
AG-6	[a]	Pronounced; height, 15 to 18 μm	Beginning of secondary ridging at hot end
AH-7	[b]	Pronounced; height, 30 μm	Secondary ridging all along the stack
AJ-8	[b]	Pronounced; height, 30 μm	Secondary ridging at hot end
AO-9	[a]	Slight tendency; height, 2.5 to 5 μm	
AM-10	[a]	Pronounced; height, 30 μm	Few secondary ridges
AK-11	15 μm increase	Slight tendency; height, 2.5 to 5 μm	Rod failed
B5-12	20 μm decrease	Pronounced; height, 15 to 20 μm	Ovality maximum 30 μm

[a] Change too small to measure.

[b] The profiles of Rods 7 and 8 showed varying diameter increases, with the largest increase at the top end of the fuel stack, the change decreasing toward the colder end.

TABLE III-VII
FORM FACTORS CALCULATED FROM THE CURVES^[a]

Rod	Isotope		
	Cs-137	Zr-95	Gross
AA	1.54	1.39	1.52
AB	1.59	1.45	1.54
AC	1.60	1.41	1.58
AD	1.58	1.40	1.58
AE	1.56	1.41	1.53
AG	1.58	1.41	1.54
AH	1.17	1.10	1.14
AJ	1.16	1.11	1.12
AO	1.17	1.11	1.16
AM	1.16	1.10	1.14
AK	1.12	1.10	1.16

[a] Local peaks are neglected in the calculation.

2.2.6 Metallography and Autoradiography. Specimens from Rods AA-1, AE-5, AH-7, AK-11, and AO-9 were examined by metallography and autoradiography. The cutting positions are shown in Figure III-12. For each rod, a macrophoto and a radius is shown (see Figures III-13 through III-20). (Figures III-16 and III-17 are alpha and beta/gamma autoradiographs from Sample AH-7-1. Sample No. 2A, from Rod AK-11, contained destroyed thermocouple fragments.)

In all samples, the fuel contained two classes of pores: the normal small ones and some very big ones (about 80 micrometres in diameter), the latter randomly distributed (from fabrication). In general, the fuel looked very nonhomogeneous. One area had normal pore structure; an adjacent area at the same distance from the center contained almost no visible pores. All samples, except the one from Rod AK-11, had only circumferential hydrides in the cladding. The measurements of oxide layer thickness on the inner and outer

TABLE III-VIII
RELATIVE POWER OF THE RODS

<u>Rod</u>	<u>Isotope</u>		
	<u>Cs-137</u>	<u>Zr-95</u>	<u>Gross</u>
AA	0.69	0.77	0.68
AB	0.73	0.86	0.72
AC	0.73	0.88	0.73
AD	0.73	0.91	0.73
AE	0.68	0.76	0.68
AG	0.74	0.89	0.73
AH	1.00	1.00	1.00
AJ	0.98	0.96	0.98
AO	0.95	0.94	0.94
AM	0.93	0.92	0.93
AK	0.92	0.90	0.92

surface of the cladding, cladding thickness and gap, estimated hydride content in the cladding, extent of fuel restructuring, and an estimated fuel center temperature are shown in Table III-XI.

Rod AK-11 contained cracks in the plenum end plug and in the cladding; it is known to have been in the reactor with these cracks for about 2 months. The cracked end plug is shown in Figures III-21 and III-22. Examples of the cracked cladding are shown in Figures III-23 through III-25.

2.2.7 Fuel Density Determination. The density of the fuel was measured by immersion in water containing 10 milligrams per litre of a detergent (catanac).

The size of each sample was one pellet. However, a smaller amount of fuel adhered to the cladding after defueling and, in all five cases, some of the fuel was sampled as coarse

TABLE III-IX

LENGTH OF THE POSTIRRADIATION FUEL STACK OBTAINED
FROM THE GAMMA SCANNING GIVEN TOGETHER WITH THE
PREIRRADIATION PELLET STACK LENGTH

Rod	Postirradiation Stack Length ^[a] (mm)	Preirradiation Stack Length (mm)	Change in Stack Length (mm)
AA	651.6	653.0	-1.4 ^[b]
AB	646.3	650.6	-4.0
AC	645.75	650.6	-4.85
AD	662.4	662.9	-1.5
AE	660.0	656.03	+1.5
AG	655.3	662.6	-7.3
AH	611.1	606.6	+4.5
AJ	611.2	608.4	+2.8
AO	617.85	613.84	+4.0 ^[c]
AM	609.0	606.12	+2.7
AK	612.25	607.2	+5.0

[a] Including interpellet gap.

[b] To this shrinkage should be added an interpellet gap of 2.5 mm, giving all together -3.9 mm.

[c] From this extension should be subtracted a 3-mm interpellet gap, giving all together +1.0 mm.

powder. The sample from Rod AO-9 consisted of approximately 50% powder, which may reflect the relatively small decreases in density for this sample. The results are shown in Table III-XII.

2.2.8 Isotopic Fuel Analysis. The isotopic fuel analysis was performed on six samples, each representing approximately one fuel pellet.

Each fuel sample was extracted from the cladding, crushed, and dissolved in 50 millilitres concentrated nitric acid. This solution was strongly diluted with 8 molar nitric acid before separation on ion exchanger columns.

TABLE III-X
RESULTS FROM THE PIERCING

Rod	Extracted Fission Gas, (Ncm ³)		Fission Gas Release (%)		Free Volume of Rod (cm ³)	Pressure in at Hot Cell Condition (atm)
	Kr	Xe	Kr	Xe		
AA-1	8.7	16.7	3.0	5.7	-	-
AB-2	8.5	9.7	2.8	3.2	14.9	1.7
AC-3	-	-	-	-	-	-
AD-4	25.5	25.8	7.8	7.9	12.1	3.6
AE-5	6.0	7.8	2.0	2.7	13.3	1.0
AG-6	45.2		13.6		10.9	10.6
AH-7	53.3	52.6	13.7	13.5	14.5	5.4
AJ-8	65.9		16.3		15.0	6.4
AO-9	68.4	66.8	17.1	16.7	16.1	5.8
AM-10	68.0		17.9		15.5	6.3
B5-12	77.1		19.8		16.1	8.2

[a] The amount of fission gas is referred to 273 K, 1 atm.

The analysis was carried out on the diluted fuel solutions along three lines as determinations of

- (1) Uranium and plutonium isotopes by mass spectrometry
- (2) Neodymium-148 by mass spectrometry
- (3) Plutonium-238, americium-241, curium-242, and curium-244 by alpha spectrometry.

Fig. III-12 Samples for metallographic examination.

The results are shown in Table III-XIII. The sample numbers in the table are identified as follows: the first figure refers to the rod number, the second is the code for the cutting position. Table III-XIV shows the cutting positions in millimetres from the bottom of the rods.

