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QUARTERLY TECHNICAL REPORT ON WATER REACTOR
SAFETY PROGRAMS SPONSORED BY THE NUCLEAR
REGULATORY COMMISSION'S DIVISION OF
REACTOR SAFETY RESEARCH, JANUARY-MARCH 1975

Aerojet Nuclear Company

IDAHO NATIONAL ENGINEERING LABORATORY

Idaho Falls, Idaho — 83401

MASTER

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Abstract

Information is presented concerning reactor safety activities in the following programs: the semiscale program to study the thermal⁽¹⁾-hydraulics of LOCA phenomena in water-cooled power reactors; the LOFT program to analyze PWR systems during LOCA conditions; the thermal fuels behavior program to study the performance of reactor fuels under normal and accident conditions; and the reactor ~~the~~ behavior program to study power reactor response to accident conditions by use of computer codes. (66 references.)

(DCC)

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Water Reactor Safety Research
Systems Engineering
Fuel Behavior
Analysis Development
TID-4500

QUARTERLY TECHNICAL REPORT ON WATER REACTOR SAFETY PROGRAMS
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AEROJET NUCLEAR COMPANY

Date – September 1975

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FORWARD

This is the first of a series of reports to be issued quarterly on the technical activities of Water Reactor Safety Programs being conducted by Aerojet Nuclear Company (ANC) at the Idaho National Engineering Laboratory (INEL) under the sponsorship of the Nuclear Regulatory Commission's Division of Reactor Safety Research. Since this is the first report of the series, fairly extensive descriptions of the programs have been included. The reader is advised that some of the results reported may be of a preliminary nature subject to revision or correction at a later date. An attempt has been made to identify such instances in the text.

Copies of the report are available from the Technical Information Center, Energy Research and Development Administration, Oakridge, Tennessee.

PREFACE

Aerojet Nuclear Company (ANC), as a prime contractor at the Idaho National Engineering Laboratory (INEL), performs major parts of the national water reactor safety program. The current water reactor safety activities of ANC are the Semiscale Program, the Loss-of-Fluid Test (LOFT) Program, the Thermal Fuels Behavior Program, and the Reactor Behavior Program.

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

The LOFT Program is a nuclear test program for providing test data to support: (a) assessment and improvement of the analytical methods utilized for predicting the behavior of pressurized water reactors (PWR) under LOCA conditions; (b) evaluation of the performance of PWR engineered safety features (ESF), particularly the emergency core cooling system (ECCS); and (c) assessment of the quantitative margins of safety inherent in the performance of these safety features. The test program utilizes the LOFT facility, an extensively instrumented 55-MW pressurized water reactor facility designed for the conduct of loss-of-coolant experiments (LOCE). The LOFT facility is in the final stages of construction. The test programs, planned to start in late 1975, 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 experimental portion of the program is concentrated on testing of single fuel rods and fuel rod clusters under power-cooling-mismatch (PCM), loss-of-coolant and reactivity initiated accident conditions. These tests provide in-pile experimental data for the evaluation and verification of analytical models that are used to predict fuel behavior under reactor conditions spanning normal operation through severe hypothetical accidents. Data from the Program provide a basis for improvement of the models.

The Reactor Behavior Program encompasses the analytical aspects of predicting the response of nuclear power reactors under normal, abnormal, and accident conditions.

Program areas include advanced code development, fuel code development, code verification, and code optimization. The program produces advanced computer codes for predicting the behavior of reactor systems and fuel, establishes data requirements for and performs verification analysis of analytical safety assessment codes, and maintains and upgrades computer codes to meet changing analytical and verification requirements.

LIST OF ABBREVIATIONS

ANC	-	Aerojet Nuclear Company
ATWS	-	Anticipated Transients without Scram
BDL	-	Beginning of Life
BWR	-	Boiling Water Reactor
CDC	-	Capsule Driver Core
CE	-	Combustion Engineering, Inc.
CHF	-	Critical Heat Flux
DNBR	-	Departure from Nucleate Boiling Ratio
ECCS	-	Emergency Core Coolant System
EDL	-	End of Life
ERDA	-	Energy Research and Development Administration
ESF	-	Engineered Safety Features
EVET	-	Equal Velocity, Equal Temperature
FM	-	Frequency Modulation
FOM	-	Figure of Merit
HBWR	-	Heavy Water Moderated Boiling Water Reactor
HPIS	-	High-Pressure Injection System
HTC	-	Heat Transfer Coefficient
IE	-	Irradiation Effects
INEL	-	Idaho National Engineering Laboratory
IPT	-	In-Pile Tube
LOCA	-	Loss-of-Coolant Accident

LOCE	-	Loss-of-Coolant Experiment
LOFT	-	Loss-of-Fluid Test
LPIS	-	Low-Pressure Injection System
LWR	-	Light Water Reactor
MTA	-	Mobile Test Assembly
NRC	-	Nuclear Regulatory Commission
PBF	-	Power Burst Facility
PCM	-	Power Cooling Mismatch
PCS	-	Primary Coolant System
PIE	-	Postirradiation Examination
PWR	-	Pressurized Water Reactor
RIA	-	Reactivity Initiated Accident
SPEART	-	Special Power Excursion Reactor Tests
TD	-	Theoretical Density
TREAT	-	Transient Reactor Test
UVUT	-	Unequal Velocity, Unequal Temperature

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I. SEMISCALE PROGRAM

J. O. Zane, Program Manager

The Semiscale Program is part of the water reactor safety research program sponsored by the Office of Nuclear Regulatory Research of the U.S. Nuclear Regulatory Commission. Government administrative functions pertaining to the program are performed by the Energy Research and Development Administration. The program is conducted by Aerojet Nuclear Company at the Idaho National Engineering Laboratory (INEL).

Sections that follow describe the purpose and nature of the program. A generalized description of the currently operational Semiscale Mod-1 test system is included. Also included are discussions of two topics that serve to illustrate some of the characteristics of the program. One of these describes results obtained from experimental investigation of variations in the location used to inject emergency coolant into the test system following system blowdown (depressurization) which serves to simulate loss-of-coolant accident (LOCA) conditions. The other describes results of a prediction for a blowdown heat transfer experiment that is one of several such experiments planned as part of the continuing program.

1. PURPOSE AND NATURE OF THE PROGRAM

E. O. Meals

The Semiscale Program consists of a continuing series of thermal-hydraulic experiments having as their primary purpose the generation of experimental data that can be applied to the development and verification of analytical models describing LOCA phenomena in water-cooled nuclear power plants. Emphasis is placed on acquiring system effects data 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 core cooling (ECC) phases of a LOCA. The experiments are performed with a test system that simulates the principal physical features of a nuclear plant but which is much smaller in volume. Nuclear heating is simulated in the experiments by a core consisting of an array of electrically-heated rods, each of which has dimensional and heat flux characteristics similar to those of nuclear fuel rods.

The Semiscale experiments, both past and present, are closely related to the Loss-of-Fluid Test (LOFT) Program which is directed toward future operation of a nuclear power system designed to investigate LOCA phenomena and ECC. The LOFT system, which has a primary coolant system volume that is intermediate between those of Semiscale and commercial plants, has served as the reference plant for design criteria and component scaling decisions for the currently operational Semiscale Mod-1 system. Both LOFT and Semiscale Mod-1 systems have a 1-1/2-loop primary coolant system configuration in which an intact (operating) loop contains active components (pump and steam generator) for simulating the performance of the intact loops in a pressurized water reactor (PWR) system and a blowdown loop with inactive component simulators that serves as the broken loop for LOCA simulation.

The volume of the Semiscale Mod-1 primary system is about 1/50 of that of LOFT and about 1/1500 of that of a typical large commercial plant of current design. Dimensions of the principal heat transfer surfaces in the Semiscale Mod-1 system (core heater rods and steam generator tubes) are the same as their LOFT counterparts, but the quantity of these surfaces is reduced to maintain the semiscale to LOFT system volume ratio. The resultant small lateral dimensions of the Semiscale Mod-1 core and steam generator make the system essentially one dimensional in terms of its effect on thermal-hydraulic phenomena. This limitation of the system requires careful interpretation of the experimental results. Although the Semiscale data are of notable worth for evaluating and improving analytical models, the user of the data must recognize that the thermal-hydraulic phenomena observed in certain regions of the Semiscale system differ from those which would occur in counterpart regions of a nuclear plant, whether the latter is the LOFT system or a commercial plant. The basic analytical models of LOCA thermal-hydraulic behavior can be applied to both Semiscale and nuclear plants, but the models must properly characterize the specific system components to obtain valid calculational results. Model adaptation includes consideration of the scaling principles employed in designing the small scale test system.

The Semiscale Program covers many facets of LOCA experimental investigation. The Semiscale Mod-1 integral test system is highly versatile in terms of geometrical arrangement of the primary coolant system piping and is extensively instrumented for acquisition of thermal-hydraulic data during blowdown and during ECC injection. The integral tests performed with the system include introduction of variables to evaluate the relative effects of differing pipe rupture locations, rupture size, flow resistances in selected regions, alternate locations for emergency coolant injection, and others. In addition to the Semiscale Mod-1 integral system effects tests, separate effects tests are performed from time to time to acquire data on two-phase flow phenomena in specific regions of the primary coolant system or on individual component performance.

