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TREE-NUREG-1147

for U.S. Nuclear Regulatory Commission

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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
APRIL — JUNE 1977**

August 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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Printed in the United States of America
Available from
National Technical Information Service
U.S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22161
Price: Printed Copy ~~\$5.00~~; Microfiche \$3.00

6.00

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PROGRAMS SPONSORED BY THE NUCLEAR REGULATORY COMMISSION'S
DIVISION OF REACTOR SAFETY RESEARCH, APRIL — JUNE 1977

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Distributed Under Categories:
NRC-2, -3, and -4
Water Reactor Safety Research
Systems Engineering
Fuel Behavior
Analysis Development

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

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August 1977

PREPARED FOR THE
U.S. NUCLEAR REGULATORY COMMISSION
AND
ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION
IDAHO OPERATIONS OFFICE
UNDER CONTRACT NO. EY-76-C-07-1570

ABSTRACT

Light water reactor research performed April through June 1977 is discussed. Results from the previously conducted Semiscale Mod-1 ECC injection test series were analyzed. Testing in the LOFT counterpart test series was essentially completed, and the steam generator tube rupture test series was begun. Two tests in the alternate ECC injection test series were conducted which included injection of emergency core coolant into the upper plenum through use of the low pressure injection system. The Loss-of-Fluid Test Program successfully completed nonnuclear Loss-of-Coolant Experiment L1-4. A nuclear test, GC 2-3, in the Power Burst Facility Reactor was performed to evaluate the power oscillation method of determining gap conductance and to determine the effects of initial gap size, fill gas composition, and fuel density on the thermal performance of a light water reactor fuel rod. Additional test results were obtained relative to the behavior of irradiated fuel rods during a fast power increase and during a high power film boiling transient. Fuel model development and verification activities continued for the steady state and transient Fuel Rod Analysis Program, FRAP-S and FRAP-T. A computer code known as RELAP4/MOD7 is being developed to provide best-estimate modeling for reflood during a postulated loss-of-coolant accident (LOCA). A prediction of the fourth test in the boiling water reactor (BWR) Blowdown/Emergency Core Cooling Program was completed and an uncertainty analysis was completed of experimental steady state stable film boiling data for water flowing vertically upward in round tubes. A new multinational cooperative program to study the behavior of entrained liquid in the upper plenum and cross flow in the core during the reflood phase of a pressurized water reactor LOCA was defined.

PREFACE

EG&G Idaho, Inc. 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 research activities of EG&G Idaho, Inc. are accomplished in five programs: the Semiscale Program, the Loss-of-Fluid Test (LOFT) Experimental Program, the Thermal Fuels Behavior Program, the Reactor Behavior Program, and the 3-D Experimental Project.

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 a pressurized water reactor (PWR) under 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 provides the analytical research aimed at 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 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.

The 3-D Project is a new multinational cooperative water reactor research project initiated during this quarter which is designed to study the behavior of entrained liquid in a full-scale upper plenum and cross flow in the core during the reflood phase of a PWR LOCA.

A more detailed description of the first 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),

TREE-NUREG-1070 (for October--December 1976), and TREE-NUREG-1128 (for January--March 1977). Copies of the quarterly reports are available from the Technical Information Center, Energy Research and Development Administration, Oak Ridge, Tennessee.

SUMMARY

Water reactor research activities by EG&G Idaho, Inc. at the Idaho National Engineering Laboratory for April through June 1977 are reported. For the reader's convenience, this summary is divided into program headings.

Semiscale Program

Results from the previously conducted alternate ECC injection test series were analyzed. Testing in the LOFT counterpart test series was essentially completed, and the steam generator tube rupture test series was begun.

Two tests in the alternate ECC injection test series were conducted with the Mod-1 system modified to represent the two-loop nuclear plant configuration. Analysis of the test results from this configuration relative to those from the four-loop configuration normally used in Semiscale indicated the two-loop system produced a significantly different core thermal response. A very rapid blowdown occurred as a result of large flow rates at the break. Large negative intact loop and core flow also developed resulting in good cooling due to delayed departure from nucleate boiling and intact loop water being swept into the core.

A portion of the analysis of the alternate ECC injection concepts investigated included an evaluation of the effectiveness of injection of ECC into the upper plenum through use of the low pressure injection system (LPIS) in conjunction with injection into the cold leg of the intact loop through use of the accumulator system. ECC injection into the upper plenum by means of the LPIS had little effect on the general system hydraulic response; however, the core thermal response was altered in that LPIS water swept into the core resulted in rapid quenching of that part of the core immediately below the injection location. Cooling on the side of the core opposite the injection location was not as good and periods of LPIS ECC bypass occurred. The multidimensional core cooling caused by preferential channeling of ECC fluid through the core

indicated that long-term upper plenum LPIS injection has the potential of providing fluid through the core to the lower plenum which could contribute to a bottom upward core reflood.

LOFT Experimental Program

The LOFT Program successfully completed nonnuclear Loss-of-Coolant Test L1-4. This experiment, the fourth in a series of five nonnuclear experiments highly successful in satisfying all of its specified experimental objectives. The pretest prediction analysis for Test L1-4 was improved over pretest prediction analyses performed for the previous LOFT nonnuclear experiments.

The instrumentation for the replacement center fuel modules to be used in the two full-power experiments in the first nuclear experiment series is described.

Thermal Fuels Behavior Program

A nuclear test, Test GC 2-3, was performed in the Power Burst Facility reactor to evaluate the power oscillation method of 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. Four BWR-type rods were tested simultaneously to obtain steady state and power oscillation data for evaluation of test fuel rod behavior. Comparisons with data obtained from previous tests, Tests GC 2-1 and GC 2-2, are presented with preliminary evaluations.

Postirradiation examinations of Test IE-2 fuel rods further defined the lower film boiling boundary. Microhardness evidence suggests that the lower film boiling boundary extends only slightly below the transition zone from stress relieved to alpha annealed microstructure.

Additional test results from Test IE-3 permit evaluation of the behavior of irradiated fuel rods during a fast power increase and during

a high-power film boiling transient. Test results indicate that the fast (20 kW/m per minute) power ramp did not cause fuel rod failure and had no obvious effect on steady state operation following the ramp. Comparison of results with those from previous tests indicates that the fast power ramp did cause increased cladding elongation. No posttest evidence was observed of expected pellet-cladding interaction failure as a result of the fast power ramp.

Development and evaluation efforts were concentrated in the areas of Power Burst Facility program development, coordination with foreign experimental programs, Nuclear Regulatory Commission technical assistance, analysis of test results (topical reports), Halden fuel behavior research, and postirradiation examination of commercial power reactor fuel.

Fuel model development and verification activities continued for the steady state and transient Fuel Rod Analysis Program, FRAP-S and FRAP-T. The FRAP-T3 tape package was transmitted to the Argonne Code Center. Calibration and independent verification of FRAP-S3 is in progress.

Reactor Behavior Program

A computer code known as RELAP4/MOD7 is being developed to provide a best-estimate model for reflood during a postulated loss-of-coolant accident in a boiling water reactor. The models in the code describe radiation heat transfer from surface to surface and from surface to fluid within a rod bundle. Radiation absorption by steam containing dispersed water droplets is calculated as a function of fluid properties, quality, and surface temperatures. The effects of a surface partially and completely wetted by the top quench front are considered in the calculations. Convection heat transfer is calculated for dry surfaces, partially wet surfaces, and completely wet surfaces.

A prediction of the fourth test in the BWR Blowdown/Emergency Core Cooling Program was completed and compared with predictions of the first

test in the test series. An uncertainty analysis of experimental steady state stable film boiling data for water flowing vertically upward in round tubes was completed.

3-D Experimental Project

The 3-D experimental project is a new cooperative effort between the United States, Germany, and Japan being pursued to study the behavior of entrained liquid in the upper plenum and cross flow in the core during the reflood phase of a PWR LOCA. During this quarter, the overall direction of the project was defined.

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DIVISION OF REACTOR SAFETY RESEARCH, APRIL - JUNE 1977

I. SEMISCALE PROGRAM

D. J. Olson, Manager

Major Semiscale Mod-1 program activities included posttest analysis of results from the alternate ECC injection test series (Test Series 5) and performance of tests and preparation of preliminary reports for both the LOFT counterpart test series (Test Series 6) and the steam generator tube rupture test series (Test Series 28). In addition, design of the Mod-3 system was essentially completed and procurement and contract award activities for components of the Mod-3 system were initiated.

The testing and preliminary documentation phases of the alternate ECC injection test series were completed last quarter. Four alternate ECC concepts were investigated in this test series, including ECC injection into the lower plenum, combined ECC injection into both the upper plenum and cold leg, ECC injection into the cold leg with venting between the broken loop hot and cold legs, and injection of ECC into the intact loop pump suction. Two of the tests in this series were conducted with the Mod-1 system modified to represent the two-loop nuclear plant configuration as nearly as possible. Analysis of the response of the Mod-1 two-loop representation relative to that of the Mod-1 four-loop representation normally used is included in Section I, and a portion of the analysis that evaluated the effectiveness of injection of ECC into the upper plenum by the low pressure injection system (LPIS) in conjunction with ECC injection by the cold leg accumulator system is included in Subsection I-2.

Testing and preliminary documentation for five tests in the LOFT counterpart test series were completed. One of these tests, Test S-06-3, was conducted as an NRC Standard Problem and another test, Test S-06-6, was added in support of the Loss-of-Fluid Test (LOFT) program. The primary objective of the LOFT counterpart tests was to assist the LOFT

Program in the planning of the first LOFT nuclear test series (Series L-2). The first test conducted in this test series was used to investigate the influence on system response of a break nozzle scaled to the LOFT break geometry. Three of the remaining four tests were designed to duplicate as closely as possible the first three tests scheduled to be run in the LOFT L-2 test series. The fourth test was conducted in support of the LOFT Program to evaluate the influence of hardware assumptions required in Appendix K to 10CFR50^[I-1]. One test, corresponding to the final test to be performed in the LOFT L-2 test series, remains to be performed.

Experimental operating specifications were prepared for the steam generator tube ruptures test series. The primary objective of the test series is to determine, in the Semiscale Mod-1 system, the sensitivities of core peak cladding temperatures to the magnitude of the flow rate from a simulated tube rupture in the steam generator. Four tests in this test series were completed and analysis of results is currently being conducted.

1. AN INVESTIGATION OF THE EFFECTS ON SYSTEM RESPONSE OF
BREAK AREA SCALING TO A 200% COLD LEG BREAK
IN A TWO-LOOP PLANT

P. North
E. A. Harvego

An integral blowdown-reflood test (Test S-05-6) was conducted to determine the effect of a 200% cold leg break configuration scaled to a two-loop pressurized water reactor (PWR) on the thermal and hydraulic behavior in the Semiscale Mod-1 system. The two-loop plant scaling was of interest because this scaling resulted in a significantly larger break size than does the four-loop plant scaling normally used in the Semiscale Mod-1 system. Test S-05-6 employed an accumulator and a low pressure injection system for injection of ECC into the cold legs of both the intact loop and the broken loop. Since these injection locations were identical to those used in the four-loop baseline test configuration for the baseline ECC injection test series (Test S-04-6)^[I-2], an assessment of the two-loop plant scaling could be made by a comparative analysis of the results from the two tests.

In addition to the larger break area used in Test S-05-6 (relative to a four-loop scaled break), modifications to the intact loop resistance and ECC injection rates and volumes were made to make the Semiscale Mod-1 system as representative of a two-loop system as possible. These modifications are discussed in the following section, which describes the experimental configuration for the two-loop Semiscale representation.