All the isotope values are the atomic ratios to U-238. The values for U-235, Pu-238, Pu-241, Am-241, Cm-242, and Cm-244 are corrected for decay so that the results were valid at the time of fuel discharge (September 20, 1975). Burnup (atom percent) [BU (at.%)] is the percent of all initial heavy metal atoms, which have fissioned (FIMA). The burnup results are calculated using 0.0170 as the fission yield of Nd-148. The factor 8.3×10^3 can be used to convert BU (A/O) to MWd/t UO_2 ; i.e., 1% FIMA is equal to 8300 MWd/t UO_2 .

x 10

x 30

Fig. III-13 Photomicrograph of Sample AA 1-1, unetched.

x 10

x 30

Fig. III-14 Photomicrograph of Sample AE 5-1, unetched.

x 10

x 30

Fig. III-15 Photomicrograph of Sample AH 7-1, unetched.

x 10

Fig. III-16 Autoradiograph of sample in Figure III-16.

x 10

Fig. III-17 Autoradiograph of sample in Figure III-16.

x 10

x 30

Fig. III-18 Photomicrograph of Sample AO 9-1, unetched.

x 10

x 30

Fig. III-19 Photomicrograph of Sample AK 11-2-A, unetched.

x 10

x 30

Fig. III-20 Photomicrograph of Sample AK 11-6-A, unetched.

TABLE III-XI
EVALUATION OF THE CERAMOGRAPHY AND THE METALLOGRAPHY

Sample	Oxide on Outer Surface of Cladding (μm)	Oxide on Inner Surface of Cladding (μm)	Cladding Thickness (μm)	Gap (μm)	Estimated Hydride Content (ppm)	Fraction of Fuel with Columnar Grains	Fraction of Fuel with Equiaxed Grains	Estimated Fuel Centerline Temperature (K)
AA-1-1	0 to 14, discontinuous	4 to 7, continuous	555 to 560	29 to 67	50; surface 80 to 90	0	0.77	<1873
AE-5-1	0 to 6, discontinuous	0 to 6, discontinuous	575 to 595	21 to 55	55 to 65	0.36	0.75	2123
AH-7-1	5 to 13, discontinuous	5 to 13, discontinuous	580 to 595	0	60 to 70	0	0.72	<1873
AO-9-1	5 to 20, continuous	0 to 10, discontinuous	560	24 to 48	40	0.68	0.86	2873
AO-9-2A	10, discontinuous	5, discontinuous	560	38	15	0.62	0.82	2623
AO-9-5A	12, discontinuous	10, discontinuous	555	38	35 to 40	0.37	0.68	2123
AK-11-2-PA	5 to 9, discontinuous	4 to 26, continuous	570 to 590	0	50; surface 90 to 100	0.48	0.71	2273
AK-11-2-P1	0 to 14, discontinuous	11 to 18, discontinuous	560 to 590	0 to 10	50 surface	0.35	0.66	2123
AK-11-3	0 to 14, discontinuous	10 to 14, continuous	575 to 585	0 to 20	30 to 40	0.36	0.72	2123
AK-11-6-PA	4 to 11, discontinuous	10 to 16, discontinuous	570 to 580	10 to 15	50	0	0.63	<1873
AK-11-6-P1	2 to 13, discontinuous	12 to 18 discontinuous	585 to 590	0 to 30	40	0	0.67	<1873
AK-11-7	6 to 11, discontinuous	0 to 9, discontinuous	570 to 580	0 to 30	60 to 75	0	0.63	<1873

Fig. III-21 Cracked end plug, Sample AO-11-1.

x 10

Fig. III-22 Cracked end plug, Sample AO 11-1, unetched.

x 30

Fig. III-23 Sample AO 11-6-1, unetched. Probably hydride-initiated cracking.

x 30

Fig. III-24 Sample AO 11-6-1, fuel and hydride etched. Probably hydride-initiated cracking.

x 100

Fig. III-25 Sample AO 11-2, hydride etched. Probably stress-corrosion initiated crack.

TABLE III-XII

FUEL DENSITY

Rod	Sample Position from Bottom End (mm)	Postirradiation ^[a] Density (% TD)	Preirradiation ^[b] Density (% TD)	Change in Density (% TD)
AA-1	138	91.61	92.58	-1.0
AE-5	145	91.01	93.10	-2.1
AK-7	508	92.14	94.09	-2.0
A0-9	508	92.59	92.87	-0.3
AK-11	515	93.93	96.65	-2.7

[a] By immersion.

[b] Geometric + 0.75% on actual pellets.

TABLE III-XIII

RESULTS OF THE ISOTOPIC FUEL ANALYSIS

Sample	Rod and Cutting Position					
	1-2	5-2	7-2	9-3	9-6	11-4
U-234	2.10×10^{-5}	1.86×10^{-5}	2.28×10^{-5}	1.93×10^{-5}	1.97×10^{-5}	1.96×10^{-5}
U-235	1.36×10^{-3}	1.31×10^{-3}	1.22×10^{-3}	1.16×10^{-3}	1.22×10^{-3}	1.15×10^{-3}
U-236	1.60×10^{-4}	1.62×10^{-4}	1.83×10^{-4}	1.78×10^{-4}	1.72×10^{-4}	1.81×10^{-4}
U-238	4.20×10^{-4}	4.23×10^{-4}	4.37×10^{-4}	4.42×10^{-4}	4.57×10^{-4}	4.34×10^{-4}
U-239	3.47×10^{-2}	3.56×10^{-2}	2.87×10^{-2}	2.91×10^{-2}	3.23×10^{-2}	2.71×10^{-2}
Pu-240	2.34×10^{-2}	2.52×10^{-2}	2.54×10^{-2}	2.56×10^{-2}	2.56×10^{-2}	2.47×10^{-2}
Pu-241	5.22×10^{-3}	5.66×10^{-3}	5.65×10^{-3}	5.92×10^{-3}	6.15×10^{-3}	5.80×10^{-3}
Pu-242	1.91×10^{-3}	2.07×10^{-3}	2.36×10^{-3}	2.44×10^{-3}	2.36×10^{-3}	2.44×10^{-3}
Am-241	7.76×10^{-4}	6.79×10^{-4}	1.05×10^{-3}	8.67×10^{-4}	9.36×10^{-4}	9.52×10^{-4}
Am-242	7.25×10^{-5}	7.34×10^{-5}	7.79×10^{-5}	9.32×10^{-5}	8.85×10^{-5}	9.11×10^{-5}
Cm-244	2.19×10^{-5}	2.58×10^{-5}	4.04×10^{-5}	3.38×10^{-5}	3.51×10^{-5}	4.25×10^{-5}
Nd-148	6.76×10^{-4}	7.06×10^{-4}	7.82×10^{-4}	8.32×10^{-4}	7.66×10^{-4}	8.16×10^{-4}
BU (a.c.%)	3.59	3.73	4.2	4.40	4.05	4.32

TABLE III-XIV
CUTTING POSITIONS

<u>Rod and Cutting Position</u>	<u>Distance from Bottom of Rod (mm)</u>
1-2	121
5-2	99
7-2	525
9-3	525
9-6	386
11-4	632

3. FUEL MODEL DEVELOPMENT AND VERIFICATION

J. A. Dearien

Development of the Fuel Rod Analysis Program-Steady State (FRAP-S) and -Transient (FRAP-T) codes continued. FRAP-S3 programming and preliminary verification were completed, and FRAP-S3 was delivered for independent verification. The independent verification and final documentation of FRAP-T3 were completed.