As indicated in the foregoing, the primary objective of the Semiscale Program is to produce integral and separate effects experimental thermal-hydraulic response data that can be applied to the evaluation and further development of analytical models describing LOCA phenomena and ECC effectiveness. An important secondary objective is to produce data that will aid other experimental programs in the optimization of test plans, selection of test parameters, and evaluation of test results. The current Semiscale Mod-1 Program places strong emphasis on support to the LOFT Program, including the assessment of instrumentation requirements and the reliability of selected LOFT instruments. Future Semiscale test system modifications, for which design of hardware has been initiated, will be more directly applicable to commercial PWR systems by virtue of employing heat transfer surfaces having the same length as their counterparts in the full-scale plants.

At the present time, the Semiscale Program is the only source of integral effects experimental data on PWR LOCA phenomena. This situation is expected to prevail until the LOFT system becomes operational. Semiscale and LOFT are complementary to each other, and in turn they both are complementary to the various separate effects experiments being performed at government and industry laboratories elsewhere.

The major areas of interest for LOCA phenomena and ECC system performance have been identified as follows:

- (1) System-component interactions
- (2) Pump performance characteristics
- (3) Reactor core blowdown flow and heat transfer
- (4) Emergency coolant bypass and steam-water mixing
- (5) Reactor core reflood flow and heat transfer.

The first three of these areas relate primarily to the blowdown phase of the LOCA sequence of events. The last two pertain to the effectiveness with which emergency coolant can be supplied to the core, including evaluation of alternate ECC system concepts for upgrading emergency cooling capability.

Extensive experimental data are needed for each of the stated areas of interest. Analytical modeling, which offers the only practical means of predicting the sequential events and results of LOCA-ECC processes in a large commercial power plant, is dependent on such data for identifying shortcomings in the analysis techniques and ultimately obtaining models that are known to provide reliable predictions. The task of producing the data is complex because the physical phenomena are complex and not easily measurable due to their highly transitory nature. Both separate effects and integral effects transient experiments of various physical scale are needed to support development of the required analytical predictive capability. The Semiscale Program, by virtue of providing integral effects experiments that cover the entire LOCA-ECC sequence of events, is contributing importantly to the data base in all five of the areas of interest noted in the preceding paragraph. Although some of the Semiscale Mod-1 experiments are designed to concentrate on the blowdown phase and some on the reflood phase, even these limited objective experiments will cover at least two of the five areas of interest during each test operation. Before the program is complete, sufficient tests covering the entire LOCA-ECC sequence of events will have been performed to give a good data base for all of the interacting phenomena. Investigation of alternate ECC concepts also is a significant part of the program.

Existing and future documents will eventually provide complete documentation of the Mod-1 system design, experimental program, and experimental results. Primary formal documentation consists of experiment data reports which present the data without interpretation and test results reports which provide results of analysis and interpretation of specific aspects of the system performance and processes occurring during the experiments. The distribution of both categories of reports is in accord with the standard distribution of water reactor safety research reports. The experiment data reports transmit the compiled data from a test or test group and provide information concerning instrumentation, control, and facility anomalies, as necessary, to set the data in perspective. All data are presented in engineering units. The test results reports present selected experimental data and provide an

interpretation of the experimental response of the Semiscale Mod-1 system relative to test predictions. Observations and interpretations are included to assist the data user in relating the experimental data, as produced, to the predictions. These observations reflect the experience of the experimentalist with the Semiscale Mod-1 system.

2. THE SEMISCALE MOD-1 TEST PROGRAM

E. O. Meals

The Semiscale Mod-1 program of LOCA experiments began with performance of the first blowdown test in August 1974. Six additional tests were performed before the end of December 1974. Two of these were duplicative tests to resolve questions created by results of preceding tests. This series of tests represents the beginning of a program that is expected to span a period of about three years. The general scope of the program is shown in Table I.

The numerical sequence of test series in Table I should not be interpreted as representing a test schedule for the program even though the first few series have been planned for accomplishment in the order listed. Also, the number of tests to be performed for each series should be viewed as an approximation only. In some cases, a test from one series will be combined with a test from another series so that a single operation of the Semiscale Mod-1 system will produce data that satisfies both test objectives. For most series, the actual number of tests performed will depend on results of the test predictions and also on results of previous tests. Performance of supplemental tests not specifically listed in the table can be expected as a result of unforeseen requirements or upon direction from the Nuclear Regulatory Commission.

Those tests in Test Series 1 which were planned for accomplishment with a core resistance simulator rather than the electrically powered core which is required for the subsequent groups have been completed. A final test with the electrical core in place but unpowered has been postponed purposely for later accomplishment. This test group initially consisted of six tests but was subsequently expanded to include two additional tests. Test Series 2 testing plus one of the Test Series 7 tests and three of the Test Series 3 tests are expected to be completed before the end of calendar year 1975. Following completion of Test Series 3 testing during the first part of calendar year 1976, several of the Test Series 4 tests will be performed during the remaining months of that year. A group of LOFT counterpart tests that will simulate LOFT nuclear operation is being considered for the first half of calendar year 1977. These tests are not shown in the table. They can be expected to fulfill some of the objectives indicated for other groups beyond those now firmly scheduled. Scheduling of Test Series 11 (active components in broken loop) and Test Series 18 (effect of core length) tests is dependent upon availability of components now in the design stage; namely, a PWR-length core, a PWR-length steam generator, and a broken loop pump. The indicated schedules are believed to be conservative. They have been made that way intentionally pending accumulation of experience in using an electrically-heated core for tests involving system blowdown.

TABLE I
SEMISCALE MOD-1 TEST GROUP SUMMARY

<u>Test Series</u>	<u>Test Description</u>	<u>Estimated Number of Tests</u>
1	Isothermal blowdown tests -- LOFT core simulation and inactive core	8
2	Blowdown heat transfer characteristics tests of the Semiscale Mod-1 system	6
3	Reflood heat transfer characteristics	7
4	ECC injection studies including changes in injection location, injection rate, injection angle, and total amount injected	21
5	System resistance (differential pressure scaling and distribution studies)	3
6	Break time effect on system response	3
7	Small break simulation	6
8	Secondary system effects -- steam valve closure rate and initial water levels	3
9	Pump coastdown effect on system behavior	2
10	Steam generator tube elevation effect on system behavior	1
11	Pump and steam generator simulator effects -- replacement with active components	6
12	Communicative type break -- simulation of a split in the piping rather than a double-ended rupture	3
13	Change of pressurizer attachment location from operating loop to the broken loop	2
14	Core differential temperature and initial velocity effects on system response	3
15	Steam generator tube leak effect on core reflood rates	2
16	Large volume upper head effects on system response	2
17	Core bypass simulation with either bypass into the downcomer or bypass into the upper plenum	2
18	Evaluation of effect of core length	15
19	Evaluation of certain simulated LOFT components	3

3. THE SEMISCALE MOD-1 SYSTEM

E. O. Meals

The Semiscale Mod-1 test system became operational in August 1974 following a two-year period during which engineering design, procurement, fabrication, assembly, and checkout testing of the system were accomplished. The Semiscale Mod-1 preparatory work included procurement and installation of rectifiers and other components for a dc power supply needed to energize the reactor simulator. Design and installation of a new experiment control system and an enlarged data acquisition system were other elements of the preoperational phase of the program.

As noted in Section I-1, the Semiscale Mod-1 design is a 1-1/2-loop configuration. The 1/2-loop portion, or "broken" loop, provides for system blowdown through two rupture assemblies that represent the upstream and the downstream ends of a sheared pipe in the primary coolant system. During a blowdown test, rupture occurs simultaneously on demand in each of the two assemblies, and the escaping coolant flows into a common header that is attached to a tank designed to represent the pressure-time behavior of a nuclear plant containment system during a LOCA. Pump and steam generator flow resistance simulators are components of the broken loop piping arrangement. The location of these passive simulators can be changed to achieve either a cold leg break or a hot leg break configuration. Figures 1 and 2 depict the Semiscale Mod-1 system in each of the configurations. The change from one configuration to another is a relatively simple process and is indicative of the experimental versatility that has been achieved in the system design.

The other loop of the Semiscale Mod-1 system, known as the intact loop, has a volume about three times that of the broken loop. This greater volume serves to represent three intact loops of a four-loop nuclear plant. An active pump and an active steam generator are incorporated into the Semiscale Mod-1 intact loop. The dimensions of the steam generator heat transfer surfaces (U-tubes) are the same as the dimensions of the corresponding surfaces in the LOFT steam generator, and the elevation of the Semiscale Mod-1 steam generator relative to that of the core is the same as that in the LOFT system.