1.1 Experiment Description

The Semiscale Mod-1 system, shown in Figure I-1, is basically modeled after a four-loop PWR system and is primarily scaled from the LOFT facility. Since the system volumes in Semiscale could not be readily modified, the volumes of the intact loop and vessel are about twice the scaled volumes necessary to represent a two-loop PWR. Therefore, to

Fig. I-1 Semiscale Mod-1 system for cold leg break configuration.

make the system blowdown and reflood response as representative as possible of that in a two-loop system, careful specification of the break area and the system resistances was required.

The Semiscale break area was scaled to a 200% double-ended cold leg break in a typical two-loop PWR on the basis of the ratio of system volumes. The volume scaling of the break area is particularly important during the blowdown portion of the transient when the depressurization rate and the system flow magnitudes and directions are directly influenced by the break flow rates.

The intact loop hydraulic resistance was scaled to a two-loop PWR on the basis of the ratio of core flow areas in order to produce pressure drops representative of the reflood portion of the transient. The vessel hydraulic resistance was fixed and was about one quarter of that which would have resulted had core area scaling methods been applied. Core area scaling was used in the specification of the ECC system volumes and flow rates, and allowances in the accumulator volumes were made for such factors as the delayed delivery of coolant due to the hot Semiscale downcomer walls.

1.2 Results from the Two-Loop Scaled Break Test (Test S-05-6)

In Test S-05-6 conducted to determine the thermal and hydraulic behavior in the Semiscale Mod-1 system with a 200% cold leg break scaled to a two-loop plant, two major phenomena were observed. First, a very rapid blowdown occurred as a result of large flow rates at the break and second, a large and sustained negative core flow occurred. Both of these phenomena affected the core thermal response. In this section the phenomena are first discussed in some detail and then the core thermal response is presented and explained.

The blowdown process with the two-loop scaled break caused the system pressure to reach that of the suppression system about 17 s after rupture, compared with 35 s for a four-loop scaled break (Test S-04-6).

The more rapid blowdown was predominately the result of the increased flow out of the cold leg side of the break shown in Figure I-2 for the two-loop scaled break.

Fig. I-2 Comparison of cold leg break flows for two-loop and four-loop scaled breaks.

In contrast, the flow through the hot leg toward the break did not increase during the blowdown period and in fact even reversed later in the transient. Two factors, both ECC related, contributed to this behavior. The first factor was that ECC, injected in the downstream side of the broken loop pump simulator, flowed back into the simulator thereby increasing its resistance. The second factor was that ECC injected into the intact loop caused significant condensation to occur in the downcomer which induced flow through the core rather than allowing flow out the hot leg side of the break.

The large flow out the cold leg side of the break and the condensation in the downcomer resulted in a large negative core flow which developed

early in the transient and was sustained for some 70 s after rupture, as shown in Figure I-3. Both density measurements and core heat transfer

Fig. I-3 Comparison of core inlet volumetric flow rates for two-loop and four-loop scaled cold leg breaks.

calculations indicated that the negative flow consisted mostly of high quality steam. Condensation in the downcomer was the principal driving mechanism by which the negative flow was sustained for an extended period after the end of blowdown. The major source of the fluid which flowed into the core during the extended core flow period appears to have been the intact loop hot leg; however, the mass necessary to sustain this flow was small due to the high quality of the fluid.

During the blowdown, early and stable departure from nucleate boiling (DNB) was completely absent for the two-loop scaled break. The delayed DNB, compared with that obtained with four-loop break scaling, was accompanied by early turnover of the temperatures and by lower peak blowdown temperatures as shown in Figure I-4. This modified thermal behavior was caused by the increased negative core flow resulting from the increased flow out the cold leg side of the break.

Fig. I-4 Comparison of core thermal response for two-loop and four-loop scaled cold leg breaks.

During the latter portion of the depressurization (between 10 and 15 s after rupture) a rapid drop in core heater rod temperatures was observed to occur. This rapid drop in temperatures throughout the core was unique to the two-loop test although similar effective cooling of the upper portions of the core was observed during tests scaled to a four-loop PWR. The increased extent of early and effective core cooling during the two-loop simulation test was a direct result of the strong negative intact loop and core flows which carried liquid from the intact loop hot leg and distributed it throughout the core. Apparently the pressurizer, which was attached to the intact loop hot leg, was a major contributor to the low quality hot leg fluid which flowed into the core.

After the bulk of the low quality intact loop hot leg fluid was exhausted (about 15 s after rupture), the continued negative core flow basically consisted of steam which provided little core cooling. As a result the rod cladding temperatures increased as shown in Figure I-5.

Fig. I-5 Comparison of rod cladding temperature response (73.7 cm above bottom of heated core).

This relatively poor core cooling when compared with that which occurred during tests with a four-loop configuration (also shown in Figure I-5) lasted until nitrogen from the intact loop accumulator began to flow into the intact loop cold leg and forced liquid from the downcomer into the core.

The increase in liquid, supplied to the core when nitrogen began to flow into the intact loop cold leg, resulted in improved core cooling which persisted for the duration of the nitrogen injection period. Following the period of nitrogen injection, core reflood from the cold leg LPIS eventually caused the temperatures to turn over and established a very slow and uniform bottom up quenching process as illustrated in Figure I-6. The quenching process appears almost linear and the lack of significant quenching above the core high power zone (79 cm above the bottom of the core) indicates relatively little entrainment took place. For comparison, the approximate quench pattern envelope for the four-loop configuration is also shown in Figure I-6. The envelope of quenches

Fig. I-6 Axial quenching pattern for two-loop and four-loop scaled cold leg breaks.

for the four-loop configuration shows concurrent quenching at both high and low elevations which is indicative of significant entrainment.

1.3 Conclusions

Overall, the core thermal response for the two-loop Semiscale test configuration (Test S-05-6) was significantly different than that of previous tests which represented a four-loop configuration. For the two-loop Semiscale test configuration, a very rapid blowdown occurred as a result of large flow rates at the breaks. Large negative intact loop and core flows also developed, which resulted in good initial cooling due to delayed DNB and intact loop water being swept well into the core. Reflood was eventually initiated and sustained by the intact loop LPIS flow which produced a very slow bottom up quenching with little evidence of entrainment. The slow bottom up quenching process quenched only the lower half of the core during the 300-s experiment.

2. AN INVESTIGATION OF THE EFFECTS OF INJECTION OF ECC
INTO THE UPPER PLENUM BY THE LPIS WITH BREAK AREA
SCALED TO A 200% COLD LEG BREAK
IN A TWO-LOOP PLANT

P. North

E. A. Harvego

An experimental investigation was conducted to determine the effect on the system thermal and hydraulic behavior of injecting ECC into the Semiscale upper plenum region. For this investigation, LPIS water was injected into the upper plenum and accumulator water was injected into the cold leg of the intact and broken loops. Since this ECC injection concept is similar to the concept used in some two-loop PWRs, the break area, intact loop resistance, and ECC volumes and flow rates for this test (Test S-05-7) were scaled to an existing two-loop plant.

The scaling criteria used in Test S-05-7 were the same as those used in the two-loop plant scaling for Test S-05-6 with cold leg ECC injection (discussed in the previous section). Therefore, the system configuration and major system variables used in the Test S-05-7 two-loop representation were identical to those used in Test S-05-6, but the LPIS fluid was injected into the upper plenum rather than into the cold legs. Through this single change, the effects of upper plenum LPIS injection on the system thermal-hydraulic response could be isolated by direct comparison of results from Test S-05-7 with results from Test S-05-6.

The upper plenum LPIS employed in Test S-05-7 injected ECC horizontally through a nozzle located 34.3 cm above the centerline of the intact loop cold leg as shown in Figure I-7. This location introduced LPIS fluid to the upper plenum about 2.27 m above the heated core. Since the heater rod extensions run through the upper plenum and out the top of the vessel, the LPIS fluid injected into the upper plenum impinged directly on the rod extensions.

Fig. I-7 Semiscale vessel showing upper plenum injection location.

2.1 Results From the Two-Loop Scaled Break Test with Upper Plenum LPIS Injection (Test S-05-7)

Comparison of the system hydraulic behavior for Tests S-05-6 and S-05-7 indicates that the system hydraulic response was essentially unchanged when the LPIS injection location was moved from the cold leg to the upper plenum. Negative core flow again developed early and was sustained until about 80 s after rupture. The magnitude of the negative flow at the core inlet was somewhat larger after upper plenum LPIS ECC injection was initiated at about 25 s after rupture. The increased negative core flow appeared to be caused by additional steam generation in the core due to the presence of LPIS fluid; however, the phenomena driving the flow appeared to be essentially the same as in Test S-05-6.

Comparison of the core thermal response for the two tests indicates that the heater rod temperatures were directly affected by the injection of ECC into the upper plenum. After initiation of LPIS injection, the negative core flow, originating largely in the intact loop hot leg, swept LPIS fluid directly into the core. Since the LPIS injection

velocity was low (about 2.4 m/s), the large negative core flow carried the fluid into the regions of the core immediately below the injection location. Consequently, very rapid top downward quenching occurred at all elevations directly below the injection location shortly after LPIS injection began. This early quenching is illustrated in Figure I-8

Fig. I-8 Comparison of rod cladding temperature response in the core region below the LPIS injection location (73.7 cm above bottom of heated core).

which compares the temperature history of a heater rod located below the injection point for Tests S-05-6 and S-05-7. In contrast, very little LPIS fluid initially penetrated to the side of the core opposite the upper plenum LPIS injection point. The lack of penetration into this region of the core when upper plenum LPIS was used is demonstrated by the similarities in core thermal behavior for the initial 150 s of Tests S-05-6 and S-05-7 shown in Figure I-9.

Fig. I-9 Comparison of rod cladding temperature response in the core region opposite the LPIS injection location (53.3 cm above bottom of heated core).

The very different thermal behavior of the regions of the core below and opposite the LPIS injection location can be seen in the presentation of the axial quenching pattern for Test S-05-7 in Figure I-10. As the test proceeded, the LPIS fluid swept into the core by the negative flow provided good cooling and resulted in a rapid top downward quench in the region below the LPIS injection location as illustrated by the large number of quenches in this region prior to 80 s. The strong negative core flow ended when nitrogen injection from the accumulator began in the intact loop cold leg and a period of sporadic quenching activity followed. Eventually, the continued LPIS injection into the upper plenum caused fluid to penetrate into the core even in the absence of negative flow, and resulted in a second, slower, and more scattered top downward quench later in the transient.

As indicated, the core thermal behavior during the upper plenum LPIS injection period appeared to be closely related to the fluid flow

Fig. I-10 Axial quenching pattern for two-loop configuration with upper plenum LPIS injection (Test S-05-7).

characteristics in and around the upper plenum region. Further evidence of the relation between the system flow characteristics and core thermal response is provided by the density measurement near the vessel in the intact loop hot leg as shown in Figure I-11. From 25 s after rupture

Fig. I-11 Measured density in intact loop hot leg next to vessel, indicating LPIS bypass (Test S-05-7).

when the LPIS injection started, to about 80 s after rupture when nitrogen injection in the cold leg began, the density measurement indicates that with large negative intact loop and core flows, little bypass of LPIS fluid occurred. Most of the LPIS fluid during this period was, therefore, available to the core to provide good cooling and rapid quenching. After the start of nitrogen injection, the negative intact loop and core flows terminated and bypass of the LPIS fluid out the intact and broken loop hot legs occurred as indicated by the rise in measured density in Figure I-11. Therefore, a decrease occurred in the supply of LPIS fluid to the core resulting in the less effective cooling. About 170 s after rupture, the oscillations in the density measurement indicate periods of reduced bypass which correspond to the start of the second top downward quenching process. Eventually, as more of the core was quenched, the countercurrent flow caused by steam generation in the core was reduced and, therefore, the LPIS bypass decreased as indicated by the decline in

the measured density. The increased supply of LPIS fluid to the core again improved the cooling and quenching process.