Improvements were made to many of the FRAP subcodes. A two-dimensional heat conduction subcode was written for inclusion in FRAP-S. This subcode permits calculation of fuel temperatures when the power generation rate varies circumferentially as well as radially. Models were added to FRAP-T to include strain rate effects in the small deformation calculations. Previously, strain rate effects were included only in the large deformation (ballooning) calculations. Finally, FRAP-T was modified so that a fuel rod with a partial length central void could be analyzed, so as to permit analysis of experimental fuel rods containing centerline thermocouples and annular pellet rods.

Two significant changes were made in the material properties subcode, MATPRO. The cladding emissivity model was revised to reflect new data that included the effect of oxide thickness, and models of the effect of oxygen on the mechanical properties of zircaloy were generated. Details of these additions to MATPRO are given in the following sections.

3.1 FRAP-T3 Model Verification Analysis

D. R. Coleman

3.1.1 General Results. Calculated results from the third version of the Fuel Rod Analysis Program-Transient (FRAP-T3) were compared with measured steady state and transient data for operating fuel rods and also for out-of-pile rupture conditions. The comparisons address the capability of the thermal and mechanical models for a limited range of steady state, off-normal, and transient conditions.

FRAP-T3 results were also evaluated to determine the consistency between steady state FRAP-S2 results and initial conditions reproduced by the transient code at different burnups. The capability of the steady portion of FRAP-T3 to appropriately use externally supplied permanent burnup effects is evaluated for standard design power reactor rods. Detailed results of FRAP-T3 verification studies have been described in Reference 22 and are summarized below.

3.1.2 Steady State Standard Design Analysis. For 7 x 7 and 15 x 15 rods, FRAP-S2 burnup-dependent center temperature behavior is reproduced initially in FRAP-T3 within 150 K over the full normal power range. Rod average burnup effects correctly represent initial gap variation versus power for the BWR cases. Input of local burnup effects is required for FRAP-T3 to reproduce FRAP-S2 gap size behavior for PWR rods at end of life. Good agreement for both fuel types is obtained between FRAP-S2 and FRAP-T3 with respect to initial rod internal pressure as a function of power at various burnups. Due to lack of corrosion effects coupling, FRAP-T3 initial cladding temperatures of burnup rods are between 10 and 40 K lower than those predicted by FRAP-S2. Initial plenum gas temperature oscillations in FRAP-T3 are associated with gas mixture properties not dominated by an individual component.

3.1.3 Steady State Data Comparisons. Pressurized rod startup centerline temperatures are underpredicted by 50 to 180 K below 32.8 kW/m. Unpressurized rod fuel temperatures are within $\pm 10\%$ of the data for rods with low fission gas content. Data comparisons for fuel temperature and gap conductance indicate that calculated effects of gap size and gas composition on the thermal model are too strong. The calculated heatup effect on startup internal pressure response is within $\pm 10\%$ of the data for a wide range of fill gas conditions. The heatup effect on fuel axial expansion is represented well by the model prior to observed gap closure. A fuel relocation adjustment compensating for current error in the gap closure model was found to be proportional to the space available for accommodating pellet cracks. Implementation of a fuel relocation model requires treatment of pellet mechanical deformation but is expected to improve capability of thermal, gas release, and pellet-cladding mechanical interaction models.

3.1.4 Off-Normal Data Comparisons. Comparison of measured and predicted flow and power conditions at initiation of CHF indicates good model performance for bundle

geometries with uniform flow and small cold-wall effect. The single-channel enthalpy rise model seems adequate in this case. Single-rod test results exhibit significant scatter due to the effect of channel geometry, flow area, and standoff devices. Comparing results for various CHF correlation options provides support for using the General Electric Company model for BWR conditions and the B&W-2 model for PBF rods with individual flow shrouds.

Low temperature cladding failure probability during power increases is represented well by the FRAIL subcode for those hard-closure conditions (>49.2 kW/m) unaccompanied by a significant bulk fuel plasticity effect. Lower observed failure probabilities associated with larger gaps and more moderate power levels are underestimated by the model, possibly due to not treating fuel relocation, stress corrosion cracking, and statistical failure effects. More frequency of failure data is needed for core average as opposed to lead rod operating conditions.

3.1.5 Transient Data Comparisons. Transient centerline temperatures were overpredicted for several rods experiencing scram under normal cooling conditions. Initial calculated temperatures were between 100 and 400 K higher than observed due to the overly strong influence of gap size and gas composition on the steady state thermal model. Transient and equilibrium temperatures for best-estimate initial condition runs were overestimated by up to 300 K, due in part to underpredicting the extent of fuel cracking which remains once power is decreased.

Transient cladding temperatures up to 1273 K were represented well by the model for the low surface heat transfer condition associated with superheated steam flow in two TREAT LOCA simulation tests. Transient internal pressure response was generally overpredicted, especially for the small plenum conditions most sensitive to error in calculated stack elongation, active length void volume and temperature, and transient plenum gas temperature. Cladding ballooning and rupture were predicted to occur at significantly lower than observed temperature and pressure levels.

3.1.6 Out-of-Pile Tube Rupture Data Comparisons. Evaluated cladding burst temperatures were underpredicted by MATPRO by at least 30% for tube heatup tests. Agreement between measured and calculated burst pressure for low temperature (588.65 to 699.75 K) isothermal tests was within data scatter. Cladding rupture deformation was largely overpredicted by MATPRO, due in part to underestimating burst temperature for heatup tests. Underpredicting rupture strain corresponded to isothermal expansion data between 1033.15 and 1171.95 K. Mean failure criteria used as the basis of the FRAIL subcode provided a better representation of observed temperature and pressure conditions at rupture. Overall out-of-pile tube burst results indicate that failure criteria based on the current $\sigma - \epsilon$ concept used in MATPRO are better applied to normal operating temperature conditions. A different $\sigma - \epsilon$ form or alternate corollary should be developed to relate

cladding stress and temperature conditions, calculated failure probability, and observed deformation behavior.

3.2 Cladding Surface Emissivity (ZOEMIS)

D. L. Hagrman

One of the important modes of heat transfer to and from cladding surfaces during an off-normal transient is radiant heat transfer. Since the energy radiated is directly proportional to the emissivity of the inner and outer cladding surfaces, accurate values of surface emissivity are needed in the analysis of off-normal transients.

3.2.1 Summary. Surface emissivities are significantly affected by surface layers on the cladding. For cladding with thin oxide coatings, the oxide surface thickness represents only a few wavelengths of infrared radiation and is partly transparent. Oxide thickness is an important parameter for these thin coatings. Thicker oxide layers are opaque, so the oxide thickness is not as important as the nature of the outer oxide surface. This surface is affected by temperature and by the chemical environment. The effect of temperature has been modeled, but variations in crud on the external cladding surface and chemical reaction products on the inside surface are not modeled explicitly.

The model for emissivity was constructed by considering measured emissivities reported by several investigators^[23,24,25]. Expressions used to predict the emissivity of zircaloy cladding surfaces are summarized below.

When the cladding surface temperature has not exceeded 1500 K, emissivities are modeled by Equations (3a) and (3b). For oxide layer thicknesses less than 3.88×10^{-6} metre

$$\epsilon_1 = 0.325 + 0.1246 \times 10^6 d. \quad (3a)$$

For oxide layer thicknesses of 3.88×10^{-6} metre or greater^[a]

$$\epsilon_1 = 0.808642 - 50.0d \quad (3b)$$

where

ϵ_1 is the hemispherical emissivity (unitless)

d is the oxide layer thickness (m).