The Semiscale Mod-1 vessel houses either a core resistance simulator or a reactor simulator. The core resistance simulator is an orificed structure which simulates the resistance of the LOFT core simulator. The reactor simulator consists of 40 electric heater rods designed to operate at 1.6 MW. The heated length of the heater rods corresponds to the length of the LOFT nuclear fuel rods (5.5 feet). The diameter of the heater rods and the spacing of the rods in the Semiscale Mod-1 core are the same as the corresponding dimensions in the LOFT core. The vessel and vessel internals have linear (vertical) dimensions and elevations that correspond closely to those of their counterparts in the LOFT system.

The numerals shown in Figures 1 and 2 identify sections of pipe (spool pieces) which are joined by Grayloc fittings and which in total comprise the Semiscale Mod-1 primary system piping. These spool pieces fulfill two basic experimental requirements; namely, configuration versatility (such as is needed for changing break location) and ease of instrument installation or repositioning along the flow path. A typical instrumented spool piece is shown in Figure 3.

Fig. 1 Semiscale Mod-1 cold leg break configuration.

Table II indicates the type of measurements being obtained in Semiscale Mod-1 experiments for determining the response of both the system and the individual components. The table shows the number of each of the different types of measurements that can be made in various components or regions of the Semiscale Mod-1 system. The response of an individual component is determined from measurements located on the component and from measurements made near the component.

Fig. 2 Semiscale Mod 1 hot leg break configuration.

Fig. 3 Typical instrumented spool piece.

TABLE II

SEMISCALE MOI-1 MEASUREMENT TYPE, NUMBER, AND GENERAL LOCATION

General Location	Measurement Type							
	Fluid Temperature	Metal Temperature	Density	Fluid Momentum Flux	Volumetric Flow	Pressure	Differential Pressure	Liquid Level
<u>Vessel</u>								
Inlet annulus	2	5				1	3	
Downcomer region	15	30			11		6	
Lower plenum	5	6	4			1	4	1[a]
Core region	17	163[b]	1	1	1		4	1[a]
Upper plenum	2	1				2	6	
<u>Intact Loop</u>								
Hot leg piping	5[c]	6	3	2	1	1		
Steam generator primary side						2	9	
Steam generator secondary side	6[d]				2[e]	1		1
Pump suction loop	4[f]	8	1	1	1	1	4	
Pump							2	
								Pump speed, power, torque
Cold leg piping	5	8	3	2	2		4	
Pressurizer	1		1		1			
<u>Broken Loop (cold leg break configuration)</u>								
Vessel side cold leg	1	4	2	2	1	1	3	
Vessel side break	1					1	1	
Hot leg	1	4	1	1	1	1	2	
Simulated steam generator							6	
Pump suction leg	2[f]		1	1	1	1	2	
Simulated pump	1	2					2	
Pump side cold leg	2		1	1		1	2	
Pump side break	1					2	4	
Simulated reflood bypass line	2							
Rupture discs						2		
<u>Pressure Suppression System</u>								
Suppression tank	6					1	1	
Spray system					1			
<u>Coolant Injection System</u>								
Intact loop accumulator						1		
Intact loop low pressure injection system					1			
Intact loop high pressure injection system					1			
Intact loop injection point	1				1			
Broken loop accumulator						1		
Broken loop low pressure injection system					1			
Broken loop high pressure injection system					1			
Broken loop injection point	1				1			

[a] LOFT liquid level probe.

[b] Includes 160 heater pin thermocouples, 3 core housing thermocouples.

[c] Includes 2 shielded thermocouples.

[d] Includes 1 feedwater and steam dome fluid thermocouple.

[e] Feedwater and steam volumetric flows.

[f] Includes 1 shielded thermocouple.

4. SEMISCALE MOD-1 ISOTHERMAL TEST SERIES – TEST RESULTS OF ECC INJECTION LOCATION VARIATIONS

D. J. Hanson

The isothermal blowdown test series (Test Series 1) of the Semiscale Mod-1 Program is essentially completed. The tests in this series are referred to as LOFT counterpart tests because they correspond to the nonnuclear isothermal tests to be performed in the LOFT system when that system becomes operational. The seven tests currently completed include all those tests in which a core resistance simulator was intended to be used in place of the electric heater rod core. Results of these tests have been documented^[1-6]. A final isothermal test with the electric heaters in place, but unpowered, has been intentionally postponed for future accomplishment.

One of the objectives of the Semiscale Mod-1 isothermal test series was to investigate the effect of integrating simulated emergency core cooling water at different locations in the system with interest being centered on the phenomena occurring in the lower plenum and in the core region. This section discusses the results from four tests in the isothermal test series which exhibit the effects of injecting ECC into the lower plenum, intact loop cold leg, and downcomer annulus. Both the hot and cold leg break configurations (Figures 1 and 2) were used in the course of performing these tests.

Tests performed to investigate the effect of ECC injection location with the system in the cold leg break configuration included one test (Test S-01-3) with injection into the lower plenum and one test (Test S-01-4A) with injection into the cold leg of the intact loop. The system configuration was identical for these two tests except for the alternate injection locations. The initial conditions were similar, with the only appreciable difference being the initial level of water in the system pressurizer. A fluid temperature of about 540°F and a pressure of about 2,250 psig were established in the system prior to rupture. The break configurations for both tests represented an offset shear (200%) break.

Tests performed to investigate the effect of ECC injection location with the system in the hot leg break configuration included one test (Test S-01-1) with injection into the upper annulus of the downcomer and one test (Test S-01-1B) with injection into the cold leg of the intact loop. With the exception of ECC injection location, the system configuration was identical for these two hot leg break tests. The initial conditions for both tests were identical, with the fluid temperature being about 540°F and the pressure being about 1,600 psig prior to rupture. The flow area for each of the two break nozzles used for each test was scaled to one-half (100%) the flow area of an offset shear.

The relative effectiveness of ECC injected at different system locations can be evaluated by comparing the amount of time required for the water to reach the lower plenum and core flow mixer box regions and also the rate of delivery of water to these regions. Information needed for these evaluations can be derived from density and flow measurements in the lower plenum and in the core flow mixer box. Density and flow measurements made in the broken loop cold leg also aid in understanding the ECC-related phenomena.

Comparisons of the experimental results with RELAP4^[7] calculated results are presented in accompanying figures to indicate the capability of this calculational tool. RELAP4 results are based on calculations performed after Tests S-01-4A and S-01-1B were conducted. The RELAP4 results end about 30 seconds after rupture due to calculated water packing in the cold leg.

4.1 Lower Plenum and Intact Loop Cold Leg ECC Injection

A direct comparison of results from Test S-01-3 (lower plenum injection) and Test S-01-4A (intact loop cold leg injection) can be made if the time of injection initiation and the rate of injection are similar for both tests. The times of injection initiation were comparable, and Figure 4 shows that the accumulator volumetric flow rate, which represented the bulk of the water injected for both tests, was similar for the two cases. The large increase in volumetric flow rate between 60 and 70 seconds corresponds to all the accumulator water having been discharged and the nitrogen beginning to flow into the system. The slightly greater accumulator water flow for Test S-01-4A caused the discharge of all water to be completed slightly earlier for Test S-01-4A than for Test S-01-3.

Fig. 4 Accumulator volumetric flow.

Continued nitrogen flow for Test S-01-3 was a result of an unplanned connection of the accumulator to the nitrogen source. The slight difference in injection rates is not considered to be attributable entirely to the difference in injection location even though care was exercised to duplicate the accumulator inventory and pressure and the resistance in the accumulator line for both tests.

The average density of fluid in the lower plenum measured during both tests and that calculated by RELAP4 for Test S-01-4A are shown in Figure 5. The average calculated and measured lower plenum density for Test S-01-4A exhibits a continuing downward trend following the initiation of water injection whereas the density measurement for Test S-01-3 exhibits a reversal of the downward trend at about the same time as the injection occurs (22 seconds after rupture). The depletion of lower plenum mass during Test S-01-4A continued until about 52 seconds, at which time small amounts of ECC began reaching the lower plenum. The delay in delivery to the lower plenum after the initiation of injection is a consequence of countercurrent flow in the downcomer and of steam generation on the hot downcomer walls. Countercurrent flow during the Semiscale Mod-1 tests persisted at a sufficiently high level to restrict downward flow of water until near the end of the depressurization period (about 40 seconds after rupture).

During Test S-01-4A, the fraction of the time delay in lower plenum density attributable to steam generation on hot downcomer walls is calculated to be about 10 seconds. This lengthy delay time due to hot walls is much longer than the hot wall delay times

Fig. 5 Average fluid density in the lower plenum.

calculated for systems that are many times larger than the Semiscale Mod-1 system. The rapid rate of increase in lower plenum density at about 60 seconds after rupture is equivalent to an ECC delivery rate of about 36 gpm. Since this delivery rate is much larger than the water injection rate, some of the injected water must have been stored in the downcomer during the hot wall countercurrent flow period.

Density measurement at the broken loop cold leg vessel outlet provides an indication of the duration of ECC bypass of the downcomer. Figure 6 shows the results of this density measurement for the period of water injection. Bypass during Test S-01-4A is indicated by higher average density and the relatively large spikes in the density data following the initiation of ECC injection into the intact loop cold leg. The Test S-01-3 density data indicate the absence of downcomer bypass flow when injection is into the lower plenum.