In summary, injecting the LPIS fluid into the upper plenum, rather than into the cold leg, caused little change in the overall system hydraulic response but resulted in significant changes in the core thermal response. Multidimensional core cooling resulted in good cooling in the region of the core below the LPIS injection location but less effective cooling elsewhere in the core. Periods of reduced cooling effectiveness corresponded to periods of LPIS bypass.

2.2 Conclusions

In Test S-05-7, which was identical to Test S-05-6 with the single exception that ECC from the LPIS was injected into the upper plenum, the upper plenum LPIS injection had little effect on the general system hydraulic response. However, the core thermal response was substantially affected. LPIS water swept into the core resulted in a rapid quenching of that part of the core immediately below the injection location. The cooling on the opposite side of the core was not as good, however, and periods of significant LPIS bypass occurred. The multidimensional core cooling caused by preferential channeling of ECC fluid through the core was significant in that it indicated that long-term upper plenum LPIS injection (longer than 300 s) has the potential of providing fluid through the core to the lower plenum which could contribute to a bottom upward core reflood.

3. REFERENCES

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- I-2. H. S. Crapo, B. L. Collins, K. E. Sackett, Experiment Data Report for Semiscale Mod-1 Tests S-04-5 and S-04-6 (Baseline ECC Tests), TREE-NUREG-1045 (January 1977).

II. LOFT EXPERIMENTAL PROGRAM

L. P. Leach, Manager

The LOFT Program successfully completed the fourth in the series of five nonnuclear loss-of-coolant experiments (LOCEs) on May 3, 1977. This experiment (designated as Test L1-4) was highly successful in satisfying all of its specified experimental objectives. Test L1-4 simulated a complete double-ended offset shear of the primary coolant piping between the pump and reactor vessel of a four-loop pressurized water reactor.

The RELAP4 LOFT system model was improved to provide a better model for predicting LOFT system thermal-hydraulic responses during a LOCE. The improved model was used for the L1-4 Test prediction analysis.

Plans for instrumenting the replacement center fuel modules (Type A_3 and F_1) for the LOFT nuclear core have been completed. These fuel modules will be used in the two full-power LOCEs in the first nuclear experiment series. The modules will contain additional instrumentation to measure fuel rod thermal and mechanical behavior during the LOCEs.

1. EXPERIMENT PREDICTION FOR LOFT TEST L1-4

J. R. White

A computer analysis using the WHAM and RELAP4 computer codes was performed to predict the LOFT system thermal-hydraulic responses for Test L1-4^[II-1]. A brief description of Test L1-4 is provided in Section 2. The model of the LOFT system used in the RELAP4 prediction analysis is shown in Figure II-1.

The system thermal-hydraulic response during Test L1-4 saturated blowdown was calculated with the RELAP4/MOD5 [LOFT SV02(73)] code. It is identical to RELAP4/MOD5^[II-2] except for the following:

INEL-A-600

Fig. II-1 RELAP4 model used in the L1-4 pretest predictions.

- (1) RELAP4/MOD5 [LOFT SV02(73)] incorporates a variable inertia pump model to improve the capability to model plant features unique to LOFT (e.g., intact loop pump driver system)
- (2) RELAP4/MOD5 [LOFT SV02(73)] incorporates an accumulator isentropic expansion model for the accumulator nitrogen pressure calculation.

A number of changes were made for the pretest prediction for Test L1-4 as compared to the pretest prediction for Test L1-3^[II-3]. A few of these changes are as follows:

- (1) The modeling of the emergency core coolant (ECC) system was changed to inject into the intact loop cold leg
- (2) An adiabatic accumulator nitrogen expansion model was used in the ECC accumulator
- (3) The downcomer was divided circumferentially into two parts (1/3 under the intact loop and 2/3 under the broken loop). This permits the code to predict simultaneous downflow of subcooled ECC flow on one side of the downcomer and an upflow of a saturated two-phase mixture on the other side of the downcomer
- (4) An extra volume was added to simulate the ECC injection line and an extra volume was added to the intact loop cold leg. These changes were made to obtain more accurate results at the ECC injection point
- (5) A new pump model (the variable inertia model) was used in the Test L1-4 pretest prediction.

Only changes to the codes or code input models were made that represented physical phenomena effects or physical differences between tests.

In modeling LOFT Test L1-4 with the RELAP4/MOD5 [LOFT SV02(73)], the Henry-Fauske-HEM critical flow model was used at all junctions. For the junctions at the break planes, a contraction coefficient of 0.85 was applied at the cold leg, and a contraction coefficient of 0.75 was applied to the hot leg. These contraction coefficients are an attempt to model the geometry effects which are assumed to occur in the vicinity of the break planes.

In general, the predictions of saturated and subcooled blowdown for Test L1-4 are consistent with the expected system behavior, and predicted trends agreed with results of past LOFT experiments and Semiscale Test S-01-4A^[II-4], which simulated the L1-4 experiment conditions.

2. PRELIMINARY RESULTS OF LOFT NONNUCLEAR TEST L1-4

H. C. Robinson

Test L1-4 was the fourth in a series of five planned nonnuclear LOCEs designed to generate large scale isothermal blowdown and reflood data from the LOFT Facility prior to initiation of the first nuclear test. (The LOFT system and nonnuclear test series is described in Reference II-5.)

Test L1-4 simulated a complete double-ended offset shear of a pressurized water reactor inlet pipe. In this experiment the nuclear core was simulated using a system of orifices that gives the reactor vessel a pressure drop equivalent to that which it would have if a nuclear core was installed. The initial conditions in the system were: pressure -- 15.65 MPa, temperature - 279°C, and intact loop flow rate - 268.4 kg/s.

All the measured experiment conditions at LOCE initiation were within the tolerances specified for Test L1-4 except the following:

- (1) The primary coolant system gauge pressure was 15.65 MPa, instead of 15.6 MPa
- (2) The primary coolant temperature was 279°C, instead of 281°C
- (3) The accumulator water volume injected was 2.05 m³, instead of 2.04 m³
- (4) The water level in the steam generator secondary side was 3.02 m, instead of 2.97 m
- (5) The suppression tank liquid level was 1.41 m, instead of 1.37 m.

None of the above discrepancies significantly affected the experiment. The instrumentation and data acquisition system performed well. Of 551 instrument channels, only 25 failed prior to or during the experiment.

Test L1-4 was successful in meeting all of the objectives (stated in Volume 2 of the Experiment Operating Specification^[II-6].) Preliminary results from the experiment are summarized below:

- (1) Test L1-4 provided data to evaluate downcomer bypass of ECC. Further analysis will be needed however to evaluate more carefully the mechanism influencing bypass.
- (2) There was more complete mixing of primary coolant and ECC in the lower plenum during Test L1-4 than was observed from the Test L1-3A^[II-7] data. There was less complete mixing of primary coolant and ECC in the downcomer region and intact loop cold leg in Test L1-4 than in Test L1-3A. The liquid volume fraction in the lower plenum and the

liquid mass in the reactor vessel are plotted as "trend data"^[a] in Figures II-2 and II-3, respectively.

- (3) Data reproducibility between Tests L1-3A and L1-4 was very good for the first ~23 s into the blowdown.
- (4) The data from the RELAP4 pretest prediction for Test L1-4^[II-1] and from the counterpart test (S-01-4A)^[II-4] performed in Semiscale were found to be in good comparison with the Test L1-4 data. This comparison is illustrated in Figures II-4 through II-8. A discussion of the RELAP4 pretest prediction is presented in Subsection II-1.
- (5) There were no discernible effects on Test L1-4 from operating the LOFT system with boric acid or the LOCE control system with plant protection system backup.

[a] Data plots labeled as "trend data" indicate that the instrument showed the general trend of the data but for various reasons could not be qualified.

Fig. II-2 Liquid volume fraction in lower plenum.

Fig. II-3 Liquid mass in reactor vessel.

Fig. II-4 Liquid volume fraction in lower plenum.

Fig. II-5 Density in intact loop cold leg.

Fig. II-6 Pressure in pressurizer.

INEL-A-4396

Fig. II-7 Temperature in broken loop cold leg.

INEL-A-4395

Fig. II-8 Pump speed for primary coolant Pump 1.

INEL-A-4397

3. LOFT FUEL ROD INSTRUMENTATION EXPANSION

M. L. Russell

New instrumentation arrangements are being incorporated in new LOFT center fuel modules. The two types of new fuel modules are unpressurized and pressurized, designated Type A₃ and Type F₁, respectively. The plans for instrumenting the Type A₃ and F₁ center fuel modules include measurement of the fuel rod thermal and mechanical behavior during the LOCEs in addition to the cladding surface temperature measurements included on the Type A center fuel module for the previous experiments. The specific additional parameters now planned to be measured are:

- (1) Fuel rod axial motion
- (2) Fuel rod centerline temperature

(3) Fuel rod plenum gas pressure^[a]

(4) Fuel rod plenum gas temperature.

The selection of the measurement locations considered the "strategic" rod positions and the fuel module physical constraints. The strategic rod positions were determined as follows:

- (1) Hottest fuel rods - predicted highest core heat flux locations. These rods are all adjacent to guide tubes.
- (2) Hottest surrounded rods - highest core heat flux locations where fuel rod is completely surrounded by the other fuel rods.
- (3) Early critical heat flux (CHF) rods - a fuel rod position located between the hottest and the hottest surrounded rods which gave the first indication of departure from nucleate boiling (DNB) in a significant number of DNB test runs.

A comparison of fuel rod instrument locations for the old (Type A) and new (Type A₃ and F₁) center fuel modules is tabulated in Table II-1 and shown graphically in Figures II-9 and II-10. The data indicate that 16 of the 19 Type A center fuel bundle instrumented fuel rods are located in the strategic rod positions. The fuel rod axial motion measurement locations are a result of fuel module physical constraints.

A summary of the number and type of fuel bundle instruments is presented in Table II-2.

[a] Pressurized rods only, Type F₁ fuel module.

TABLE II-1

LOFT CENTER FUEL BUNDLE FUEL ROD INSTRUMENTATION ARRANGEMENT COMPARISON

Fuel Rod Classification	Quantity	Existing (Type A)	Proposed (Type A ₃ and F ₁)				
			Instrumented Fuel Rod Type				
		4TC ^[a]	4TC ^[b]	& 3TC	P&T ^[c,d]	P&T & 2TC ^[d]	LVDT ^[e]
Hottest rod (1.00 P _f)	16	4	2	6	2	2	
Hottest surrounded rod (0.94 P _f)	12	9	2	3	2	2	
Early CHF rod (0.98 P _f)	4	3	1	1		1	
Hot rods (0.99 and 0.98 P _f)	24			2	2		2
Intermediate rods (0.97, 0.96, and 0.95 P _f)	28			1			1
Low power rods (<0.95 P _f)	120	3		4	2		

[a] TC - Surface thermocouple.
 [b] C - Centerline thermocouple.
 [c] P&T - Plenum gas pressure and temperature.
 [d] Not included on Type A₃ (unpressurized).
 [e] LVDT - Linear variable differential transformer (fuel rod length).