[a] The use of six significant figures in Equation (3b) ensures an exact match of the values ϵ_1 at $d = 3.88 \times 10^{-6}$ metre.

When the maximum cladding temperature has exceeded 1500 K, emissivity is taken to be the larger of 0.325 and

$$\epsilon_2 = \epsilon_1 \exp [(1500 - T)/300] \quad (4)$$

where

ϵ_1 is the value for emissivity obtained from Equations (3a) and (3b)

T is the maximum cladding temperature.

The standard error expected from the use of Equations (3a) and (3b) to predict emissivity when cladding surface temperature has remained below 1500 K was determined to be

$$\sigma_1 = \pm 0.1 \quad (5a)$$

When cladding temperature has exceeded 1500 K, the expected standard error is estimated by considering the value σ_2 in the expression

$$\sigma_2 = \pm 0.1 \exp (-[1500 - T]/300) \quad (5b)$$

where T is the maximum cladding temperature. If Equation (5b) and Equation (3b) predict values of $\epsilon_2 \pm \sigma_2$ which fall within the range of physically possible values for emissivity (0 to 1), the value σ_2 is the expected standard error. If the predicted value of $\epsilon_2 + \sigma_2$ is greater than 1 or if $\epsilon_2 - \sigma_2$ is less than 0, the expected standard error of Equation (5b) is changed to limit $\epsilon_2 + \sigma_2$ at 1, $\epsilon_2 - \sigma_2$ at 0, or both.

The following subsection is a review of the cladding emissivity data which were used to construct the model. Subsection 3.2.3 describes the approach used to formulate the model for emissivity and Subsection 3.2.4 is a discussion of the uncertainty of the model for cladding emissivity.

3.2.2 Data Used to Construct the Model. Measurements of zircaloy-2 emissivities as a function of temperature and dissolved oxygen content were reported by Lemmon^[23]. The measurements were made using the hole-in-tube method and were carried out in vacuum. Data from samples with an oxide film were reported, but the nonoxidizing environment of the sample during emissivity measurements caused the emissivity to change with time. Moreover, the thicknesses of the oxide films were not reported. The Lemmon data were not used in formulating the model because the unknown oxide thickness probably influenced the values of emissivity and because of the complications caused by the vacuum environment.

The emissivity of zircaloy-4 was reported by Juenke and Sjodahl^[24] from measurements on oxidized zircaloy in vacuum and from measurements in steam during the isothermal growth of oxide films. These authors report a decrease in the emissivity measured

in vacuum, which they attribute to the formation of a metallic phase in the oxide. This metallic phase did not form in the presence of steam. The data taken in steam were used in constructing ZOEMIS because the steam environment is similar to the off-normal reactor environment.

The data of Juenke and Sjodahl suggest that emissivity decreases when oxide films become very thick. In fact, Juenke and Sjodahl expect the total emissivity of very thick films to approach values of 0.3 or 0.4, which are characteristic of pure ZrO_2 . However, the decrease in emissivity at temperatures greater than about 1473 K is greater than one would predict from oxide layer thickness alone. The correlation of these emissivity data with oxide layer thickness is discussed in Subsection 3.2.3.

Juenke and Sjodahl's data do not include very thin oxide films, but they do report that the total emittance rises almost instantaneously from about 0.2 to 0.7 with the introduction of steam. Data relevant to thin films are discussed below.

The emissivity of oxide films measured in air at temperatures in the range of 373 to 673 K was reported by Murphy and Havelock^[25]. The emissivities are not strongly dependent on temperature, but they do increase rapidly with oxide thickness for the thin oxide layers measured. The one value of emissivity measured with an oxide thickness of 94×10^{-6} metre is important because the oxide was approximately 30 times the thickness associated with the transition from "black" to "white" oxide layers. The emissivity of this oxide, which was described as white by the authors, had a measured emissivity characteristic of surfaces which are black in the infrared region of the spectrum. Since (a) the Murphy and Havelock data were taken in an oxidizing environment and (b) the emissivity of the 94×10^{-6} -metre oxide film agrees with the emissivity of films measured in steam, all of the Murphy and Havelock data were used in the formulation of the expression for cladding surface emissivity.

Additional data were reported by T. B. Burgoyne and A. Garlick at the Organization for Economic and Cooperative Development – Committee on Safety of Nuclear Installations (OECD-CSNI) meeting on the behavior of water reactor fuel elements under accident conditions in Spind, Norway, on September 13 to 16, 1976. Using a hot-filament calorimeter, these authors measured the emissivity of zircaloy-2 cladding surfaces coated with uniform oxide, nodular oxide, and crud. The emissivities were measured in vacuum. However, the following arguments are presented in favor of including some of these data in the data base for the cladding surface emissivity model:

- (1) A significant decrease in emissivity was not noticed with initial oxide layers greater than 10^{-5} metre until the samples were heated above approximately 1073 K (the alpha-beta transition of zircaloy)
- (2) The low temperature values of emissivity measured with uniform oxides correspond closely to values measured in steam

- (3) The data taken with nodular- and crud-coated surfaces are representative of in-reactor surfaces not represented in other data.

Data from Burgoyne and Garlick which did not show the sudden decrease in emissivity characteristic of the change caused by a vacuum environment were used in the model.

3.2.3 Development of the Model. Near infrared radiation has a wavelength of 10,000 angstroms or 10^{-6} metre. Oxide films up to a few wavelengths thick should be partly transparent to infrared radiation and should therefore have emissivities which are strongly dependent on oxide thickness. The emissivity versus oxide thickness data of Murphy and Havelock^[25] were fit with standard least-squared residual analysis to deduce Equation (3b).

The equation for the emissivity of oxide films thicker than 4×10^{-6} metre is based on the data of Burgoyne and Garlick, data from Juenke and Sjodahl^[24], and one measurement from Murphy and Havelock, as discussed in Subsection 3.2.2. Oxide thicknesses were calculated from the time and temperatures reported by Juenke and Sjodahl using the correlation recently published by Cathcart^[8]

$$X = [2.25 \times 10^{-6} \exp(-18063/T)t]^{1/2} \quad (6)$$

where

X = the oxide layer thickness (m)

T = temperature (K)

t = time at temperature (s).

Values of emissivity and oxide layer thickness from Murphy and Havelock, Burgoyne and Garlick, and Juenke and Sjodahl for oxide layers thicker than 4×10^{-6} metre were used to establish Equation (3b).

Figure III-26 is a comparison of the curves generated by Equations (3a) and (3b) with the data base used to derive these equations. Predicted values of emissivity increase rapidly until the surface oxide layer is 3.88×10^{-6} metre thick, then decrease very slowly with increasing surface layer thickness.