Fig. 6 Density of the broken loop cold leg vessel outlet.

The average density in the lower plenum calculated by RELAP4 (Figure 5) is in fairly good agreement with the data from Test S-01-4A. The discrepancy between the RELAP4 results and the data in the time period 14 to 28 seconds after rupture may be the result of reverse core flow sweeping out some fluid from the lower plenum in the experiment. Since this phenomenon is not modeled in RELAP4, the calculated results are too high. The large drop in the calculated density at about 27 seconds cannot be explained; however, in RELAP4 a bubble rise model was included for the lower plenum volume which may have contributed to the decline.

The density measured in the core flow mixer box is shown in Figure 7. The trends in the measured density for both tests are in relatively good agreement until the water that was

Fig. 7 Density at the core flow mixer box.

injected into the lower plenum during Test S-01-3 reached the core flow mixer box at about 37 seconds after rupture. The calculated results from RELAP4 appear to provide an averaged approximation to the measured density. A possible cause of this averaged approximation to the experimental data is the RELAP4 assumption of homogeneous fluid properties over a volume. When this distributed homogeneous density is compared to a point measurement of density, exact agreement would not occur.

Figure 8 shows the flow in the core flow mixer box immediately below the core inlet. Again, reasonable repeatability of measured flow rates exists for both tests up to the time that water enters the flow mixer box during Test S-01-3. The RELAP4 calculated flow rate exhibits trends similar to those of the data, but the magnitudes of the flow rate are greatly different, especially early in the blowdown. The difference between the calculated core inlet flow and the data appears to be caused by differences between calculated and actual break flows and also differences between calculated and actual pump heads.

The effect of the accumulator nitrogen flowing into the lower plenum during Test S-01-3 is shown in Figures 9, 10, and 11 which cover the period from 60 to 130 seconds after rupture. As indicated by Figure 9, the nitrogen enters the bottom of the lower plenum at about 70 seconds and expands through the water, thus lowering the average density. A similar trend is seen in the density at the core flow mixer box which is plotted on the same figure. The average lower plenum density then recovers somewhat but remains relatively low as nitrogen flow continues over an extended period. The swell of the liquid level that occurs when the nitrogen mixes with the water causes some of the water originally in the vessel to flow through the broken loop cold leg vessel outlet, thus decreasing the liquid inventory in

Fig. 8 Flow at the core flow mixer box.

Fig. 9 Average density in the lower plenum -- Test S-01-3.

Fig. 10 Mass flow at the broken loop cold leg vessel outlet - Test S-01-3.

Fig. 11 Mass flow at the core flow mixer box - Test S 01-3.

the vessel. This water-nitrogen flow out of the vessel, which begins at 70 seconds, is shown in Figure 10. At about the same time, a sudden increase in flow occurred in the core flow mixer box as shown in Figure 11. The mixer box flow reversal between 70 and 80 seconds appears to be caused by a pressure buildup in the upper plenum region. Investigation of the flow reversal is continuing.

The flow of accumulator nitrogen through the system and out of the cold leg break during Test S-01-4A does not appear to cause additional ECC water bypass of the downcomer. As shown in Figure 6, prior to the time that nitrogen flow begins, the density at the broken cold leg vessel outlet for Test S-01-4A drops to about the density of saturated steam, thus indicating the end of ECC bypass. The density measurement at the broken loop cold leg vessel outlet does not show a significant rise above the saturated steam density for the period of nitrogen injection, indicating that the nitrogen did not cause additional ECC bypass.

In summary, the comparison of results from Tests S-01-3 and S-01-4A shows that injection of ECC into the lower plenum has the expected advantage over injection into the cold leg of providing earlier flow of ECC to the core region. The lack of downcomer bypass during lower plenum ECC injection results in a larger vessel liquid inventory. Although accumulator nitrogen flowing into the lower plenum causes water level swell in the vessel and, in turn, some water flow out of the vessel, this level swell offers potential for additional core cooling. Additional tests using a heated core are needed to ascertain whether significant increase in core cooling is provided by the level swell due to nitrogen.

Although a delay occurs in the delivery of ECC water to the lower plenum with cold leg injection, the results from Test S-01-4A indicate that the water does not completely bypass the downcomer during the delay period. Some ECC appears to be stored in the downcomer rather than bypassing to the break. Accumulator nitrogen flow does not appear to cause additional water bypass.

4.2 Upper Annulus and Intact Loop Cold Leg ECC Injection

Comparison of the results of Test S-01-1 (upper annulus injection) and Test S-01-1B (intact loop cold leg injection) exhibits very little effect of the change in injection location. The time of ECC injection and the rate of injection were similar for both tests, with the initiation of accumulator injection occurring about 33 seconds after rupture. Figure 12 shows the density measured at the core flow mixer box for both tests. The results from Test S-01-1 indicate a slightly earlier delivery of a significant amount of ECC to the mixer box. As for the results from the cold leg injection test (Test S-01-3), the delay in ECC water delivery to the core region appears to be caused by the combined effects of downcomer countercurrent flow and steam generation on the hot downcomer walls. The density measurement at the broken cold leg vessel outlet, shown in Figure 13, indicates a slightly later initiation of downcomer bypass for Test S-01-1B. This later bypass initiation with cold leg injection appears to result from ECC partially filling the intact loop cold leg before flowing to the annulus-downcomer region and, therefore, does not signify an improvement in delivery to the downcomer. The difference in the time of delivery to the core flow mixer box may be due to the time required for the ECC to partially fill the cold leg.

Fig. 12 Density at the core flow mixer box.

Fig. 13 Density in the broken loop cold leg.

The RELAP4 results presented in Figure 12 show that a calculation which includes the effect of slip improves the agreement between the calculated and the measured densities at the core flow mixer box. The results of RELAP4 with slip presented in Figure 13 also show reasonable agreement with the measured density in the broken loop cold leg. However, comparison of densities and flows at other locations around the system has indicated that the RELAP4 model with the slip calculation does not consistently improve the calculated densities and flows.

5. SEMISCALE MOD-1 BLOWDOWN HEAT TRANSFER TEST SERIES - PREDICTIONS FOR TEST S-02-1

T. K. Larson

The blowdown heat transfer test series is the second series of tests conducted in the Semiscale Mod-1 experimental program and is composed of six tests (Tests S-02-1 through S-02-6). These tests will be the first tests conducted in the Semiscale Mod-1 system with power applied to the electrical-heater-rod core. The primary purpose of this test series is to obtain information required to evaluate the heat transfer characteristics of the Semiscale Mod-1 core and to obtain information necessary for evaluating the analytical models that are currently used to calculate core flow and heat transfer coefficients. In addition, the test series will provide data necessary for determining the appropriate post-DNB power control characteristics required for simulating the thermal behavior of a nuclear fuel rod, and will provide an assessment of the initial hydraulic conditions appropriate for subsequent tests investigating core reflood behavior.

The following discussion pertains to the first test (Test S-02-1) to be conducted with the Semiscale Mod-1 system. Test objectives, initial conditions, and predicted test results are presented. The RELAP4 computer program^[7], which was employed to obtain the test predictions, is described.

5.1 Test S-02-1 Description

Test S-02-1 is conducted at conditions of 74% rated core power (1.2 MW) and 100% design flow rate (165 gpm) to maintain safe operation relative to the departure from nucleate boiling ratio (DNBR) for the Semiscale Mod-1 core. The break configuration represents a full-size (200%) double-ended offset shear in the hot leg. The steam generator is in an active mode. Emergency core cooling is not used. The primary purpose of Test S-02-1 is two-fold: (1) to provide the first experimental evaluation of the heat transfer characteristics of the Mod-1 core and (2) to allow a relatively more simple basis for comparison of experimental results with analytical capabilities in the area of blowdown heat transfer by providing data from a test with unidirectional flow through the core which results from the break location being in the hot leg.

5.2 RELAP4 Computer Model Description

The RELAP4^[7] computer code was used to predict the response of the Semiscale Mod-1 system for Test S-02-1. This code allows a system to be spatially represented in terms of volumes and junctions that correspond to actual system components. An illustration of the volumes and junctions used in the RELAP4 model is shown on Figure 14. Table III defines the control volumes used in the model as they relate to the Semiscale Mod-1 system.

Fig. 14 RELAP4 nodalization diagram for hot leg break configuration.

The RELAP4 model of the Semiscale Mod-1 core allows both the high-power and the low-power electrical heater rods to be represented as heat slabs. Through code input, the appropriate geometry and power fraction for each heat slab can be specified so as to describe the axial power distribution of the heater rods. The four high-power heater rods are represented by eight core heat slabs and the 36 low-power heater rods are represented by four core heat slabs as shown in Figure 15. A table of normalized core power versus time (scram curve) is also input to the code. This scram curve defines the total core power such that the electrical core energy generation rate will simulate the stored energy in a nuclear core in a blowdown situation.