	A	B	C	D	E	F	G	H	I	J	K	L	M	N	O
1	0.80	0.82	0.84	0.85	0.86	0.87	0.88	0.87	0.88	0.87	0.86	0.85	0.84	0.82	0.80
2	0.82	0.85	0.86	0.88	0.91	0.93	0.92	0.91	0.92	0.93	0.91	0.88	0.88	0.85	0.82
3	0.84	0.88	X	0.94	0.96	X	0.98	0.97	0.98	X	0.96	0.94	X	0.88	0.84
4	0.85	0.86	0.94	0.97	0.99	1.00 TY 14	1.00	X	1.00	1.00 TY 14	0.99	0.97	0.94	0.88	0.85
5	0.86	0.91	0.96	0.99	X	0.99	0.97	0.98 TY 1A	0.97	0.99	X	0.99	0.96	0.91	0.86
6	0.87	0.93	X	1.00 TY 15	0.99	0.96	0.94 TY 4A	0.94 TY 5A	0.94 TY 2A	0.96	0.99	1.00 TY 15	X	0.93	0.87
7	0.88	0.92	0.98	1.00	0.97	0.94 TY 2H	0.93 TY 3A	0.93 TY 3A	0.93 TY 4A	0.94 TY 5A	0.97	1.00	0.98	0.92	0.88
8	0.87	0.91	0.97	X	0.98 TY 1B	0.94 TY 5E	0.93 TY 3B	X	0.93 TY 3A	0.94 TY 5A	0.98 TY 1A	X	0.97	0.91	0.87
9	0.88	0.92	0.98	1.00	0.97	0.94 TY 3E	0.93	0.93	0.93	0.94 TY 2A	0.97	1.00	0.98	0.92	0.88
10	0.87	0.93	X	1.00	0.99	0.95	0.94	0.94	0.94	0.96	0.99	1.00	X	0.93	0.87
11	0.86	0.91	0.96	0.99	X	0.99	0.97	0.98	0.97	0.99	X	0.99	0.96	0.91	0.86
12	0.85	0.88	0.94	0.97	0.99	1.00	1.00	X	1.00	1.00	0.99	0.97	0.94	0.88	0.85
13	0.84	0.88	X	0.94	0.96	X	0.98	0.97	0.98	X	0.96	0.94	X	0.88	0.84
14	0.82	0.85	0.88	0.88	0.91	0.93	0.92	0.91	0.92	0.93	0.91	0.88	0.88	0.85	0.82
15	0.80	0.82	0.84	0.85	0.86	0.87	0.88	0.87	0.88	0.87	0.86	0.85	0.84	0.82	0.80

Surface thermocouple and instrumented fuel rod type

Hottest rods (1.00 Pf)

Hottest (0.94 Pf) surrounded rods at
F7, 8 and 9: G6 and 10: H6 and
10: 16 and 10: J7 8 and 9

Early CHF (0.98 Pf) rods at
E8, H5, H11 and K8

Fuel rod power factor

Fig. II-9 LOFT existing Type A center fuel bundle instrumented fuel rod locations.

Fig. II-10 LOFT proposed Type A₃ and F₁ center fuel bundle instrumented fuel rod locations.

TABLE II-2

LOFT CENTER FUEL BUNDLE INSTRUMENTATION SUMMARY

Measurement	Existing (Type A)	Proposed	
		(Type A ₃)	(Type F ₁)
Fuel rod surface TC	76	71	81
Fuel rod centerline TC	0	17	17
Fuel rod plenum pressure and temperature	0	0	13
Fuel rod length (LVDT)	0	3	3
Liquid level	1 (19) ^[a]	2 (38)	1 (19)
Guide tube TC	8	8	8
Inlet coolant TC	4	4	4
Outlet coolant TC	8	8	8
Fixed flux	1	1	1
Flux scan	2 (0)	2 (0)	2 (0)
Inlet flow	0	2 (6)	2 (6)
Inlet void fraction	0	2 (4)	2 (4)
Outlet flow	1 (3)	1 (3)	1 (3)
Total cables	119	163	167

[a] Numbers in parentheses indicate the number of cables required for the particular instruments. All other instruments require one cable each.

4. REFERENCES

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III. THERMAL FUELS BEHAVIOR PROGRAM

J. O. Zane, Manager

Accomplishments of the Thermal Fuels Behavior Program (TFBP) during April to June 1977 include the completion of Test GC 2-3 in the Power Burst Facility (PBF) Reactor. The GC 2-3 test was conducted to evaluate the power oscillation method of determining gap conductance and to determine the effects of initial gap size, fill gas composition, and fuel density on the thermal behavior of a light water reactor (LWR) fuel rod.

The following sections describe: (a) PBF testing efforts, (b) activities in the area of program development, coordination with foreign experimental programs, Nuclear Regulatory Commission technical assistance, topical reports, Halden fuel behavior research, and postirradiation examination of commercial power reactor fuel, and (c) fuel model development and verification activities.

1. PBF TESTING

P. E. MacDonald, W. J. Quapp

Test GC 2-3 was conducted in the PBF and postirradiation examinations continued on fuel rods from Tests PCM-3, PCM-4, IE-2, IE-3, IE-5, GC 2-1, and GC 2-2 (tests conducted in previous quarters). Section 1.1 presents a summary of the ongoing activities associated with the conduct of the PBF tests and the analysis of the results. Section 1.2 presents preliminary results from the Gap Conductance Test GC 2-3. Section 1.3 presents results from the Irradiation Effects Test IE-2 postirradiation examinations, and Section 1.4 presents a brief description of the IE-3 test results.

1.1 Summary of Ongoing Activities

1.1.1 Power-Cooling-Mismatch (PCM) Test Series. The initial test results report for Test PCM-4^[III-1] was issued. This report describes the design, conduct, test results, a comparison of Fuel Rod Analysis

Program Transient (FRAP-T) computer code calculations with the experimental results, and the conclusions that were obtained from the test results. The initial draft of the PCM-3 postirradiation examination (PIE) report was completed. This report describes the metallurgical examination results including estimates of the maximum cladding temperatures attained and the conclusions drawn from the posttest condition of the test fuel rods. The PIE for Test PCM-4 was completed. Preparations for future tests in the series (PCM-5, PCM-6, and PCM-7) continued. These nine-rod cluster tests are designed to investigate post-DNB (departure from nucleate boiling) fuel rod behavior within a cluster and possible rod-to-rod interactions. The shroud design for the PCM-5 test was modified on the basis of pretest analysis. Analysis of the expected results of the PCM-5 test was continued and preparation of the Experiment Prediction report was initiated. The Experiment Operating Specification report for Test PCM-5 was issued.

1.1.2 Gap Conductance (GC) Test Series. Significant accomplishments in the Gap Conductance Test Series during the quarter include: performance of Test GC 2-3, issuance of the quick look report (QLR) for Test GC 2-3, completion of the postirradiation examination for Test GC 2-1, and initiation of the postirradiation examination for Test GC 2-2.

1.1.3 Reactivity Initiated Accident (RIA) Test Series. Preparations for the upcoming RIA tests were continued. The RIA Scoping Test will be the first test performed. The purpose of this test is to determine the range of possible pressure pulses that can result from an RIA test of a single fuel rod in a liquid full system. Also, information obtained from the scoping test will be used to verify the steady state calorimetric measurements of fuel rod power by comparison with fission product analysis results of fuel rod power. A check of this comparison is also planned for Test RIA 1-1.

During the quarter, the Experiment Specification document for Tests RIA 1-1 and RIA 1-2 was issued. The Experiment Specification document contains a description of the hardware, planned instrumentation, and test fuel rods. An evaluation of fuel rod down-forces resulting from an

RIA was completed. Results indicate that, for the RIA tests planned, a maximum down-force of about 445 N can result for one typical boiling water reactor (BWR) fuel rod. Initial SCORE code calculations are under way to evaluate the effects of fuel rod heat transfer on the coolant hydrodynamics within a fuel rod flow shroud. RIA test data obtained at NSRR in Japan indicate severely degraded heat transfer for shrouded test fuel rods as compared with data from unshrouded test rods.

1.1.4 Loss-of-Coolant Accident (LOCA) Test Series. Preparations for the first LOCA blowdown test in the PBF continued during the past quarter. The test will consist of approximately 20 hr of nuclear operation followed by a blowdown of the PBF in-pile tube. At the start of the transient, the reactor will be scrammed and the in-pile tube isolated from the remainder of the loop. The quick opening blowdown valves situated between the loop isolation valves and the in-pile tube then will be cycled to create the desired mass flow on the test fuel rods.

The RELAP4 model used to predict the thermal-hydraulic behavior of the PBF-LOCA loop during the blowdown phase of a LOCA was modified to include additional volume nodes. The updated model was used in a sensitivity study to evaluate the effect of several other parameters on fuel rod cladding temperature. Factors having a measurable effect on calculated cladding temperature included the piping heat capacity, selection of post-CHF film boiling heat transfer correlations, and the selection of bubble rise velocity in a stagnant bypass volume surrounding the fuel assembly.

It was determined further that early fuel assembly flow stagnation and a corresponding earlier occurrence of CHF could be obtained by use of a bypass line connecting the hot and cold blowdown piping legs. Placement of this bypass line near the in-pile tube appeared to be the optimum choice to obtain early CHF.

A best-estimate prediction will be made for the upcoming LOC-11 test with the improved RELAP4 model to obtain boundary conditions for transient prediction of fuel rod behavior using the FRAP-T code.

1.1.5 Irradiation Effects (IE) Test Series. The effort on the Irradiation Effects Test Series consisted of evaluation of data from the IE-2, IE-3, and IE-5 tests and postirradiation examinations of the fuel rods irradiated during these tests.

1.1.6 Inlet Flow Blockage Test Series. The major activities in thermal-hydraulic analyses consisted of merging a new version of the SCORE/EVET with MOXY/SCORE code and the completion of preliminary experiment test predictions for the inlet flow blockage test series. The results will be used to aid in specifying the test conditions for the 25-rod flow blockage test.

1.2 Gap Conductance Test GC 2-3 Preliminary Results

R. W. Garner, D. T. Sparks

This section describes preliminary results of the Gap Conductance Test 2-3 (GC 2-3)^[III-2]. The test was completed on June 6, 1977. Test GC 2-3 was the third of a series of five tests that was developed on the basis of a 3 x 3 fractional factorial design 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 power oscillation method will be evaluated by comparison with values obtained by the steady state $fkdT$ method.

Four BWR-type rods were tested simultaneously in the PBF in-pile tube. Test GC 2-3 was performed in essentially the same manner and under the same test conditions as Tests GC 2-1^[III-3], and GC 2-2^[III-4], with the variations in fuel rod design shown in Table III-1. Test rod power densities ranged from 13 to 56 kW/m. All power oscillations were performed at $\pm$ 20% amplitude at an oscillation period of 20 s/cycle.

TABLE III-1

NOMINAL DESIGN CHARACTERISTICS OF TEST GC 2-3 TEST FUEL RODS

Parameter	Rod Designations			
	GC 523-1	GC 523-2	GC 523-3	GC 523-4
Initial gap width (diametral)	0.10 mm	0.10 mm	0.35 mm	0.23 mm
Fuel density	92% TD	95% TD	97% TD	92% TD
Fill gas composition (all rods at 2.6 MPa pressure, at ambient temperature)	Helium	Xenon	Helium	Argon

The test consisted of three periods of operation; (a) a power calibration period, (b) a preconditioning period during which the fuel was intentionally cracked to simulate operation in a power reactor, and (c) a power oscillation period during which data were obtained to evaluate the phase lag between cladding surface temperature oscillations and test rod power oscillations.

Test rod measurements included fuel centerline and off-center (near the pellet surface at three azimuthal orientations) temperatures, cladding surface temperatures, coolant flow rate, coolant temperature rise (ΔT) between inlet and outlet over the length of each rod, and axial neutron flux.

1.2.1 Experiment Results. Steady state and power oscillation data on fuel temperatures, cladding surface temperatures, and test rod linear power were obtained on each test rod. Representative on-line steady state data are provided in Table III-2. The power oscillation data will be analyzed to obtain gap conductance values.