The values of emissivity measured by Juenke and Sjodahl at 1575 K (0.62 and 0.6) are significantly below the measured emissivities at lower temperatures. Since thicker oxide films were formed at lower temperatures, the low emissivity is not due to the thickness of the oxide film. Moreover, the low values of emissivities measured by Juenke and Sjodahl at high temperature are supported by INEL posttest observations of cladding surfaces which have been at high temperatures^[26]. Cladding surfaces which experienced film boiling of

Fig. III-26 ZEOMIS predictions compared to the model's data base.

the coolant and, therefore, high temperatures, showed spalled oxide and somewhat lighter oxide surfaces in the region of the cladding which had been at high temperature. The INEL observations and the fact that the trend toward lower values of emissivity at higher temperatures was reported by Juenke and Sjodahl at 1475 and 1575 K implies that lower values of cladding surface emissivity are likely at temperatures above approximately 1500 K. This trend in the limited data has been included in the model for cladding surface emissivity by (a) adding a multiplicative factor to the expression for emissivity:

$$\exp \left[\frac{(1500 - T)}{300} \right] \quad (7)$$

where T is the greater of 1500 K and the maximum cladding temperature and (b) limiting the minimum value of emissivity to 0.325, which is the value predicted by the model for zero oxide thickness.

3.2.5 Uncertainty. The standard errors obtained with Equations (3a) and (3b) and the data base used to develop these equations are listed in Table III-XV.

TABLE III-XV
STANDARD ERRORS OF MODEL PREDICTIONS

<u>Surface Description</u>	<u>Standard Error</u>
Oxide films $< 3.88 \times 10^{-6}$ m	± 0.04
Pure oxide films $> 3.88 \times 10^{-6}$ m	± 0.05
Oxide films including samples with nodular oxides and crud	± 0.07

Standard errors shown in Table III-XIV for oxide layers without the complicating features of nodular oxides or surface crud are consistent with measurement errors of $\pm 3\%$ estimated by Lemmon^[23]. However, the model is intended to predict the emissivity of cladding surfaces with crud or UO_2 reaction products as well as the oxide layer. The data from Burgoyne and Garlick (illustrated in Figure III-27) suggest that crud layers introduce a systematic error of approximately 0.1. The value of ± 0.1 is therefore included in the model as the best estimate for the standard error of the model prediction for emissivity during off-normal reactor operation at temperatures below 1500 K.

The uncertainty of the prediction for emissivities above 1500 K is difficult to estimate. Equation (5b) was selected as a reasonable expression for the expected standard error of Equation (4), simply because the expression, $\pm 0.1 \exp [-(1500 - \text{maximum cladding temperature})/300]$, predicts a standard error approximately equal to the change in emissivity caused by the empirical multiplicative factor of Equation (7).

In Figure III-27, the data base and model predictions of Figure III-27 are repeated. The standard error expected for temperatures below 1500 K is represented by the cross-hatched area centered on the solid line. The cross-hatched area centered on the dashed line represents the standard error estimated for temperatures of 1573 K.

3.3 Effect of Oxygen on the Mechanical Properties of Zircaloy G. A. Reymann

The presence of oxygen causes changes in zircaloy's mechanical properties. This section describes modifications to be made in order to quantify these changes in the cladding plastic deformation subcodes of the materials properties handbook, MATPRO^[27]. The general effect is to increase the strength and decrease the ductility of the zircaloy.

Fig. III-27. Expected standard errors for temperatures below 1500 K and at 1573 K.

3.3.1 Summary. In the MATPRO handbook, the correlation describing stress-strain (as a function of strain rate, temperature, fluence, and cold work) when used in the FRAP codes [28,29], models plastic deformation of zircaloy cladding as

$$\sigma = K \epsilon^n \left(\frac{\dot{\epsilon}}{10^{-3}} \right)^m \quad (8)$$

where

σ = true stress (N/m^2)

ϵ = true strain (m/m)

$\dot{\epsilon}$ = true strain rate (s^{-1})

K = strength coefficient (N/m^2)

n = strain hardening exponent (dimensionless)

m = strain rate sensitivity constant (dimensionless)
(K, n, and m are all temperature dependent).

The correlations for the strength coefficient as a function of oxygen content and temperature are

$$K = K_0 + (1.794 \times 10^{11} - 2.21 \times 10^8 T) (C - C_0) \quad (9)$$

for $293 < T < 802$ K and $1.1 \times 10^{-3} < C < 6.3 \times 10^{-3}$; and

$$K = K_0 + 2.158 \times 10^9 (C - C_0) \quad (10)$$

for $802 < T < 1373$ K and $1.1 \times 10^{-3} < C < 1.1 \times 10^{-2}$

where:

K_0 = strength coefficient of as-received zircaloy (N/m^2)

T = temperature (K)

C = oxygen concentration (weight fraction)

C_0 = oxygen concentration of as-received zircaloy (weight fraction).

Due to lack of data, the strain hardening exponent (n) is not modeled as a function of oxygen content.

The strain rate sensitivity constant is correlated with oxygen content in the temperature range $1123 < T < 1673$ K and only for strain rates of approximately 10^{-3} s^{-1} . The correlation is

$$m = m_0 \exp [-69 (C - C_0)] \quad (11)$$

for $1123 < T < 1673$ K and $\dot{\epsilon} = 10^{-3} \text{ s}^{-1}$

where

m_0 = strain rate sensitivity constant of as-received zircaloy.

The correlations are for zircaloy with a uniform oxygen concentration, and the data base does not include ZrO_2 .

3.3.2 Effect of Oxygen Content on the Strength Coefficient. The true strain at maximum engineering stress, i.e., the engineering stress at the onset of plastic instability in a tensile test on sheet specimens at constant strain rate, is given by^[30]

$$\epsilon = \frac{n}{1+m} \quad (12)$$

and the engineering stress (S) as a function of true strain is ^[27]

$$S = \frac{\sigma}{\exp(\epsilon)} \quad (13)$$

Substituting Equations (8) and (12) into Equation (13), at maximum engineering stress, results in

$$S_{\max} = \frac{K \left(\frac{n}{1+m} \right)^n \left(\frac{\dot{\epsilon}}{10^{-3}} \right)^m}{\exp \left(\frac{n}{1+m} \right)} \quad (14)$$

So K is given by

$$K = \frac{S_{\max} \exp \left(\frac{n}{1+m} \right)}{\left(\frac{n}{1+m} \right)^n \left(\frac{\dot{\epsilon}}{10^{-3}} \right)^m} \quad (15)$$

where

$S_{\max}$ = the ultimate tensile strength in a tensile test (N/m^2).

When $\dot{\epsilon}$ was specified in the data, it was 10^{-3} per second. Since $\dot{\epsilon} = 10^{-3} \text{ s}^{-1}$ is typical in tensile tests, this value was assumed when not specified. In this case, Equation (15) reduces to

$$K = S_{\max} \left[\frac{\exp \left(\frac{n}{m+1} \right)}{\left(\frac{n}{m+1} \right)^n} \right] \quad (16)$$

(1) Low Temperature Correlation. A paper by Rubenstein^[31] gives values for the ultimate tensile strength as a function of oxygen concentration for temperatures ranging from about 300 to 650 K. For this range, m has little effect on the value of K calculated with Equation (16). The term $\exp(n)/n^n$, which results when m is neglected, varies from 1.45 to 1.53 for these temperatures. If this term is replaced by 1.49, the maximum error introduced is smaller than 3%, which is substantially less than the scatter in the data. Therefore, the strength coefficient has been calculated by simply multiplying the ultimate tensile strength by 1.49. Strength coefficients calculated in this way are shown in Figure III-28 for four temperatures as a function of oxygen concentration. Also shown in the figure are straight lines fit to these points by the least squares method.