Selection of the flow model used at the break junctions (Junctions 40 and 45, Figure 14) depends on the break flow regime. The Henry choked flow model^[8] was selected for the subcooled phase and the Moody^[9] choked flow model (with a multiplier of 0.6) was selected for the saturated phase.

TABLE III

RELAP4 MODEL FOR BLOWDOWN HEAT TRANSFER TEST S-02-1

Volume	Description
1	Inlet annulus
2	Top third of downcomer
40	Middle third of downcomer
41	Bottom third of downcomer
3	Lower plenum
4 }	Core flow mixer region and instrumentation
5 }	
6 }	
7 }	Heated core region
8 }	
9 }	
10 }	Inactive core region
11 }	
12 }	
13	Upper plenum
53	First third of intact loop hot leg
14	Second third of intact loop hot leg
15	Last third of intact loop hot leg
16	Steam generator inlet plenum
17 }	Steam generator tubes
45 }	
46 }	
18 }	Steam generator outlet plenum
19 }	
20 }	
21 }	Pump suction leg
22	Intact loop pump
23	Not used
54	First third of intact loop cold leg
24	Second third of intact loop cold leg
55	Last third of intact loop cold leg
25	Not used
26	Pressurizer
47	First third of broken loop hot leg
56	Second third of broken loop hot leg
57	Last third of broken loop hot leg
42	Pressure suppression system header
27	
28	Simulated steam generator inlet plenum
29 }	Simulated steam generator
30 }	
31 }	
32 }	Simulated steam generator outlet plenum
33 }	
34 }	
35	Simulated pump inlet
36 }	Simulated pump
37 }	
38 }	
38	Simulated pump outlet
39	Simulated pump to vessel
43	Pressure suppression system downcomer
44	Pressure suppression tank

Fig. 15 Baseline RELAP4 model of Semiscale heated core configuration.

5.3 Predicted Results for Test S-02-1

The figures discussed in the following paragraphs illustrate analytical results obtained from the RELAP4 computer code. The predicted behavior of key system variables (for 32 seconds following rupture) are presented and discussed.

Figure 16 shows the predicted upper plenum depressurization rate. The pressure is seen to drop almost immediately to the saturation pressure corresponding to the hot leg fluid temperature (594°F). The relatively greater hot leg temperature and resulting saturation pressure cause the vessel-side break flow rate to be greater than that which would have resulted from an isothermal condition. The greater break flow rate in turn causes an accelerated depressurization rate. The vessel-side break flow rate (Junction 40) is shown in Figure 17. Figure 18 shows the simulated steam generator-side break flow rate (Junction 45). The simulated steam generator-side break flow is somewhat less than the vessel-side break flow.

Fig. 16 Upper plenum pressure.

Fig. 17 Vessel-side break flow rate.

Fig. 18 Simulated steam-generator-side break flow rate.

This lower flow is attributable to the high resistance of the simulated steam generator and simulated pump that are used in the Semiscale Mod-1 broken loop. The resistance of the pump simulator represents a locked rotor condition.

The broken loop fluid density directly upstream (relative to the normal flow direction) of the vessel-side break nozzle is shown in Figure 19. Pump and core flow rates are depicted in Figures 20 and 21, respectively. The pump flow drops sharply as pump inlet density decreases. The core flow remains positive, as expected, during the blowdown.

The predicted cladding surface temperatures of the low-power and high-power heater rod hot spots (points of maximum power generation) are shown in Figures 22 and 23, respectively. Predictions indicate that the mode of heat transfer for the hot spots of both rods will be that of nucleate boiling for about 14 seconds after the break occurs. The resulting conduction-limited situation coupled with the positive core flow causes the rod surface temperatures to decrease initially. The predicted temperature increase at 19.5 seconds results from a change in heat transfer correlation as the core quality approaches unity. Cladding temperature oscillations after 25 seconds are apparently caused by slight core flow oscillations.

Fig. 19 Density in broken loop hot leg.

Fig. 20 Intact loop pump inlet flow rate.

Fig. 21 Core inlet flow rate.

Fig. 22 Low power rod hot spot cladding surface temperature.

Fig. 23 High power and hot spot cladding surface temperature.

II. LOFT PROGRAM

L. J. Ybarrondo, Program Manager

LOFT is an integral nuclear reactor test facility which has been designed to simulate, as nearly as possible, all of the important effects that are anticipated to occur during a LOCA in a pressurized water reactor nuclear steam supply system^[a].

The LOFT Integral Test Program is one of the major phases of the Energy Research and Development water reactor safety program plan^[10]. Its major mission objectives are:

- (1) To provide data required to evaluate the adequacy of and to improve the analytical methods currently used to predict the LOCA response of PWRs, the performance of engineered safety features (ESF) with particular emphasis on ECCS(s), and the quantitative margins of safety inherent in the performance of the ESF.
- (2) To identify and investigate any unexpected events or thresholds in the response of either the plant or the ESF and develop analytical techniques that adequately describe and account for the unexpected behavior.

The following sections present the design objectives of the integral test system, the integral test experimental program, a description of the experimental system, a description of the experimental incore instrumentation, and the results of an analysis of the LOFT LOCE subcooled blowdown behavior.

1. LOFT INTEGRAL TEST SYSTEM DESIGN OBJECTIVES

On the basis of the LOFT Integral Test Program mission, the following specific design objectives were established:

- (1) All core and reactor coolant system nuclear reactivity, temperature, pressure, and flow transients during a LOCE in LOFT are to be similar to the LOCA transients which are anticipated to occur in industrial PWRs.

[a] The word integral is used here to define an experiment combining the nuclear, thermal, hydraulic, and structural processes occurring during a LOCA and differentiates it from the single- and dual-effects, nonnuclear, small-scale, thermohydraulic experiments conducted to date.

- (2) Sufficient special experimental instrumentation is to be provided and located to measure significant interacting nuclear and thermohydraulic processes required to assess LOCA and ECCS performance.
- (3) Sufficient versatility is to be provided to allow assessment of the margin inherent in the performance of the several design variations in the ECCSs currently used as well as any predictable future system variations.
- (4) Where significant differences must exist between LOFT and PWR plant parameters, the design is to be such that parameters may be varied over a sufficiently wide range during the LOFT experiments to verify that the selected essential design compromises do not affect the validity of the analytical tools proven on LOFT for PWRs.

2. LOFT INTEGRAL TEST EXPERIMENTAL PROGRAM – BASIC TEST PLAN

The LOFT Integral Test Program consists of both nonnuclear and nuclear test series. The nonnuclear tests are those tests conducted with isothermal conditions existing in the reactor coolant loop (that is, with no nuclear heat being produced throughout the test). The nuclear tests are those tests in which the reactor core is producing power and a coolant temperature differential exists across the core.

The general order of tests begins with nonnuclear tests and progresses toward the more demanding tests (that is, toward those nuclear tests with increasing potential for high cladding temperatures). This approach is utilized to:

- (1) Maximize LOFT fuel reusability, thereby increasing the rate of data acquisition and minimizing program costs
- (2) Provide reasonable assurance, based on the initial, less demanding tests, that the experimental system will respond to LOCE conditions as planned during subsequent tests
- (3) Evaluate and, if necessary, mature key analytical prediction areas prior to proceeding to the more demanding tests.

The LOFT basic test plan consists of five test series ranging from nonnuclear LOCEs to full-power LOCEs. These test series are defined in Table IV in the planned chronological order.

TABLE IV
LOFT BASIC TEST SERIES PLAN

<u>Test Series</u>	<u>Type of Test</u>	<u>System Parameters</u>
L1	Nonnuclear LOCE	Isothermal system and component blowdown tests
L2	Low-power LOCE	Typical ECCS configuration and design point performance
L3	Full-power LOCE	Typical ECCS configuration and design point performance
L4	Full-power LOCE	Typical ECCS configuration and degraded system performance
L5	Full-power LOCE	Improved ECCS designs

3. PHYSICAL DESCRIPTION OF THE LOFT EXPERIMENTAL SYSTEM

Figure 24 is an aerial photograph of the LOFT facility. Significant elements of the facility are identified.

3.1 Mobile Test Assembly

The mobile test assembly (MTA) is a rail-transportable test dolly on which the primary coolant system and the reactor system are mounted as shown in Figures 25 and 26. The MTA concept was used to enhance the versatility of the basic facility and to provide for possible removal and replacement of the reactor and primary coolant systems. The experimental system is designed for a target design capability of 26 minimum and 260 maximum LOCEs and a design life of 30 years. (Components not capable of withstanding that many repeated LOCEs may be replaced.) The actual experimental program is expected to be completed within fewer tests. The first reactor core is designed to operate intermittently or continuously for up to 2,000 effective full-power hours (efph).

3.2 Primary Coolant System

The primary coolant system is shown in Figure 27. For the first sequence of tests, the LOFT coolant system has been designed to be representative of a four-by-four PWR primary loop. The operating parameters for the LOFT primary coolant system, such as system pressure, reactor outlet temperature, and core enthalpy rise, were selected to be similar to the parameters utilized in the current design for PWRs. The core coolant enthalpy rise is considered to be of fundamental importance to the experiments. The core length, therefore, was made sufficiently long so that the maximum PWR enthalpy rise could be achieved and maintained. The core flow rate is variable and may be adjusted over as wide a range as experimental procedures may require.