Figure III-1 is a composite plot of the fuel centerline temperatures of all four test rods as a function of power density. The data in

TABLE III-2

ON-LINE STEADY STATE DATA FROM THE POWER CALIBRATION PART
OF GAP-CONDUCTANCE TEST GC 2-3 541 K INLET TEMPERATURE

Test Rod No., Gas, Gap	Core Power (MW)	Rod Peak Power (kW/m)	Fuel Temp (K)	Off-Center Fuel Temp (K) [a]			Cladding Temp (K)			Coolant Flow (cm ³ /s)	Coolant ΔT (K)
				0	120	240	60	180	300		
GC 523-1	3.1	13.9	873	755	734	708	561	559	559	192	11.9
Helium	4.7	21.1	1024	829	812	782	562	559	560	249	14.0
0.10-mm	6.2	26.2	1186	871	877	843	563	561	561	209	15.0
	7.9	32.9	1381	927	959	923	563	[b]	562	311	17.5
	9.4	37.3	1553	978	1025	987	567	[b]	551	360	17.0
	10.9	42.2	1738	1047	1104	1058	568	[b]	564	461	15.4
	12.5	51.1	1952	1117	1189	1127	570	[b]	566	568	15.2
	9.4	37.8	1571	1069	1097	1021	568	[b]	564	366	17.2
	6.2	25.6	1214	903	941	918	564	[b]	561	284	14.9
	3.1	13.2	889	745	771	759	562	[b]	559	202	10.8
GC 523-2	3.1	13.2	1410	1312	1299	1284	564	564	553	190	11.0
Xenon	4.7	20.2	1605	1369	1376	1343	568	576	567	244	13.3
0.10 mm	6.2	26.5	1750	1416	1416	1358	572	571	571	287	14.9
	7.9	33.2	1935	1508	1458	1371	575	573	573	312	17.2
	9.4	38.6	2100	1598	1519	1434	577	575	574	360	17.4
	10.9	46.2	2310	1734	1640	1558	580	576	574	462	16.4
	12.5	52.9	2286[c]	1675	1822	2011	582	577	575	574	15.2
	9.4	40.3	1877[c]	1542	1598	1609	576	573	572	369	17.8
	6.2	27.9	1633[c]	1419	1510	1508	571	569	568	288	15.7
	3.1	14.7	1348	1222	1354	1339	566	563	562	203	11.5
GC 523-3	3.1	12.8	1056	937	846	938	561	563	560	196	10.9
Helium	4.7	19.9	1307	1077	971	1082	561	563	560	246	13.4
0.35 mm	6.2	25.6	1513	1171	1067	1172	561	565	562	290	14.7
	7.9	32.1	[b]	1278	1184	1262	564	567	562	316	16.9
	9.4	36.5	[b]	1338	1276	1334	568	569	563	360	16.9
	10.9	45.2	[b]	1650	1360	1393	570	569	563	467	16.3
	12.5	50.4	[b]	1688	1430	1421	573	571	565	569	15.0
	9.4	38.7	[b]	1346	1368	1424	570	569	563	369	17.5
	6.2	26.2	[b]	1115	1145	1228	565	567	561	290	15.0
	3.1	13.6	[b]	850	896	952	561	551	560	205	11.0
GC 523-4	3.1	13.5	1406	[d]	[d]	1227	562	560	556	191	11.9
Argon	4.7	20.9	1641	[d]	[d]	1332	565	560	559	242	14.5
0.23 mm	6.2	26.5	1814	[d]	[d]	1410	569	563	563	286	15.6
	7.9	32.1	1932	[d]	[d]	1465	573	569	565	308	17.5
	9.4	37.2	2117	[d]	[d]	1518	576	571	567	354	17.7
	10.9	46.9	[b]	[d]	[d]	1612	578	571	568	458	17.4
	12.5	53.2	[b]	[d]	[d]	1921	580	573	570	560	16.3
	9.4	40.9	[b]	[d]	[d]	1653	577	570	567	362	18.6
	6.2	26.6	1780[c]	[d]	[d]	[b]	571	566	564	281	15.9
	3.1	13.7	1389	[d]	[d]	[b]	563	560	550	200	11.5

[a] Not corrected for perturbation effects.

[b] Not operating properly.

[c] Appeared to respond properly.

[d] Rod GC 523-4 did not contain these thermocouples.

Fig. III-1 Composite plot of test rod fuel centerline temperatures during Test GC 2-3.

Figure III-1 are representative values obtained throughout the test and illustrate the reproducibility of the centerline temperature measurements. The plots also show that the xenon and argon filled rods are consistently the hottest rods, and that the small gap in Rod GC 523-2 (the xenon filled rod) results in heat transfer rates similar to the wider gap argon filled rods. The centerline thermocouple in Rod GC 523-3 responded erratically beyond a power level of 32 kW/m and the data are not shown in Figure III-1.

Off-center thermocouples were located in each of the four test rods. An example of the excellent agreement among the azimuthally

oriented off-center temperature measurements in a given rod is shown in Figure III-2 for Rod GC 523-1 (helium, 0.10-mm gap).

Fig. III-2 Fuel off-center temperature measurements in Rod GC 523-1 (helium, 0.1-mm gap) during power calibration for Test GC 2-3.

1.2.2 Comparisons with Data Obtained from Tests GC 2-1 and GC 2-2.

Sufficient data have been obtained from Tests GC 2-1, 2-2, and 2-3 to permit preliminary evaluation of the effects of variations in fuel design parameters on fuel thermal response and resulting gap conductance. Figures III-3 and III-4 are representative composite plots of fuel centerline and off-centerline temperatures obtained with helium filled rods in the three tests. Figure III-3 shows the strong effect of initial gap width on the resulting centerline temperatures, and a subtle effect of fuel density at the higher power levels. Figure III-4 shows that the effect of density is significantly reduced at the off-center thermocouple locations, indicating that the density effect manifests itself through the fuel thermal conductivity and has little effect on the gap conductance. Figure III-4 also illustrates the low azimuthal temperature variations in the helium filled 0.1-mm gap rods, the data for each test being enclosed by vertical boxes.

Fig. III-3 Composite plot showing effect of initial gap width and fuel density in helium filled rods on fuel centerline temperature measurements. This plot also demonstrates reproducibility between tests for rods of similar design.

Fig. III-4 Composite plot showing azimuthal variations in measured off-center temperatures in helium filled rods with 0.1-mm diametral gaps. This plot also shows consistency between the different portions of the tests and reproducibility between tests for similar design rods.

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 have been evaluated.

Comparisons of data from Test GC 2-3 with data from Tests GC 2-1 and 2-2 have shown excellent reproducibility for similar rod designs between the three tests and consistency in observed effects of variations in fill gas composition, gap width, and fuel density. The data to be obtained from Tests GC 2-4 and 2-5, the remaining two tests in Series-2, will provide additional information for a complete evaluation of the effects of the three design variables on rod behavior and gap conductance values.

1.3 Irradiation Effects Test IE-2 PIE -- Definition of Lower Film Boiling Boundary

A. S. Mehner

In previous examinations of fuel rods from the Irradiation Effects and Power-Cooling-Mismatch Test Series, the lower and upper film boiling boundaries were metallographically defined by the transition in zircaloy cladding microstructure from the as-fabricated stress relieved microstructure to an alpha annealed microstructure to a prior beta microstructure. The transition zone was unexpectedly narrow, extending for only about 0.1 to 0.2 cm.

The lower film boiling boundary was further defined during the postirradiation examination of Rod IE-011 from Test IE-2. This fuel rod was fabricated with previously irradiated cladding and fresh fuel. Microhardness measurements were taken on three longitudinal sections from just below and through the lower film boiling boundary. Since irradiation damage is expected to anneal out at a temperature below the temperature at which microstructural changes first occur, that is, 920 K (where alpha annealing occurs), the detection of irradiation damage annealing would provide a lower temperature benchmark for the occurrence of film boiling.

The results of microhardness traverses along the three longitudinal sections are given in Table III-3. From well below the film boiling zone to the 0.540-m rod elevation, the hardness of the stress relieved microstructure remained relatively constant at 271 to 284 DPH. However, at the 0.545-m elevation, the material apparently softened, as measured by the microhardness decrease, to about 262 DPH. Since the microstructure still appeared to be a stress relieved microstructure, the softening was interpreted to be due primarily to radiation damage annealing and possible additional softening due to further stress relieving of the material. This evidence suggests that the lower film boiling boundary extends only slightly below (for this rod about 0.1 cm) the transition zone from stress relieved to alpha annealed microstructure. Thus, the original interpretation of the location of the lower film boiling boundary is not significantly altered by these findings.

TABLE III-3
MICROHARDNESS TESTING OF IE-2 FUEL ROD IE-011

<u>Sample Identity</u>	<u>Approximate Position[a] (m)</u>	<u>Orientation (degrees)</u>	<u>Micro-structure</u>	<u>Average Micro-hardness (Vickers DPH)</u>
A1	0.502	225	stress relieved	278
	0.506	225	stress relieved	280
	0.511	225	stress relieved	280
A2	0.524	225	stress relieved	278
	0.529	225	stress relieved	273
	0.533	225	stress relieved	276
A3	0.540	225	stress relieved	281
	0.545	225	stress relieved	262
	0.549	225	equiaxed alpha	209

[a] From bottom of fuel rod.

1.4 Irradiation Effects Test IE-3 Test Results

L. C. Farrar

This section describes the results of the third programmatic test, Test IE-3, in the Irradiation Effects Test Series. Test IE-3 was conducted in November, 1976 in the PBF reactor. Four PWR-type Saxton fuel rods, supplied by Westinghouse Electric Corporation, were used. The fuel rods, designated as Rods IE-015, IE-016, IE-017 and IE-018, had burnups of 11 060, 8550, 14 780 and 15 860 MWd/tU, respectively. The upper end caps on the 0.97-m long fuel rods were removed and replaced with end caps containing a pressure transducer. The fuel rods had an active fuel length of 0.89 m and were prepressurized to 2.6 MPa with an argon and helium gas mixture that had a thermal conductivity similar to fission gases.

The objectives of this test were to (a) determine the behavior of irradiated fuel rods during a fast power increase where the possibility of a pellet-cladding mechanical interaction failure is enhanced, and (b) determine the behavior of these fuel rods during a high power film boiling transient. The results of this test are compared with the results obtained from a previous irradiation effects test, where four irradiated fuel rods of a similar design were tested^[III-5].

The test consisted of 29 hours of preconditioning at rod peak powers not exceeding 35 kW/m. This was followed by a power ramp of 20 kW/m per minute to an average (of four rods) rod peak power of 69 kW/m to evaluate the potential for pellet-cladding mechanical interaction induced cladding failure. The rods were held at a high power for 1 hr, and then were subjected to a flow reduction to induce film boiling. Film boiling was first detected at a flow rate of 430 cm³/s (2120 kg/s·m²) and the flow reduction was continued to 280 cm³/s (1380 kg/s·m²). Steady state conditions were maintained for one minute, after which the reactor was rapidly shut down.

After approximately 1 minute of film boiling, there was severe cladding deterioration, but no cladding failure. However, one of the fuel rods, IE-015, did fail at 45 seconds after the reactor had been shut down, while the other three fuel rods remained intact. Preliminary post-irradiation examinations provided evidence that fuel swelling, cladding collapse, and molten fuel occurred during the film boiling operation.

The test results indicated that the fast power ramp, 20 kW/m per minute, did not cause fuel rod failure nor was there any obvious effect of this faster ramp on fuel rod behavior during 1 hour of steady state operation following the ramp. The higher rate of power increase caused an increased amount of cladding elongation when compared with the elongation of fuel rods of a similar design tested under similar conditions in Test IE-1, but at a ramp rate of 3.2 kW/m per minute. During the test planning it was expected that power ramps of this magnitude and rate would induce pellet-cladding-interaction failure. No posttest evidence has been found to date to support this expectation.