From Figure III-28 it is clear that in this temperature range the rate of increase of K with oxygen concentration decreases as temperatures increase. This rate of increase is plotted as a function of temperature in Figure III-29. Again, the points seem to fall approximately on a straight line and a least squares fit was made to these points. The result is

$$\frac{\Delta K}{\Delta C} = 1.794 \times 10^{11} - 2.21 \times 10^8 T \quad (17)$$

where $\frac{\Delta K}{\Delta C}$ is the rate of change of the strength coefficient with oxygen concentration. Therefore, the strength coefficient is given by Equation (9), where K_0 is the value of K for as-received zircaloy.

Equation (9) should not be used for the unlikely conditions $C < C_0 = 1.1 \times 10^{-3}$ or $T < 293$ K (room temperature), because there are no supporting data. Its validity for $C > 6.3 \times 10^{-3}$ is also questionable. It should be valid at least to about 650 K, the maximum temperature reported by Rubenstein, and may be extrapolated with caution to about 800 K, where it intersects the high temperature correlation for K , which will be described in the next section.

The effect of the change in K caused by oxygen is shown in Figure III-30, where stress is plotted as a function of strain for temperatures of 300 and 644 K. At both temperatures, a curve is shown for as-received material ($C = C_0 = 10^{-3}$ weight fraction) and for material with twice the as-received oxygen content.

(2) High Temperature Correlation. Ultimate tensile strength data as a function of oxygen concentration have been reported by Chung et al^[32]. For their temperatures (1123 to 1373 K), the strain hardening exponent (n) returned by MATPRO is a constant, 0.028. For this n and all strain rate sensitivity constants (m) returned by MATPRO, the bracketed term in Equation (16) is equal to 1.14, with an accuracy better than 0.5%. In Figure III-31, the Chung et al data are presented, showing the strength coefficient and the ultimate tensile strength as a function of oxygen concentration, along with the least squares straight line for each temperature. The change in K with temperature simply reflects the

Fig. III-28 Strength coefficient and ultimate tensile strength as a function of oxygen concentration for several low temperatures.

Fig. III-29 Rate of change of the strength coefficient with oxygen concentration as a function of temperature for low temperatures.

Fig. III-30 Stress as a function of strain for two temperatures and two oxygen concentrations per temperature.

Fig. III-31 Strength coefficient and UTS as a function of oxygen concentration for various high temperatures.

temperature dependence of the strength coefficient. Its rate of increase with oxygen content seems constant as the temperature changes. Since the data are not extensive, there is no firm basis for a detailed model. Therefore, the assumption is made that at high temperatures K increases linearly with concentration and that the rate of increase is constant, as presented in Equation (7), $K = K_0 + 2.158 \times 10^9 (C - C_0)$.

The data base includes temperatures from 1123 to 1373 K and concentrations from 1.1×10^{-3} to 1.1×10^{-2} oxygen weight fraction. The same limits should serve as a safe range of validity, although the limit is extended down to 802 K to attain calculational continuity with the low temperature correlation.

The effect of oxygen content on K at high temperature is shown in Figure III-32, where stress is plotted as a function of strain at 1373 K for as-received oxygen concentration and for 2×10^{-3} weight fraction oxygen. The lower temperature range covers normal reactor operation, while the upper one covers most postulated accident conditions.

Fig. III-32 Stress as a function of strain at 1373 K for two oxygen concentrations showing the effect of the change in the strength coefficient.

3.3.3 Effect of Oxygen Content on the Strain Rate Sensitivity Constant. For the strain rate sensitivity constant (m), data are available only for high temperatures (1123 to 1673 K) and only at a single strain rate (10^{-3} s^{-1}). All the data used to formulate the model were taken from Chung et al^[33,34]. These data are shown in Figure III-33, where m is plotted as a function of C for seven temperatures. It is evident that m decreases with increasing C in all cases, and each curve resembles an exponential decay, although the scatter in the data precludes any definitive quantification of the temperature dependence. The 1173 K curve is anomalous because, at this temperature, the as-received zircaloy is in the α - β transition region.

Fig. III-33 Strain rate sensitivity constant (m) as a function of oxygen concentration from Chung et al[34,35].

Because of this scatter, only the ratio m/m_0 as a function of concentration, as shown in Figure III-34, was modeled. The equation used was

$$\frac{m}{m_0} = \exp [-B (C - C_0)] \quad (18)$$

where

$$B = \text{constant to be determined (weight fraction)}^{-1}.$$

A value of B equal to 69 was obtained using a least squares fit of the data to the equation. This value for B results in Equation (11).

The quality of the fit of Equation (11), using Chung's data can be seen in Figure III-34. Although a quantitative statement cannot be made at this time, the scatter may be partly the result of phase transitions which can occur even isothermally with changes in in oxygen content (Figure III-35). To include the 68% of the data which should fall within one standard deviation from the calculated line, the coefficient B of Equation (11) must be given quite large uncertainty limits, $B = 69 \pm 40$.

3.3.4 Conclusions and Recommendations. Stress as a function of strain rate for two different oxygen concentrations at 1273 K is shown in Figure III-36. Most of the change concentration is due to changes in the strength coefficient rather than the strain rate sensitivity constant, and since the model was based on data taken only at strain rates of about 10^{-3} /s, Figure III-36 shows considerable extrapolation to different strain rates.

The oxygen concentration in zircaloy cladding during accident conditions is generally believed to have discontinuities at various radial locations^[35]. At the surface exposed to the steam attack, a ZrO_2 layer is usually formed. Beneath this, an oxygen-stabilized alpha phase zircaloy layer is found with an oxygen concentration which depends primarily on the temperature of formation and which ranges from more than 20% to a fraction of a percent by weight. A discontinuity in the concentration is obviously present at the oxide-alpha phase interface. Proceeding inward toward the cladding center from the alpha zircaloy, there is usually a layer of beta phase zircaloy. There is another concentration discontinuity at the boundary between the alpha and beta phase zircaloy, believed to be due to the different oxygen diffusion constants in the two phases^[36].

Therefore, a detailed description of the mechanical behavior of zircaloy oxidized under transient conditions requires knowledge of the material properties at various temperatures with oxygen concentrations between as-received material and ZrO_2 . The model presented in this section, which describes the effects of oxygen concentrations less than about 2.0×10^{-2} weight fraction homogeneously distributed in the zircaloy, can therefore only be considered as a first approximation.

Fig. III-34 The ratio m/m_0 as a function of oxygen concentration, showing Chung et al data and the line used to fit to these data [34,35].

Fig. III-35 Zircaloy-oxygen phase diagram, taken from Chung et al[34].

Fig. III-36 Stress as a function of strain rate of 1273 K for two different oxygen concentrations.

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IV. REACTOR BEHAVIOR PROGRAM

R. R. Stiger, Manager

Accomplishments are reported for loss-of-coolant accident (LOCA) code development and containment code development.

1. LOCA CODE DEVELOPMENT

L. H. Sullivan

This section describes an improved solution scheme for the field equations in an advanced LOCA analysis code designated RELAP5 Pilot Code, Version 2. Code results are presented for a two-phase blowdown problem.

RELAP4/MOD7 is being developed to provide a best estimate model for core reflood following a postulated LOCA in a boiling water reactor (BWR). A model for steam/water separation in a BWR separator and dryer and a model for core top quench velocity during spray cooling are described. Analytical results of the core top quench model are compared with experiment data.