Special nondestructive test baseline features are provided to ensure (a) prompt requalification of the reactor and primary coolant systems subsequent to a LOCE and (b) a

Fig. 24 Aerial photograph of LOFT test facility.

Fig. 25 MTA entering containment building.

Fig. 26 Side view of LOFT MTA.

Fig. 27 LOFT primary coolant system with blowdown loop.

relatively rapid turnaround time to prepare the integral test system for a retest. The major elements of the system are briefly described in the following.

The reactor vessel has two cold and two hot leg nozzles which are in the same horizontal plane and located above the core. The steam generator is a vertical shell and U-tube heat exchanger with the primary coolant in the tube side and is mounted such that its entire volume drains into the reactor vessel. The dual primary coolant pumps are of the canned-rotor, zero-leakage type. Variable speed operation, necessary for versatility in selection of test conditions, is provided by driving the pumps. A motor-generator set having variable speed control is provided. Coastdown simulation of PWR pumps is provided by flywheel driven synchronous generators electrically connected to the pump motors. The pressurizer is a vertical, cylindrical vessel with immersion-type electrical heaters, a surge line and a coolant spray line connecting to the primary loop, and pressure relief valves.

3.3 Blowdown LOCA (Simulation) System

The blowdown loop which is also connected directly to the reactor vessel, contains the following major components: steam generator simulator; pump and inlet loop seal simulator; isolation and blowdown valves; and reflood assist bypass. Suitably sized pipe spool with orifice plates are used to limit flow area and to modify loop flow resistances. The specific size of each resistance is determined by the type and location of the break being

simulated (that is, an outlet break, an inlet break, or a break between the steam generator and pump). Blowdown initiation (break simulation) is by means of two quick-opening valves, which can open in 10 to 50 msec.

Prior to initiation of a blowdown, the isolation valves in the blowdown lines are opened. These valves are located upstream from the quick-opening blowdown valves and prevent premature blowdowns and also provide the means of isolating and refilling the primary coolant system after testing.

The coolant in the hot leg blowdown line and the cold leg blowdown line will be maintained essentially equal to the temperature of the corresponding active loop leg by means of a small but continuous flow in recirculation lines connected to the active loop prior to the initiation of a LOCE.

A reflood assist bypass connects the 14-in. portion of the blowdown hot leg to the 14-in. portion of the cold leg through parallel 10-in. gate valves. The reflood assist bypass is classified as a safety feature. It can be used to increase the maximum reflood rate in the event of a LOCA in LOFT. Also, during a LOCE with blowdown from the cold leg, this bypass can be used after the blowdown is completed to increase the core reflood rate to values larger than those of PWRs. The increased core reflood rate is achieved by the lower resistance to steam flow from the core outlet to the pressure suppression tank manifold which results when the bypass is opened.

The purpose of the LOFT pressure suppression system is to simulate containment back pressure response of PWRs and to contain the blowdown effluent. This system greatly simplifies recovery following a LOCE and requalification of the system for succeeding tests. The LOFT containment vessel volume is too large to produce a typical maximum LOCE back pressure, and fission product cleanup times would also be excessive compared to those for the pressure suppression system.

3.4 Reactor System

The LOFT core design is based on the use of a 55-MW(t) core nominal power level. This power level was selected to provide a plant large enough to furnish test results applicable to PWRs, at a reasonable cost. Engineering studies have indicated that this power level will provide power profile, core temperature differentials, and peak-power characteristics sufficiently representative of PWRs, and that the resulting core design can be adequately instrumented without disturbing these characteristics. A peak linear power density (19 kW/ft) was selected to permit testing at the peak values typical of those currently being designed into PWRs.

The axial and radial flux profiles, thus the axial and radial peaking factors, were chosen to give a spatial power distribution which would be representative of local core power profiles of a PWR. The reactor system consists of the following major components.

The reactor vessel, shown in Figure 28, has a coolant volume which is a volume-to-power scaled to a typical PWR reactor vessel. The vessel is large enough to allow its use with longer and, if desired, larger, higher-power-rated cores. Internals consist of the core support barrel, upper and lower core support structures, flow skirt, and the filler assembly. The filler assembly is used to obtain a coolant volume distribution representative of that of PWRs and to permit the use of various size cores within the same reactor pressure vessel. The fuel assembly modules for the 5.5-ft-active-length reactor system consist of fuel rods, control rods, an upper fuel assembly support structure, test instrumentation, and an integral instrument lead, reactor vessel penetration assembly. Typical PWR fuel assembly materials and configurations are used, and the basic assembly design is based on a 15 x 15 open lattice array with typical end box and spacer grid designs. Three basic fuel assembly designs are utilized for the core as shown in Figure 29: (a) One central 15 x 15 fuel assembly. The fuel assembly support structure and the instrument lead reactor vessel penetration and external termination stalk are an integral part of this center fuel assembly, extending vertically to allow replacement without disturbing the other outer fuel assemblies. (b) Four outer 15 x 15 fuel assemblies that have control rod clusters. The four outer fuel assemblies are similar to the center fuel assembly except that they contain the control rods and three of them have lesser densities of test instrumentation. (c) Four triangular-shaped corner fuel assemblies containing some instrumentation. One of the two instrumented assemblies contains the startup source. The construction of the triangular fuel assemblies is similar to the central fuel assembly except that the core section is triangular.

Each control rod assembly is made up of 20 control rods. The materials and configuration of the assembly are typical of those of a PWR configuration except for the reduction in length. The control rod drives are roller-nut-type units.

3.5 Emergency Core Cooling System

The LOFT ECCS is classified as a safety feature as well as a principal experimental system and is composed of three subsystems [high-pressure injection system (HPIS), accumulator system, and low-pressure injection system (LPIS)], each with its redundant subsystem similar to those in PWRs. Injection points are provided for each of the subsystems and, by preselected valving, any of the several design variations found among the various PWR designs may be simulated. Other injection points such as upper head and lower plenum injection are provided for possible use in alternate ECCS programs.

A schematic of the ECCS is shown in Figure 30. The emergency coolant is supplied by either of two high-pressure injection positive displacement pumps and by either of two nitrogen gas driven accumulators. Each HPIS pump has a capacity which can be preset between 2.7 and 27 gpm at up to 2,500 psig discharge pressure. Each LPIS pump has a capacity of up to 300 gpm at 325 feet head, and each accumulator contains about 87 ft³ (650 gallons) of coolant and 41 ft³ of pressurizing nitrogen which can be preset at pressures from 0 to 1,000 psig. LPIS and accumulator discharge lines are orificed as required to simulate various PWR emergency coolant injection systems. The accumulators are equipped with an adjustable height standpipe which allows varying the effective accumulator volume.

Fig. 28 LOFT reactor vessel and internals.

Fig. 29 Cross section of LOFT core with in-core instruments.

Fig. 30 Schematic of LOFT ECC system.

In addition, the LPIS has heat exchangers and ion-exchange filters to provide for long-term core cooling and fission product removal by the continuous recirculation of water from the pressure suppression tank or the containment vessel pressure suppression pump. Provisions also exist to permit recirculation from the blowdown system hot leg through the LPIS pumps and heat exchangers and back into the reactor vessel for post-LOCE recovery.

4. EXPERIMENTAL INCORE INSTRUMENTATION

The basic concept for the incore measurements is to heavily instrument the central fuel assembly (Assembly 5) and instrument the remainder of the core by quadrants. Three quadrants of the core are relatively heavily instrumented, and the remaining quadrant is uninstrumented. This uninstrumented quadrant will allow some assessment of the effects of the instrumentation. Implementation of this concept, taking into account minimizing the number of one-of-a-kind assemblies, results in Assemblies 1 through 6 containing instrumentation and Assemblies 7, 8, and 9 having no instrumentation. The instrumentation is tabulated in Table V, and the general location was illustrated in Figure 29.

TABLE V

LOFT 5.5-FOOT CORE TEST INSTRUMENTATION SUMMARY^[a]

Sensor Type	Total Measurements by Type in Each Fuel Assembly						Total
	<u>1</u>	<u>2</u>	<u>3</u>	<u>4</u>	<u>5</u>	<u>6</u>	
Fuel pin cladding temperature	11	29	11	29	76	29	185
Guide tube temperature		1		1	8	1	11
Coolant temperature	3	6	3	6	11	6	35
Density-velocity -- temperature	1		1		1		3
Coolant pressure (free field)	1		1				2
Coolant pressure	1		1				2
Core differential pressure	1		1				2
Neutron flux (fixed)		1		1	1	1	4
Neutron flux (scan)	1		1		2		4

[a] All numbers of detectors are approximate; minor changes may occur prior to testing.

5. LOFT LOCE SUBCOOLED BLOWDOWN BEHAVIOR

V. T. Berta

Before loss-of-coolant experiments are performed in LOFT, system response is predicted analytically with the techniques normally used for reactor design and safety analysis. These predictions serve two main purposes: (a) to determine the adequacy of the LOFT design (including instrumentation), and (b) to determine the accuracy of the computer codes for predicting system response.