Comparison of the fuel rod internal pressure data from this test with data from fuel rods in the IE Scoping Test 2 and the IE-1 Test indicate that the more rapid power ramp did not affect the amount of fission gases released during the steady power periods following the ramp or during film boiling operations. The fuel rod internal pressure data in conjunction with conventional assumptions regarding fission gas generation were used to make estimates of fission gas release from the fuel rods for two different phases of the test.

During the 1-hour steady power period at 69 kW/m fuel rod peak power following the power ramp, approximately 21 and 27 % of the original inventory of fission gases were released from the fuel stacks of Rods IE-016 and IE-017. During or after film boiling, additional fission gas release from the film boiling region of the fuel stack was 80 and 8% of the original inventory for Rods IE-016 and IE-017, respectively.

The large differences in fission gas release between the fuel rods are due primarily to differences in the fuel temperatures of the rods during the steady power and film boiling operation. Preliminary post-irradiation examinations indicate that much more fuel melting occurred in the film boiling region of the fuel rod for Rod IE-016 than for Rod IE-017. The fact that nearly 100% of the fission gases are released from regions of the rod where fuel is molten helps explain the higher fission gas release from Rod IE-016 than IE-017 during film boiling operations. Although the amount of fission gas released from the 0.242-m long film boiling region on Rod IE-016 is significant, the total fuel rod internal pressure increase during film boiling due to thermal effects and fission gas release was only 18%. For Rod IE-016 the total measured pressure increase during film boiling was 7%.

The amounts of fission gases released during high power and film boiling operations are not large enough to cause large fuel rod internal pressure increases which result in the loss of cladding integrity or cause the cladding to balloon. Comparison of the results of this test with previous IE tests [III-6, III-7, III-8] where unirradiated fuel rods were tested indicates that the irradiated state of the fuel rods did not significantly affect fuel rod behavior during normal, abnormal (power ramp of 20 kW/m per minute) or accident (film boiling) conditions.

2. TFBP PROGRAM DEVELOPMENT AND EVALUATION

P. E. MacDonald

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 (topical reports), Halden fuel behavior research, and postirradiation examination of commercial power reactor fuel. The scope and objectives of these efforts are described in Reference III-9.

2.1 Summary of Ongoing Activities

Work continued on the IE Test Series topical report addressing integral fuel rod thermal, mechanical, and metallurgical behavior during PCM transients with emphasis on the effects of prior irradiation history. Topics chosen from detailed study are (a) steady state and transient fission gas release, (b) pellet-cladding interaction, and (c) permanent cladding deformation. Fission gas release was observed during both steady state and film boiling operation in Tests IE-1 and IE-3. Ramp rates to 20 kW/m per minute did not cause failure of previously irradiated cladding. Permanent cladding swelling and elongation were observed in fuel rods with initial diametral gaps <0.15 mm whereas rods with larger gaps exhibited cladding collapse and length reduction. These observations are being evaluated in terms of fuel rod design and operating variables.

Work also continued on the Reactivity Initiated Accident (RIA) topical report. SPERT and Japanese (NSRR) data have been reviewed and correlated to establish general thermal and mechanical fuel rod behavior during an RIA. The data show the cladding peak temperatures for single, unshrouded rods to be correlated to the fuel pellet peak energy deposition. Maximum cladding temperature data for rod bundles are slightly higher than otherwise identical single rods. Cladding DNB was found to be related to fuel-cladding gap closure, generally occurring at energy depositions sufficient to cause gap closure. Cladding radial expansion was found to coincide with pellet thermal expansion for unirradiated fuel rods, while waterlogged fuel rods exhibited much larger cladding expansion, indicating large fuel rod internal pressures. Cladding axial strain data from waterlogged fuel rods and irradiated rods, although limited, are near zero or negative, suggesting high fuel rod internal pressure precludes fuel-cladding mechanical interactions.

Evaluation of the data has also led to identification of two cladding failure modes. The first mode occurs at energy depositions near, but below, 1255 J/g in which the cladding has undergone extensive melting and oxygen embrittlement. The second failure mode occurs for

fuel energy depositions greater than 1464 J/g which are characterized by high fuel rod internal pressures, as a result of UO_2 vapor pressure.

Comparison of FRAP-T predictions with SPERT and NSRR data is continuing; current comparisons for two tests have been run and are being evaluated.

Collection of fuel rod property data continues from experiments in the Halden Boiling Water Reactor in Halden, Norway. A data report on cladding elongation, fuel centerline temperature, and rod internal pressure is being prepared on the mixed oxide test, IFA-226. These variables were monitored as a function of power and time for rods with varying initial gaps and densities. A revised version of the IFA-226 thermal response report is being prepared.

Postirradiation examination of the first three rods removed from the IFA-429 fission gas release and helium sorption experiment is under way in Harwell, UK.

Fabrication of IFA-430, the axial gas flow and fuel conductivity experiment is continuing.

Two commercial BWR fuel bundles were received at the INEL under the Power Reactor Postirradiation Examination Program. Detailed PIE on three rods known to have failed by pellet-cladding interaction will be conducted. Final design of a gamma scanner to accommodate commercial length fuel rods was completed.

3. FUEL MODEL DEVELOPMENT AND VERIFICATION

J. A. Dearien

Model development and verification activities continued for the steady state and transient Fuel Rod Analysis Programs, FRAP-S and FRAP-T. The FRAP-T3 tape package was transmitted to the Argonne Code Center and programming of FRAP-T4 is under way. Calibration and independent verification of FRAP-S3 is also in progress. A summary of ongoing activities

is presented in Subsection III-3.1 and a zircaloy anisotropy model is described in Subsection III-3.2.

3.1 Summary of Ongoing Activities

Highlights of the model development activity during this past quarter include a new interface pressure-dependent fuel cracking and relocation model. FRAP-S was modified to have a two-dimensional ($r-\theta$) heat conduction capability in the fuel. In FRAP-T, strain rate effects were added to the calculation of the free cladding deformation, and a set of multiplicative factors was included on a number of important variables (gap heat transfer, fuel thermal conductivity, heat transfer coefficients, etc.), so that statistical model sensitivity studies could be performed. To meet the needs of various experimental fuel rod programs using centerline thermocouples, FRAP-T was modified to permit analysis of fuel rods containing partial length central voids. Lastly, a version of the new deformable pellet mechanical subcode FRACAS-II was frozen and is being incorporated into FRAP-T4.

A general review of the fuel, cladding, and gap gas material properties subcodes (MATPRO) was completed. The MATPRO review was part of a two-week meeting of German and American experts on fuel rod behavior. New models for fuel hot pressing, the effects of the anisotropy of Zircaloy on plastic deformation, and the influence of cladding oxygen content on the plastic deformation of zircaloy cladding were emphasized during the review.

The scope of data comparison studies and commercial fuel standard design analyses now under way for independent verification of FRAP-S3 has been expanded. The sample size for the current data comparison study includes more than 600 rods. In addition to allowing a statistical interpretation of results, this sample reflects an emphasis on fuel thermal response, gas release, creep collapse, and gap closure response for moderate duty operation up to extended burnup. Standard design analyses were set up to characterize differences between FRAP-S3

and FRAP-S2^[III-10]. Supporting analyses were completed to characterize realistic initial condition distributions for calculating subsequent transient response.

3.2 Preliminary Model for Anisotropy of Cladding Plastic Deformation

D. L. Hagrman

Models which describe zircaloy cladding plastic deformation in the FRAP code are based on tubing uniaxial tests^[III-11]. These models have been modified to include coefficients of anisotropy defined to make the effective stress-effective strain law coincide with the uniaxial stress-strain correlation. The modification is based on data reported by Busby^[III-12,III-13] and on an analysis by Merkle^[III-14].

3.2.1 Summary. In Merkle's analysis, strain components are given by

$$\epsilon_1 = \frac{1}{E} [\sigma_1 (A1 + A3) - \sigma_2 A1 - \sigma_3 A3] \quad (1a)$$

$$\epsilon_2 = \frac{1}{E} [-\sigma_1 A1 + \sigma_2 (A2 + A1) - \sigma_3 A2] \quad (1b)$$

$$\epsilon_3 = \frac{1}{E} [-\sigma_1 A3 - \sigma_2 A2 + \sigma_3 (A3 + A2)] \quad (1c)$$

where

$\epsilon_1, \epsilon_2, \epsilon_3$ = principal axis strain components

E = the plastic modulus of the effective stress-strain curve

$\sigma_1, \sigma_2, \sigma_3$ = principal axis stress components

$A1, A2, A3$ = coefficients of anisotropy

The expressions derived for the coefficients of anisotropy in terms of the temperature, T , and the effective strain, ϵ_{eff} , are

$$A1 = \frac{R}{R+1} \quad (2a)$$

$$A2 = - \frac{R-5.4}{4.4 (R+1)} \quad (2b)$$

$$A3 = \frac{1}{R+1} \quad (2c)$$

where R is the contractual strain ratio during an axial test and can be defined as follows:

For temperatures above 1203.233 K

$$R = 1 \quad (3a)$$

For temperatures below 1203.233 K and strains below 0.15

$$R = 2.65 + T (1.36 \times 10^{-3} - T 2.27 \times 10^{-6}) \quad (3b)$$

For temperatures below 1203.233 K and strains between 0.15 and 0.30

$$R = 1 - \frac{0.3}{0.0225} [1 - R_0] \epsilon_{eff} + \frac{1}{0.0225} [1 - R_0] \epsilon_{eff}^2 \quad (3c)$$

3.2.2 Development of Model. Three conditions are required to evaluate the constants A1, A2, and A3.

- (1) To make the effective stress-effective strain curve coincide with the uniaxial stress-strain curve

$$A1 + A3 = 1 \quad (4)$$

- (2) In measurements of the ratio of contractile strains during an axial tension test [corresponding to $\sigma_2 = \sigma_3 = 0$ in Equations (1a-1c)],

the measured ratio of contractile strains for constant strain ratios^[a] during loading is

$$R = \frac{d\epsilon_2}{d\epsilon_3} = \frac{\epsilon_2}{\epsilon_3} = \frac{A1}{A3} \quad (5)$$

- (3) In measurements of the ratio of contractile strains during a circumferential uniaxial test on tubing [$\sigma_1 = \sigma_3 = 0$ in Equations (1a-1c)], the ratio of contractile strains for constant strain ratios during loading is

$$P = \frac{d\epsilon_1}{d\epsilon_3} = \frac{\epsilon_1}{\epsilon_3} = \frac{A1}{A2} \quad (6)$$

Values of R for use in evaluating Equation (5) at normal reactor operating temperatures were taken from Busby^[III-12,III-13] who found R = 2.6 to 2.8 for a typical lot of tubing. Miyamoto^[III-15] recently reported R = 3.13. The ratio P has been estimated by Busby^[III-13] from uniaxial tests transverse to the rolling direction of plate material to be approximately 1.7 R for typical commercial tubing. Recent measurement by Miyamoto^[III-15] of R = 3.13 and P = 5.78 are consistent with this estimate.