1.1 RELAP5 Model Development

V. H. Ransom and J. A. Trapp

This section documents the development progress on the RELAP5 advanced hydrodynamic model. A two-velocity thermal equilibrium (2VET) model has been formulated and described in Reference 1. This 2VET model was coded for testing in Version 1 of a small pilot computer program. Since Reference 1 was issued, the model has been further tested and improved in the course of running several test problems. A second more general version of the pilot code which includes all improvements was programmed. This code, designated RELAP5 Pilot Code, Version 2, is more modular in form and has a revised solution scheme to permit application to fluid network problems characteristic of reactor systems as well as straight passage problems. The network branching technique will require further generalization in order to correctly model all branching geometries of interest. The improved solution scheme for the field equations and some of the results of the test problems are reported.

1.1.1 Numerical Scheme. The numerical scheme used in the pilot code, Version 2, is based on a system of four difference equations consisting of overall continuity, mixture internal energy, overall momentum, and the difference of the phase momentum equations. These four equations are utilized in the following form:

Overall Continuity

$$(\rho_i^{n+1} v_i^{n+1} - \rho_i^n v_i^n) / \Delta t + \sum_j \epsilon_{ij} [\rho_{ij}^n (v_{ij}^{n+1} - v_{ij}^n) + g_{\rho_{ij}}^n (v_{ij}^{n+1} - v_{ij}^n)] A_{ij} = 0 \quad (1)$$

Mixture Energy

$$(\rho_i^{n+1} U_i^{n+1} v_i^{n+1} - \rho_i^n U_i^n v_i^n) / \Delta t + \sum_j \epsilon_{ij} [\rho_{ij}^n U_{ij}^n (v_{ij}^{n+1} - v_{ij}^n) + g_{\rho_{ij}}^n U_{ij}^n (v_{ij}^{n+1} - v_{ij}^n)] A_{ij} = -p_i^n \sum_j \epsilon_{ij} (\alpha_{ij}^n v_{ij}^n + g_{\alpha_{ij}}^n g_{v_{ij}}^n) + Q_i^n v_i^n - v_i^n \sum_j \{ \beta_{ij}^n F_{WL_{ij}}^n (v_{ij}^n)^2 + \gamma_{ij}^n F_{WG_{ij}}^n (g_{v_{ij}}^n)^2 + \delta_{ij}^n F_{I_{ij}}^n (v_{ij}^n - g_{v_{ij}}^n)^2 + \eta_{ij}^n g_{v_{ij}}^n [g_{v_{ij}}^n (g_{v_{ij}}^n / 2 - \tilde{v}_{ij}^n) - v_{ij}^n (v_{ij}^n / 2 - \tilde{v}_{ij}^n)] \} \quad (2)$$

Overall Momentum

$$[\rho_{ij}^n (v_{ij}^{n+1} v_{ij}^{n+1} - v_{ij}^n v_{ij}^n) + g_{\rho_{ij}}^n (v_{ij}^{n+1} v_{ij}^{n+1} - v_{ij}^n v_{ij}^n)] / \Delta t + \rho_{ij}^n \text{CONVL}_{ij}^n + g_{\rho_{ij}}^n \text{CONVG}_{ij}^n + (p_i^{n+1} - p_j^{n+1}) A_{ij} = g_{M_{ij}}^n (v_{ij}^n - g_{v_{ij}}^n) v_{ij}^n + g (\rho_{ij}^n + g_{\rho_{ij}}^n) v_{ij}^n - (F_{WL_{ij}}^n v_{ij}^n + F_{WG_{ij}}^n g_{v_{ij}}^n) v_{ij}^n \quad (3)$$

Difference of Momentum

$$[(v_{ij}^{n+1} v_{ij}^{n+1} - v_{ij}^n v_{ij}^n) - (g_{v_{ij}}^{n+1} v_{ij}^{n+1} - g_{v_{ij}}^n v_{ij}^n)] / \Delta t + \text{CONVL}_{ij}^n - \text{CONVG}_{ij}^n + \left(\frac{1}{\rho_{ij}^n} - \frac{1}{g_{\rho_{ij}}^n} \right) (p_i^{n+1} - p_j^{n+1}) A_{ij} = -(F_{WL_{ij}}^n v_{ij}^n / \rho_{ij}^n - F_{WG_{ij}}^n g_{v_{ij}}^n / g_{\rho_{ij}}^n) v_{ij}^n - \left(\frac{\rho_{ij}^n + g_{\rho_{ij}}^n}{\rho_{ij}^n g_{\rho_{ij}}^n} \right) F_{I_{ij}}^n (v_{ij}^{n+1} - g_{v_{ij}}^{n+1}) v_{ij}^n - g_{M_{ij}}^n [(\tilde{v}_{ij}^n - v_{ij}^n) / \rho_{ij}^n + (\tilde{v}_{ij}^n - g_{v_{ij}}^n) / g_{\rho_{ij}}^n] v_{ij}^n \quad (4)$$

where:

P	=	pressure
ℓ^α, g^α	=	liquid fraction and void fraction, respectively
$\rho, \ell\rho, g\rho$	=	density of mixture, liquid, and vapor, respectively
$U, \ell U, gU$	=	internal energy of mixture, liquid, and vapor, respectively
V	=	volume of the scalar control volume
ℓ^v, g^v	=	velocity of liquid and vapor, respectively
J	=	junction velocity
A	=	junction area
Q	=	heat transfer from surroundings
FWL, FWG, FI	=	drag factors for liquid, vapor, and interphase, respectively
$\ell_\rho = \ell^\alpha \ell\rho$	=	partial density of liquid
$g_\rho = g^\alpha g\rho$	=	partial density of vapor
$\rho = \ell_\rho + g_\rho$	=	mixture density
$U = (\ell_\rho \ell U + g_\rho gU)/\rho$	=	mixture internal energy
$\beta, \gamma, \delta, \eta$	=	weighting factors for junction energy terms
ϵ_{ij}	=	0 if $i = j$, = 1 if $i > j$, = -1 if $i < j$
γ	=	unit momentum associated with interphase mass transfer
ℓ^M, g^M	=	volumetric liquid and vapor mass transfer rates, respectively
t	=	time
$CONVL, CONVG$	=	specific momentum convection for liquid and vapor phases, respectively

Subscripts:

i, j = junction property for junction connecting volumes i and j

i, j = volume property for volumes i and j , respectively

Superscripts:

$n+1$ = new time level, $t = (n+1) \Delta t$

n = old time level, $t = n \Delta t$.

This system of difference equations is linear in the new time variables (time level $n+1$) and is written for a generalized mesh in which the junctions are permitted to move with velocities J . The linear feature permits time advancement by inversion of a system of linear equations. The variable junction location permits tracking of discontinuities such as liquid levels.

The implicit coupling of the equations (at time level $n+1$) is sufficient to yield a stable solution scheme that is not time-step-size limited by acoustic propagation speed. The implicit coupling is provided by evaluating the velocities in the mass and energy convective terms and the pressure gradient and interphase drag terms in the momentum equations all at the new time. All other terms are evaluated at the old time. Donor differencing is used for the mass and energy convective terms, and a centered plus artificial viscosity differencing scheme is used for the momentum convective terms.