The initial portion of the LOCE transient, normally referred to as the subcooled blowdown, is analyzed by use of the WHAM^[11] computer code. In this report, the application of the WHAM code to the LOFT primary coolant system (PCS) is described with respect to establishing acoustic wave energy loss terms on which the overall rate of depressurization and the amplitudes of pressure oscillations during subcooled blowdown depend.

The values of the energy loss terms are dependent on the size and geometric characteristics of the system (lengths and diameters of components such as pipes). Data for determining the values of the acoustic energy loss terms were obtained from an experimental system similar in size and geometry to the LOFT PCS.

Correlation of the WHAM code calculations with the data from the experimental system is discussed. The WHAM code, utilizing the loss-term values, then was applied to the LOFT PCS to determine the effects of the energy loss terms on the absolute and differential pressures at intended measurement locations. Initial temperature and pressure conditions were selected to be approximately those for the first LOFT LOCE. Different values of the loss terms were used in an attempt to place bounds on the variation in subcooled blowdown behavior.

5.1 WHAM Code and System Modeling

Accurate calculations of pressure as a function of time during subcooled blowdown are dependent on how well the geometry of the system and the acoustic wave energy loss effects that take place in the system can be defined.

The hydraulic model of the LOFT PCS geometry on which the WHAM code operates has a resolution given by the equation

$$R = ct/2$$

where c is the sonic velocity and t is the time step increment. The resolution for full power steady state operation, for example, is 2.04 inches in the cold legs and 1.65 inches in the hot legs of the PCS. These values are small in comparison with the wavelength of the initial acoustic wave which results from a 10-msec opening of the quick-opening blowdown valves (the shortest opening time of the valves). The wavelength is approximately 30 feet ($\lambda = ct$

where $t = 0.01$ sec). Thus, the resolution of the WHAM model used to describe the geometry of the LOFT PCS is sufficiently good for calculation of the frequency content of pressure oscillations in the PCS during subcooled blowdown.

The amplitude of the acoustic pressure waves, the amplitude and phase of the resulting pressure oscillations, and the overall rate of depressurization of the system are governed by the energy losses in the system. In effect, the energy losses in the system result in absorption of the energy of acoustic pressure waves as they propagate through the coolant system. This absorption results in an attenuation or reduction in the amplitude of the acoustic pressure waves which affects both the amplitude and phase of pressure oscillations in the system.

The WHAM code (version MOD-005) contains two energy loss terms. The first term is the energy loss coefficient at the break in the system. This coefficient primarily controls the overall rate of depressurization of the system. This loss coefficient is part of the reflection coefficient for acoustic waves incident on the break from the system. The purpose of the reflection coefficient is to determine how much of the acoustic wave energy is reflected back into the system and, therefore, by inference, how much is radiated out the break. The loss coefficient has the value "1" for the no loss case. The larger the energy loss, the smaller the value for the loss coefficient. That is, the larger the energy loss, the less energy is reflected back into the system.

The second energy loss term is defined as the "system loss factor". This term primarily controls the amplitude and phase of the pressure oscillations that occur during blowdown. The WHAM code applies the loss factor to the entire coolant system geometry as an overall energy loss in acoustic pressure waves. Energy loss in acoustic waves in liquids is similar to energy loss in sound waves in air. Sound waves in air are dissipated by attenuation (absorption) and dispersion. In a coolant system such as a PWR PCS, dissipation of acoustic pressure wave energy occurs primarily by absorption because of the confinement of the acoustic pressure waves. Energy losses by absorption are due to viscous effects, heat conduction effects, and molecular energy exchange effects. Viscous effects are dominant by far in polar liquids such as water. Thus, the loss factor is dependent on the size of the coolant system, especially on the cross-sectional areas.

The WHAM code utilizes a device referred to as a "friction orifice" to handle the loss factor. A fictitious orifice is placed at each internal node of the hydraulic segments that make up the system model. The system loss factor is used by the code to calculate an orifice size which results in a loss in amplitude of the acoustic pressure waves passing each node point. Preliminary comparisons of available experimental data with WHAM calculations have shown that the loss factor is dependent on cross-sectional areas. Preliminary values of the loss factor determined thus far range from approximately 15 for small systems such as pressure detector standoff pipe geometries down to approximately zero for large diameter systems having a minimum of two components such as the early Semiscale experiments involving a tank and connecting pipe.

5.2 Experimental Energy Loss Data

The LOFT pump experiments conducted by Aerojet Nuclear Company and Combustion Engineering provided subcooled blowdown data from a coolant system similar in size and geometry to the LOFT PCS. These data were used to determine the approximate values of the break loss coefficient and the system loss factor for the LOFT PCS. The experimental system, the specific experiment, and the determination of the loss term values are described in the following subsections.

5.2.1 Experimental Facility Description. The experimental system was a modified steam-water-separator-test facility located in the KDL Laboratory of Combustion Engineering, Inc. (CE), Windsor, Connecticut.

The basic system is shown in Figure 31. The pump was placed in a loop approximately 18 feet below a 180-ft³ drum. The system was pressurized by an auxiliary feed pump. Temperature was controlled by means of a feed-bleed system whereby either cool water or steam at temperatures to 635°F could be fed. Just prior to a test, the system was isolated by closing off both the feed and the bleed lines.

Fig. 31 System for LOFT PCS pump test program.

Flow rates in the main loop were controlled with the 10-inch valve and were measured with an orifice assembly during single-phase steady state conditions. Auxiliary systems included pump-coolant supply, pump-injection-water supply, data acquisition, boiler, demineralizer, deaerator, and power supply.

Decompression was initiated by bursting a rupture-disc assembly which was located at Port B during the subcooled tests and at Port A during the slow, saturated blowdown. An expansion chamber with a volume of 0.7 ft^3 was attached to Port B. Blowdowns from Port A were vented to the atmosphere.

Six pressure-measuring transducers were placed in the lower portion of the system to provide sensing redundancy as well as data for instrument sensitivity and response studies. One flush-mounted and two standoff-mounted transducers, with standoff lengths of 10 inches and 20 feet, were placed near the pump suction. One flush-mounted and one 2-foot standoff-mounted transducers were located near the pump discharge. A 2-foot standoff-mounted transducer was located on the motor-injection port. Other parameters recorded were outer pump-volute temperatures, motor-cavity temperature, pump vibration, volute strains, and pressure differentials across the pump. Data records were made in parallel on magnetic tape and on an oscillograph.

5.2.2 Subcooled Blowdown Experiment Description. The rupture-disc assembly was located at blowdown Port B (Figure 31), and Port A was sealed with a blanking flange. The system was filled, vented, and pressurized. The pump was started and run at 60 Hz and 5,000-gpm flow in order to heat the system to 260°F . When this temperature stabilized, the power supply was reduced to 35 Hz and the flow rate was adjusted to 2,000 gpm (conditions near to those anticipated in the LOFT facility). The expansion volume was evacuated to 29.5 inches of mercury. Data records were begun, and the decompression was initiated.

Several blowdown experiments were conducted, beginning at a low initial inlet pressure (560 psig) to subject the pump to hydraulic loading similar to that expected in LOFT LOCEs. The blowdown generating the largest pump loads was conducted with a 2-inch orifice and an initial inlet pressure of 1,515 psig. The data from this experiment are used in a comparison with the WHAM code results in the following section for determination of the energy loss coefficients.

5.3 WHAM Correlation With CE Experimental Data

A hydraulic model of the CE loop was defined for the WHAM MOD-005 code. Several computer runs were made with the model unchanged except for the values of the loss coefficient at the break and the system loss factor. The results of four code runs are shown compared to the experimentally obtained pump inlet pressure in Figures 32 through 35.

The "no energy loss" case is shown in Figure 32. Both the overall rate of depressurization and the amplitudes of the oscillations of the WHAM calculations are greater than those measured. Reducing the orifice loss coefficient from 1.0 to 0.65 resulted in a

Fig. 32 Comparison of WHAM pump inlet pressure results with CE loop experimental data -- most conservative case.

Fig. 33 Comparison of WHAM pump inlet pressure results with CE loop experimental data -- most realistic orifice coefficient.

Fig. 34 Comparison of WHAM pump inlet pressure results with CE loop experimental data -- most realistic case.

Fig. 35 Comparison of WHAM pump inlet pressure results with CE loop experimental data -- least conservative case.

calculated rate of depressurization that agrees well with experimental results as shown in Figure 33. However, the calculated pressure oscillations are observed to be slightly out of phase with the experimental oscillations and of slightly longer duration. Also, the calculated amplitudes remain slightly higher than the experimental amplitudes. Use of a system loss factor of three (Figure 34) changes the oscillations such that the calculated oscillations agree quite well with the measured oscillations. Increasing the system loss factor further to a value of five (Figure 35) reduces the amplitudes of the oscillations and changes the character of the oscillations sufficiently such that the calculations no longer agree with the experimental results.