The measurements discussed previously were made at 294, 590, and 644 K. However, a description of material properties for accident analysis must include material behavior at higher temperatures. The data base used to estimate R at high temperature comes from a series of tensile tests conducted by Busby on one lot of tubing^[III-13]. In these

[a] The second equality of Equations (5) and (6) contains the assumptions that the strain ratios are constant. This is not valid for strains above 0.20 and may be justified below 0.05. Considerable experimental and theoretical development will be required to remove this deficiency by accounting for texture changes during an arbitrary loading history.

tests, a tendency was found for R to decrease with increasing temperatures up to 922 K. Busby's measured values were described with a second degree polynomial in temperature (Equation 3b). The value of R predicted by this polynomial decreases to 1.0 at a temperature of 1203.233 K which is near the end of the α - β phase transition. Since the beta phase has a cubic crystal structure, it would be expected to be nearly isotropic ($R=1$). For this reason, the value of R is taken to be 1.0 at temperatures above 1203.233 K.

Although Busby recommends use of the estimate $P = 1.7R$ ^[III-13] for tubing which has been fabricated in a relatively normal manner and heat treated in the alpha phase, the parameter P was not measured at temperatures above 644 K. In order to provide preliminary estimates of the change in P with temperature, the following assumptions are employed.

- (1) Busby's parameter, $Q = \frac{d\epsilon_1}{d\epsilon_2}$, measured during a uniaxial test in the radial direction was introduced
- (2) Since values of Q and P, like R, should be consistent with the crystallographic texture, P and Q should approach 1.0 as the cladding becomes isotropic. The ratio of the difference $P-1.0$ and $Q-1.0$ is assumed to be constant

$$\frac{P-1}{Q-1} = \text{a constant} = C \quad (7)$$

- (3) The relation $\frac{RQ}{P} = 1$ derived by Busby, was used with Equation (7) to deduce the expression

$$Q = \frac{P}{R} = \frac{1-C}{R-C} \quad (8)$$

- (4) Finally, Busby's recommended low temperature values, $R = 2.8$ and $\frac{P}{R} = 1.7$, were used with Equation (8) to find $C = 5.4$.

The expressions for A_1 , A_2 , and A_3 which result from substituting Equation (8) with $C = 5.4$ into Equations (4), (5), and (6) are Equation (2). As mentioned previously, R is a complex function of strain for large strains. The existing data do not allow even a preliminary model of this change for biaxial stress states. The constants A_1 , A_2 , and A_3 are, therefore, assumed to approach their values for isotropic material ($A_1 = A_2 = A_3 = 0.5$) for large strains.

The isotropic assumption was tested against the assumption that the constants A_1 , A_2 , and A_3 remain unchanged at large strains by comparing closed tube burst strength predictions with Busby's reported values. The isotropic assumption produced predictions consistently closer to the measured values. The expression used to return the low strain values of R and thus the constants A_1 , A_2 , and A_3 to their isotropic values as effective strain increases from 0.15 to 0.30 is Equation (3C).

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IV. REACTOR BEHAVIOR PROGRAM

R. R. Stiger, Manager

Accomplishments are reported on RELAP4/MOD7 development and code verification studies.

1. LOCA CODE DEVELOPMENT

L. H. Sullivan

RELAP4/MOD7 is being developed to provide a best estimate model for reflood during a postulated loss-of-coolant accident (LOCA) in a boiling water reactor (BWR). A model has been developed to describe core heat transfer during spray cooling in a BWR. This model will be used to calculate heat transfer from the fuel rods and channel box surfaces to the fluid flowing through the core during the period of spray cooling. Later in the transient when bottom reflood has been initiated, the reflood heat transfer models in RELAP4/MOD6 are used for heat slabs below the top quench front. A top quench model to be used in RELAP4/MOD7 to calculate the quench front propagation velocity, and position for both the fuel rods and channel boxes has been described in Reference IV-1.

The RELAP4/MOD7 core spray heat transfer model presented in this document considers the most important of the heat transfer mechanisms. The model describes, within a rod bundle, radiation heat transfer from surface to surface and from surface to fluid. Radiation absorption by steam containing dispersed water droplets is calculated as a function of fluid properties, quality, and surface temperatures. The effects of a surface partially and completely wetted by the top quench front are considered in the radiation calculations. Convection heat transfer is calculated for dry surfaces, partially wet surfaces, and completely wet surfaces. The spray cooling heat transfer model is described in the following sections.

1.1 Radiation Heat Transfer

G. E. McCreery and G. A. Jayne

The general equation for the net heat flux at each surface was developed by Siegel and Howell^[IV-2] by considering a gray wall enclosure with N surfaces containing an isothermal fluid. The net energy leaving the surface j of the enclosure, Q_j , is

$$Q_j = q_j A_j = (q_{o,j} - q_{i,j}) A_j \quad (1)$$

where

j	=	surface index
Q_j	=	net energy leaving the surface
q_j	=	net flux
A_j	=	surface area
$q_{o,j}$	=	outgoing flux
$q_{i,j}$	=	incident flux.

The outgoing flux at any surface is composed of emitted and reflected energy as follows:

$$q_{o,j} = \epsilon_j e_j + \rho_j q_{i,j} \quad (2)$$

where

ϵ_j	=	emissivity
e_j	=	blackbody emission = σT_j^4
σ	=	Steffan-Boltzmann constant
T_j	=	temperature
ρ_j	=	reflectivity = $1 - \epsilon_j$ for a gray surface.

For the gray wall enclosure bounding an isothermal gas at temperature T_g , the incident energy, $Q_{i,k}$, on any surface k from all surrounding surfaces j is

$$Q_{ik} = A_k q_{i,k} = \sum_{j=1}^N (q_{o,j} A_j F_{jk} \tau_{jk} + e_g A_j F_{jk} \alpha_{jk}) \quad (3)$$

where

- k = surface index
- A_k = surface area
- F_{jk} = view factor of surface j viewing surface k
- τ_{jk} = transmissivity of fluid between surfaces j and k
- e_g = blackbody emission of fluid = σT_g^4
- T_g = fluid temperature
- α_{jk} = absorptivity between surfaces j and k .

Equations (1), (2), and (3) are combined algebraically to form the general equation for the net heat flux at each surface as given by Siegel and Howell^[IV-2]:

$$\sum_{j=1}^N \left(\frac{\delta_{kj}}{\epsilon} - F_{kj} \frac{1-\epsilon_j}{\epsilon_j} \tau_{kj} \right) q_j =$$

$$\sum_{j=1}^N [(\delta_{kj} - F_{kj} \tau_{kj}) e_j - F_{kj} \alpha_{kj} e_g] \quad (4)$$

where

$$\delta_{kj} = 1 \text{ if } k = j$$

$$\delta_{kj} = 0 \text{ if } k \neq j$$

Equation (4) is the basis for radiation calculations in RELAP4/MOD7. The equation is solved for q_j using values of e_j calculated from surface temperatures at the previous time step. Calculation of ϵ , τ , and F are dependent on surface properties, fluid properties, geometry, and temperature. The values of e_g are calculated using a simplified steam superheat model.

The energy added to the fluid in the rod bundle by radiation is

$$Q_{\text{RAD}_g} = \sum_{j=1}^N A_j q_j \quad (5)$$

where

A_j = surface area.

Since the fluid is assumed to be optically thin and isothermal, Q_{RAD_g} is assumed to be distributed uniformly across the rod bundle at any elevation. The heat radiated to the fluid in channel n associated with surface j , $Q_{\text{RAD}_{gn}}$, is

$$Q_{\text{RAD}_{gn}} = Q_{\text{RAD}_g} \frac{A_n}{\sum_{n=1}^N A_n} \quad (6)$$

where

A_n = flow area of channel n
 N = number of channels.

Because the radiation is assumed to be distributed uniformly to the fluid in the rod bundle, $Q_{\text{RAD}_{gn}}$ is not necessarily equal to Q_j from the adjacent surface.

1.2 Convection Heat Transfer

G. E. McCreery and G. A. Jayne

The convection heat transfer from a dry rod is very low during spray cooling because of low steam velocity, steam superheating, and the inability of liquid droplets to contact the high temperature rods.

Convection heat transfer from a dry heat slab to steam is calculated by using a constant input value of the convection coefficient, h , at each node. The convection heat transfer rate is given by

$$q_{\text{CONV}_j} = h (T_j - T_{\text{SAT}}). \quad (7)$$

The use of constant values of h is shown by Rogers and Leonard^[IV-3] to give fairly good results.

For a heat slab completely wetted by a falling film, a constant user input heat transfer coefficient is used. Values typical of forced convection vaporization are recommended by Duncan and Leonard^[4].

1.3 Heat Transfer Solution When the Top Quench Front Lies Within a Heat Slab

G. E. McCreery and G. A. Jayne

A special model is used to describe the radiation and convective heat transfer for a heat slab containing the quench front. An actual and idealized axial surface temperature distribution at the quench front within a heat slab is shown in Figure IV-1. The surface temperature rapidly drops from the dry wall temperature, T_1 , to saturation temperature, T_{SAT} , across the quench front position Z_q . Upstream of the quench front, the surface temperature increases to slightly above T_{SAT} because of internal heat generation. This temperature distribution is modeled as a step change from T_1 to T_{SAT} at axial position Z_q .

1.3.1 Convection Heat Transfer for a Partially Quenched Heat Slab.

Convection heat transfer is calculated for a partially quenched heat slab by defining an average temperature, $\bar{T}$, and an average convection coefficient, $\bar{h}$, such that

$$Q_c = \bar{h} A (\bar{T} - T_{\text{SAT}}) \quad (8)$$

Z_0, Z_1 = heat slab boundaries
 A_q = quench front position
 $\bar{T}$ = average temperature
 T_1 = dry wall temperature
 T_{SAT} = saturation temperature

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Fig. IV-1 Actual and idealized slab surface axial temperature distribution.

where

Q_c = convection heat transfer
 A = heat slab surface area
 T_{SAT} = saturation temperature.

The average temperature is defined as

$$\bar{T} = \frac{Z_1 - Z_q}{Z_1 - Z_0} T_{SAT} + \frac{Z_q - Z_0}{Z_1 - Z_0} T_1 \quad (9)$$

where

$$\begin{aligned} Z_0, Z_1 &= \text{heat slab boundaries} \\ Z_q &= \text{quench front position} \\ T_{SAT} &= \text{saturation temperature.} \end{aligned}$$

In this equation $\bar{T}$, Z_q , T_{SAT} , and T_1 are functions of time.

The average convection coefficient, $\bar{h}$, for a rod is calculated from Equation (8) by assuming Q_c is equal to the enthalpy loss rate of the rod due to quenching plus the internal heat generation rate; that is:

$$Q_c = \bar{\rho C}_p \frac{\pi D^2}{4} |u_q| (T_1 - T_{SAT}) + q_{GEN} (Z_1 - Z_0) \quad (10)$$

where

$$\begin{aligned} q_{GEN} &= \text{heat generation rate per unit length} \\ \bar{\rho C}_p &= \text{average density times specific heat} \\ D &= \text{rod diameter} \\ u_q &= \text{quench front velocity.} \end{aligned}$$

The resulting convection coefficient is then

$$\bar{h} = \frac{\bar{\rho C}_p (D/4) |u_q|}{Z - Z_0} + \frac{q_{GEN}}{\pi D (\bar{T} - T_{SAT})} \quad (11)$$

In this equation, $\bar{T}$ is the surface temperature calculated from the conduction solution in RELAP4.

For the channel box, the average convection coefficient is given by an expression similar to that for the rod:

$$\bar{h}_{CB} = \frac{\bar{\rho C}_p \left(\frac{\delta}{2}\right) |u_g|}{Z - Z_0} \quad (12)$$

where

δ = wall thickness.

1.3.2 Radiation Heat Transfer for a Partially Quenched Heat Slab.

Radiation heat transfer for the case when one or more heat slabs are partially quenched is calculated by using, in Equation (4), equivalent values of the quantities

$$\sigma T_{\text{EFF}}^4$$

and

$$\epsilon_{\text{EFF}}$$

where

- σ = Stefan-Boltzmann constant
- T_{EFF} = effective surface temperature ($\neq \bar{T}$)
- ϵ_{EFF} = equivalent emissivity.