Terms are centered between old and new time levels wherever possible without complicating the numerical scheme. This combination of difference operators yields a stable, fast, and accurate difference algorithm.

The system of difference equations is solved by forming a pressure equation for each cell by elimination of all other variables from the overall continuity equation. This is accomplished by using a two-term Taylor series expansion of the state equation about the old time level, that is,

$$\rho_i^{n+1} = (\partial \rho / \partial P)_i^n (P_i^{n+1} - P_i^n) + [\partial \rho / \partial (\rho U)]_i^n [(\rho U)_i^{n+1} - (\rho U)_i^n] + \rho_i^n. \quad (5)$$

Velocities are eliminated using the two momentum equations, Equations (3) and (4). The product of density times energy is eliminated using the energy equation, Equation (2). This process results in a system of simultaneous linear equations for the pressure in each cell. The system matrix is tridiagonal for a straight passage and is a more general matrix for interconnected networks. The system of equations is solved for the pressure at the new time level by matrix inversion. This can be accomplished directly or by iteration. A direct method is used in the pilot code.

The new velocities at each junction are calculated using the momentum equations with the new pressures. The new velocities are then used to set the final donor cell formulation. Then, the mass and energy equations are used to obtain the final energy in each cell. The last step uses the state relationships to obtain the void fraction, partial energies, and partial densities for both phases. This completes a time cycle and the process is used repetitively to advance the time integration.

Version 2 of the pilot code includes a solution algorithm which has a provision for solving an interconnected hydraulic network typical of power reactor systems. The mass and energy flux terms at branches assume a uniform mass and energy distribution within a volume. The momentum flux terms are correct only for a straight passage. Additional model development work will be required to extend these formulations to a general branch. However, the steady state momentum option may be used with correlated data at branching junctions.

1.1.2 Pilot Code Results. Version 2 of the pilot code was applied to several straight pipe problems in the process of developmental verification. Figures IV-1 and IV-2 illustrate application of the code to a two-phase blowdown problem. Figure IV-1 is an isometric plot of the time-space-pressure surface. The initial pressure was 75×10^5 pascals. The initial fluid energy results in a saturation pressure of approximately 29×10^5 pascals. Figure IV-1 illustrates the progress of the depressurization wave from the open end to the closed end of the pipe. Figure IV-2 illustrates the liquid fraction associated with the depressurization.

Figures IV-3, IV-4, and IV-5 show a comparison of the pilot code results with results obtained using BEACON^[2] (a two-dimensional containment version of the KACHINA code)^[3,4] for a three-volume liquid-over-steam problem. In the liquid-over-steam problem,

Fig. IV-1 Pressures for two-phase blowdown of a straight pipe.

Fig. IV-2 Liquid fraction for two-phase blowdown of a straight pipe.

Fig. IV-3 Bottom volume void fraction comparison for three volume problem.

a closed vertical tube is initially filled with one-third liquid and two-thirds steam, with the liquid at the top. The acceleration of gravity causes inversion of the position of the two fluids. Two cases were compared: (a) no interphase drag and (b) a low drag corresponding to a droplet/bubble population of five per cubic metre. The drag used in the pilot code was reduced by a factor of 10 to obtain agreement between the two codes. Figures IV-3, IV-4, and IV-5 are comparisons of the transient void fraction in the bottom, center, and top cells, respectively. Note that the trends are very similar. The reduction of the pilot code drag by a factor of 10 to obtain agreement indicates some difference between the interphase drag formulations in the two codes. The agreement obtained for the zero-drag case indicates that the momentum formulations in the two codes are essentially the same.

Fig. IV-4 Middle volume void fraction comparison for three volume problem.

Fig. IV-5 Top volume void fraction comparison for three volume problem.

2. CONTAINMENT CODE DEVELOPMENT

W. J. Mings

Experiment results from tests conducted at the Marviken Containment Test Facility in Sweden and the Battelle Test Facility in Frankfurt, Germany, were used for developmental verification of operational computer containment codes. Test results and physical details of both facilities were reviewed. One test from each series was selected for evaluation. The tests used were the Marviken-II Blowdown 18 Experiment^[17] and the Battelle-Frankfurt Blowdown C-2 Experiment^[18]. The codes used were RELAP4/MOD5^[19] with the containment option, CONTEMPT-LT/Version 26^[20], and CONTEMPT4/MOD1. The latter code is not a production code. The Marviken test was modeled with all three codes. The

Battelle test was modeled with RELAP4/MOD5 and CONTEMPT4/MOD1. Analytical results were compared with each other and with measured values. The predicted values of pressures and temperatures within the containment were close to measured values. The models which were developed for this evaluation will be used for additional evaluations of other experiments from the Marviken-II test series and the Battelle-Frankfurt test series.

The analytical models were only as detailed as necessary to obtain satisfactory results, because computer running time increases rapidly with increased model detail. The models used for the Marviken analysis are shown schematically in Figures IV-11, IV-12, and IV-13. The CONTEMPT-LT model (Figure IV-11) consists of a drywell and a wetwell with an interconnecting vertical vent pressure suppression system. A more detailed model (Figure IV-12) was used for the RELAP4 analysis. This model consists of seven volumes, seven junctions, and four heat structures. The CONTEMPT4 model shown in Figure IV-13 includes five compartments, four junctions, and heat structures in two compartments.

The Battelle-Frankfurt experiment was modeled with RELAP4 and CONTEMPT4 as shown in Figures IV-14 and IV-15, respectively. The experimental facility consisted of nine compartments with interconnecting junctions and metal condensation surfaces of varying sizes.

Comparison of analytical results with the Marviken data shown in Figure IV-16 indicates that the best predictions were obtained with RELAP4. The CONTEMPT-LT results are good. The CONTEMPT4 results were promising but indicated that long-term analyses of this containment geometry is not advisable without further development effort.

The RELAP4 code cannot provide a good representation of the Battelle-Frankfurt data since it does not include a simulation of the steam condensation on the containment walls. Therefore, RELAP4 is used only for short-term comparison evaluations and the calculated results are shown for the first 5 seconds of the test. A number of runs were made using the CONTEMPT4 code, but good agreement with test data was not obtained. The pressure and temperature history in the rupture compartment appears reasonable. However, calculated temperatures in other rooms were too high. Work is underway to resolve the apparent problem. The comparison of analytical results and the Battelle-Frankfurt data (Figure IV-17) shows that RELAP4 predictions are within 2% of measured pressure in the rupture compartment for the first half of the blowdown. Thereafter, the results diverge because the heat transfer coefficient dial was not reset. The CONTEMPT4 results (Figure IV-17) are within 10% of measured values and will be improved with further code development.

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Fig. IV-6 CONTEMPT-LT model of Marviken Containment.

Fig. IV-7 RELAP4/MOD5 model of Marviken Containment.

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Fig. IV-8 CONTEMPT4 model of Marviken Containment.

Fig. IV-9 RELAP4/MOD5 model of Battelle-Frankfurt Containment.

Fig. IV-10 CONTEMP4 model of Battelle-Frankfurt Containment.

Fig. IV-11 Marviken-II Blowdown 18 Experiment, short-term containment pressure.

Fig. IV-12 Battelle-Frankfurt Blowdown C-2 Experiment, pressure in rupture compartment.

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