The equivalent emissivity is given by

$$\frac{\epsilon_{\text{EFF}}}{1-\epsilon_{\text{EFF}}} = \frac{\epsilon_{\text{WET}}}{1-\epsilon_{\text{WET}}} \frac{Z_1-Z_q}{Z_1-Z_o} + \frac{\epsilon_{\text{DRY}}}{1-\epsilon_{\text{DRY}}} \frac{Z_q-Z_o}{Z_1-Z_o} = B. \quad (13)$$

The equivalent value of σT_{EFF}^4 is

$$\sigma T_{\text{EFF}}^4 = \frac{1-\epsilon_{\text{EFF}}}{\epsilon_{\text{EFF}}} \left(\sigma T_{\text{SAT}}^4 \frac{\epsilon_{\text{DRY}}}{1-\epsilon_{\text{DRY}}} \frac{Z_1-Z_q}{Z_1-Z_o} + \sigma T_7^4 \frac{\epsilon_{\text{WET}}}{1-\epsilon_{\text{WET}}} \frac{Z_q-Z_o}{Z_1-Z_o} \right). \quad (14)$$

1.4 Total Heat Transfer

G. E. McCreery and G. A. Jayne

The total heat transfer from a slab surface j is given by the sum of the radiation and convection terms

$$Q_{\text{TOTAL}_j} = q_j A_j = Q_{\text{RAD}_j} + Q_{\text{CONV}_j}. \quad (15)$$

The total heat transfer to a fluid g in a channel associated with surface j is

$$Q_{TOTAL_g} = Q_{RAD_g} + Q_{CONV_j} \quad (16)$$

2. CODE VERIFICATION

R. E. Rice

A prediction of the fourth test in the BWR Blowdown/Emergency Core Cooling Program^[IV-5] was completed and compared with predictions of the first test in the test series. An uncertainty analysis was completed of experimental steady state stable film boiling data for water flowing vertically upward in round tubes. Results of these two studies are described in the following sections.

2.1 The Fourth BWR-BD/ECC Test: A RELAP4 Prediction and Comparison with a RELAP4 Analysis of the First BWR-BD/ECC Test

R. R. Schultz

A RELAP4^[a] prediction of the fourth experiment (TLTA-3 Test 6004) in the BWR Blowdown/Emergency Core Cooling Program^[IV-5] is presented. Test 6004 is the fourth in a series of nine tests which will investigate the differences between scaled versions of BWR/4 (218-in. diameter vessel, 560 fuel bundles) and BWR/6 (218-in. diameter vessel, 624 fuel bundles) systems during the blowdown portion of a postulated loss-of-coolant accident. The TLTA-3 hardware is the first TLTA configuration to simulate a BWR/6. The hardware consists of an 8 x 8 bundle contained within a pressure vessel whose volumes, mass, energy, and flow rates are 1/624 of the BWR/6 vessel. The initial thermodynamic conditions and power level of this test configuration are the same as those of the BWR/6 system for an average bundle. To change from the BWR/4 TLTA-1 configuration to the TLTA-3 configuration, the lower plenum was enlarged, the initial downcomer mixture level was lowered, the 7 x 7 bundle

[a] RELAP4 EXP*P4/CE 06/25/75 -- an experimental version of the code very similar to RELAP4/MOD5 Update 2.

was changed to an 8 x 8 bundle, the power was increased by 9.5%, and the break area was reduced. The TLTA-3 Test 6004 prediction is compared to the TLTA-2 Test 6001 (a BWR/4 configuration except an 8 x 8 average powered bundle was used) prediction to assess the thermal hydraulic differences.

2.1.1 RELAP4 Model. The model utilized for the TLTA-3 Test 6004 prediction is basically the same as that used for the TLTA-2 Test 6001 prediction^[IV-6]. The model was changed to account for the enlarged lower plenum, lower mixture level in the downcomer, increased core power, and reduced break area.

2.1.2 Summary of Results. The TLTA-3 Test 6004 prediction and comparison with the TLTA-2 Test 6001 prediction are summarized in curves showing the system depressurization, the core hydrodynamics, and the core heatup characteristics.

(1) System Pressure Response. The calculated steam dome pressure responses of the TLTA-3 Test 6004 prediction and the TLTA-2 Test 6001 prediction are shown in Figure IV-2. The steam dome pressure response is typical of the behavior of all system pressures.

Figure IV-2 shows the steam dome pressure calculated in the Test 6004 prediction to be dropping rapidly during the first 5 s following the break. The TLTA pressure control valve controls the steam flow and thus the system pressure until the recirculation pump suction is uncovered at 9.6 s. Following this event, the vessel undergoes a rapid depressurization for the remainder of the transient.

The Test 6001 prediction of the steam dome pressure is quite different. The pressure drops rapidly during the first 2 s after the break. The pressure stabilizes after 2 s until a rapid depressurization occurs at 7.8 s when the jet pump suction uncovers. The most significant system pressure change occurs at 10.1 s as the recirculation pump suction line uncovers and results in lower plenum fluid flashing. The difference between the steam dome pressure behavior in the two test

Fig. IV-2 Comparison of the predicted steam dome pressure in TLTA-2 Test 6001 and TLTA-3 Test 6004.

predictions after 10.1 s is due primarily to a 54% decrease in break area for the Test 6004 prediction.

(2) Core Hydrodynamics. The core inlet mass flows calculated for Tests 6004 and 6001 are shown in Figure IV-3. The Test 6004 calculation predicts the core inlet flow to fall to approximately 28% of the initial value immediately following the break. The mass flow continues its downward trend until lower plenum flashing at 10.3 s results in a peak value of approximately 4.54 kg/s. The lower plenum flashing spike lasts for nearly 3 s after which the inlet core flow remains near zero.

Comparison of the core inlet mass flows shows the results to be similar. The mass flows remain at approximately the same level throughout the transient. However, the Test 6004 calculation predicts the jet

Fig. IV-3 Comparison of predicted core inlet mass flow in TLTA-2 Test 6001 and TLTA-3 Test 6004.

pump suction is uncovered at 5.8 s versus 7.8 s for the Test 6001 calculation as evidenced by the flow spikes which occur at these times. The time that the broken loop jet pump suction is uncovered during Test 6004 is not representative of that which would occur on a BWR/6 because the jet pumps must be lowered approximately 0.28 m to scale this event properly. Lower plenum flashing occurs at nearly the same time in both tests, but the lower plenum flashing flow spike in the Test 6001 calculation peaks at 8.62 kg/s and only lasts for 1.5 s. These differences in behavior are due to the different initial downcomer mixture level, break area, and vessel depressurization rate.

(3) Core Heat Transfer Characteristics. Figure IV-4 compares the predicted peak temperatures for Tests 6004 and 6001. The peak temperature occurs at the 2.45-m elevation in both cases.

Fig. IV-4 Comparison of predicted peak thermocouple temperatures of TLTA-2 Test 6001 and TLTA-3 Test 6004 (slab elevation = 2.45 m, local peaking factor = 1.04).

The Test 6004 calculation does not show significant heatup until 15.5 s. The rod is dried out at 16.7 s and reaches 669 K at 30 s. For Test 6001, the rod dries out at 16.1 s and reaches 641 K at 30 s. Thus the various hardware changes coupled with a 9.5% increase in power results in a 28 K difference in peak temperature between Tests 6004 and 6001.

2.1.3 Conclusions. The following conclusions are apparent from this study:

- (1) The Test 6004 depressurization rate will be much lower than those of the previous tests in the Blowdown/Emergency Core Cooling Program due to a 54% decrease in break area from the TLTA-2 to the TLTA-3 configuration.

- (2) The peak rod temperature rise will occur in the vicinity of the 2.45-m instrumentation plane. The predicted peak thermocouple temperature is 669 K.
- (3) The effect of increasing the lower plenum volume for the TLTA-3 configuration is not apparent in this study.
- (4) The net effect of all the changes in geometry and initial conditions between the TLTA-2 Test 6001 and the TLTA-3 Test 6004 predictions (including a 9.5% increase in power) is a 28 K temperature increase.

2.2 An Uncertainty Analysis of Tube Steady State Stable Film Boiling Data

S. J. Bengston and K. G. Condie

An uncertainty analysis of experimental steady state stable film boiling data for water flowing vertically upward in round tubes has been completed. These data^[IV-7 through 16], taken from 10 separate experimental investigations, have been widely used throughout the nuclear community for development and verification of post-CHF heat transfer correlations. This analysis provides the necessary uncertainty values on heat transfer coefficients as required for the data bank.

The purpose of this analysis was to estimate the variance of experiment random error and the variance of experiment systematic error. The variable of interest throughout this study was the heat transfer coefficient. The errors mentioned refer to errors in the heat transfer coefficient determination.

Uncertainties were taken from the documentation of each of the experiments when available. The remainder were estimated from the data using statistical analysis. Experiment random errors were analyzed using only the individual experimenter's data, while systematic errors

required combining all the data. Effects of systematic errors on a combined data regression model were also investigated.

Results of the analysis are found in Table IV-1. The table shows that the random error standard deviation differs from experimenter to experimenter. However, the systematic error standard deviation will dominate the uncertainty analysis in most cases. There was no indication that any data should be eliminated.

Most of the statistical estimation problems encountered in this analysis (unbalanced data, voids in the data) are typical ones which are inherent in large data aggregates. These problems could be corrected by properly designed experimental programs. The combination of no formal statistical experimental design and existing systematic experimenter effects does not have an apparent effect on the combined data regression model other than that represented by the root mean square error (measure of average difference between the experimental data and the regression model).

The data base could be improved with data from specially designed experiments. All instrument random and systematic errors should be identified and their variances recorded.

TABLE IV-1

FILM BOILING DATA STANDARD DEVIATIONS^[a]

Experimenter	Number of Points	Experiment Random Standard Deviation	Experiment Systematic Standard Deviation	Regression Model Standard Deviation
B & W ^[7]	904	0.110	0.14	0.05
Bennett 1 ^[8]	1111	0.070	0.14	0.03
Bennett 2 ^[9]	73	0.070	0.14	0.03
Bertoletti ^[10]	65	0.130	0.14	0.05
Bishop ^[11]	78	0.065 ^[b]	0.14	0.05
Era ^[12]	576	0.085	0.14	0.03
G. E. ^[13]	846	0.160	0.14	0.05
Herkenrath ^[14]	826	0.090 ^[b]	0.14	0.05
Schmidt ^[15]	75	0.040	0.14	0.02
Swenson ^[16]	80	0.045	0.14	0.02

[a] All standard deviations are expressed relative to the expected heat transfer coefficient.

[b] Taken from the literature.

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V. 3-D EXPERIMENT PROJECT

R. T. French, Manager

During this reporting period, the overall direction of the 3-D Experiment Project was formulated. A multinational cooperative program is being pursued to investigate the behavior of entrained liquid in the upper plenum and cross flow in the core during the reflood phase of a pressurized water reactor loss-of-coolant accident. Two experiment facilities are currently planned: (a) 180-degree sector, full-scale, upper plenum separate effects reflood test facility, and (b) a slab-shaped core separate effects reflood test facility. The two facilities will be coordinated through the application of a multidimensional computer code. The United States, Germany, and Japan currently plan to enter the cooperative project.

The Idaho National Engineering Laboratory (INEL) will provide technical assistance to the Nuclear Regulatory Commission as well as perform development, design, fabrication, assembly, and installation of various two-phase flow instrumentation in support of the overall 3-D Experiment Project. This work was initiated at the INEL on June 1, 1977.

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