Thus, the values which are most representative of blowdown in the CE loop are 0.65 for the loss coefficient at the break and three for the system loss factor. Since the coolant characteristics are similar in the CE loop and the LOFT PCS and since the two systems have similar pipe sizes and lengths and similar tank-plenum volumes, these values are considered also to be representative of LOFT LOCE subcooled blowdowns.

5.4 Subcooled Blowdown Behavior

The effects of the energy loss terms on the subcooled blowdown behavior in the LOFT PCS were calculated for seven differential pressure measurement locations and fourteen absolute pressure measurement locations. The identification of the measurements and their locations are shown in Figures 36, 37, and 38 for the primary loop, blowdown loop, and reactor vessel, respectively.

Fig. 36 Primary loop pressure measurements.

Fig. 37 Blowdown loop pressure measurements.

Fig. 38 Reactor vessel pressure measurements.

The initial temperature and pressure conditions, 540°F and 1,350 psig, and a 50% break area were selected for the subcooled blowdown analysis because these values are very near those for the first LOCE. Three cases, defined in Table VI, were run which included the expected behavior and two extremes for the purpose of placing bounds on the blowdown behavior. The dependence of the absolute and differential pressures on the loss terms is shown for the measurement locations defined. Energy loss effects on differential pressures are shown in Figures 39 through 45. Energy loss effects on absolute pressures are shown in Figures 46 through 59.

TABLE VI
ENERGY LOSS CASES FOR LOCE L1-1 SUBCOOLED BLOWDOWN PREDICTIONS

<u>Case</u>	<u>Title</u>	
1	No Energy Losses	This case is defined as having an orifice coefficient at the break of 1.0 and an over-all system loss factor[a] of zero.
2	Expected Energy Losses	The orifice coefficient value of 0.65 and the system loss factor is 3. These values were found to be reasonable from analysis of CE loop LOFT pump test data.
3	Largest Expected Energy Losses	The orifice coefficient is the same as for Case 2. The system loss factor is 5.

[a] The system loss factor is used in the WHAM code to account for acoustic wave energy loss. The system loss factor is dimensionless and is used in the same manner as the friction coefficient by placing a fictitious orifice at each internal node the size of which is related to the value of the loss factor.

As the figures show, the attempt to place bounds on the expected behavior met with only limited success. All figures show that the expected behavior is bounded initially. However, because of the phase shifts and frequency shifts caused by the loss terms, the expected behavior was not bounded over the entire transient.

5.5 Conclusions

The absolute and differential pressure response during subcooled blowdown exhibits a strong dependence on the acoustic wave energy losses in the coolant system. The energy losses affect the overall rate of depressurization and the amplitude and phase of the pressure oscillations occurring during subcooled blowdown. Because of the effect on the phase of pressure oscillations, the attempt at bounding the expected energy loss case with cases of higher and lower energy losses met with only limited success. The expected energy loss case

Fig. 39 Differential pressure across primary coolant pumps - measured by PDE-PC-1.

Fig. 40 Differential pressure across steam generator - measured by PDE-PC-2.

Fig. 41 Differential pressure across steam generator simulator -- measured by PDE-BL-6, PE-BL-7, and PDE-BL-8.

Fig. 42 Differential pressure across pump simulator -- measured by PDE-BL-5.

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Fig. 45 Differential pressure across core simulator -- measured by PDE-CS-1.

Fig. 46 Pressure between reactor vessel and steam generator -- measured by PE-PC-2.

Fig. 47 Pressure in steam generator outlet pipe -- measured by PE-PC-3A.

Fig. 48 Pressure in primary loop reactor vessel inlet -- measured by PE-PC-1.

Fig. 49 Pressure in blowdown loop hot leg 5-inch pipe outlet -- measured by PE-BL-4.

Fig. 50 Pressure in steam generator 5-inch outlet pipe -- measured by PE-BL-3.

Fig. 51 Pressure between pump simulator and steam generator simulator -- measured by PE-BI-6.

Fig. 52 Pressure in blowdown loop hot leg 14-inch pipe -- measured by PE-BL-2.

Fig. 53 Pressure in blowdown loop cold leg 14-inch pipe -- measured by PE-BL-1.

Fig. 54 Pressure in upper part of downcomer near blowdown loop inlet -- measured by PE-1ST-3FF and PE-ST-3A.

Fig. 55 Pressure in lower part of downcomer near blowdown loop inlet -- measured by PE-1ST-1FF and PE-1ST-1A.

Fig. 56 Pressure in upper part of downcomer near primary loop inlet -- measured by PE-2ST-3FF.

Fig. 57. Pressure in lower part of downcomer near primary loop inlet -- measured by PE-2ST-1FF.

Fig. 58 Pressure in reactor vessel upper plenum -- measured by PE-CS-2FF and PE-CS-1A.

Fig. 59 Pressure in reactor vessel upper plenum -- measured by PE-CS-1FF.

is bounded initially in the pressure transient but not at all times during the pressure transient. Thus, the magnitude of the energy losses must be known with good accuracy in order to provide accurate forcing functions by means of the WHAM code for structural response measurement predictions.

III. THERMAL FUELS BEHAVIOR PROGRAM

J. G. Crocker, Program Manager

The objective of the Thermal Fuel Behavior Program being conducted by Aerojet Nuclear Company is to produce data which will contribute to an improved understanding of the behavior of reactor fuels during a range of normal and off-normal reactor conditions including postulated severe accidents. This objective is to be accomplished through a coordinated analytical and experimental effort. Analytical capability will be developed by using experimental data to assess the completeness and the accuracy of the analytical models. These models will be used for predicting fuel response in full-scale reactors during various operating and accident conditions.

Testing is to be conducted in the Power Burst Facility (PBF). The objectives are to determine the response of a fuel-cladding system to off-normal reactor conditions. The range of transient events to be studied occur in the following postulated accidents:

- (1) An accident in which a break in the primary cooling system occurs
- (2) An accident in which an overpower or undercooling situation, or both, occurs
- (3) An accident in which a very rapid and large reactivity increase occurs, which leads to a power excursion.

The spectrum of off-normal conditions considered will cover the range extending from the normal operating conditions to conditions predicted to result in cladding and fuel melting. The consequences of exceeding the normal operating limits will be monitored during testing by recording the dynamic response of key variables and by posttest examination of the test fuel. For each test series, analyses will be completed using applicable models adapted to the specific test configuration. Test data will be compared with the analytical predictions to help evaluate the experimental phenomena and to provide a basis for evaluation and modification of the models.

The following sections identify the tests series to be conducted in the Thermal Fuels Behavior Program, briefly describe the Power Burst Facility, and present results of initial gap conductance tests in that facility.

1. TEST SERIES DESCRIPTION

A brief discussion of the objectives of each test series to be conducted in the Thermal Fuels Behavior Program follows.

1.1 Power-Cooling-Mismatch (PCM-20)

The primary objective of the PCM-20 Test Series is to provide in-pile experimental data on the behavior of PWR fuel rods under power-cooling-mismatch conditions. To meet this objective, tests will be performed to characterize the behavior of single unirradiated PWR fuel rods at normal operating power densities through the range of densities over which critical heat flux (CHF) is exceeded, and to determine the modes, mechanisms, and consequences of fuel cladding failure under various test conditions of overpower or undercooling.

1.2 Gap Conductance

The primary objectives of the Gap Conductance Test Program are: (a) to determine the effects of fuel design parameters (such as gap size and fuel density) on the magnitude of the gap conductance and (b) to evaluate the capability of the thermal oscillator technique for providing accurate gap conductance values. If the thermal oscillator technique can be used to obtain gap conductance values, this method is to be used in the Irradiation Effects Test Program where more conventional methods cannot be employed.

1.3 Irradiation Effects (IE)

The objectives of the Irradiation Effects Test Series is to evaluate the behavior of irradiated fuel rods under PCM accident conditions and to develop techniques to simulate that behavior with fuel rods made from various treatments of irradiated and unirradiated cladding and fuel components. The simulation program will focus on the metallurgical aspect of these treatments. Included will be analyses of induced mechanical properties and their thermal recovery with particular regard to ductility and strain hardenability. Knowledge obtained from fuel rod cladding burst temperature, pressure, and strain analyses coupled with detailed pre- and posttest examinations will yield data essential to the early development of successful analytical models of irradiation effects. The initial test materials in the form of irradiated fuel rods and components were obtained from the Saxton Experiment Reactor Program^[12].

1.4 Loss-of-Coolant Accident (LOCA)

The objective of the Loss-of-Coolant Accident Program is to measure the thermal, mechanical, chemical, and internal gas pressure response of both irradiated and unirradiated fuel rods during each major phase of a variety of loss-of-coolant accident situations. Representative irradiated and unirradiated PWR fuel rods with varying rod internal pressures will be tested in isolated minishrouds (4 rods per test) and in 16-rod clusters to evaluate the effects of rod deformation and rod-to-rod propagative interactions.

The first LOCA tests will be heatup tests to simulate and evaluate appropriate, past out-of-pile ballooning tests. A second series of LOCA tests involving the complete LOCA sequence of blowdown, heatup, and reflood will then be conducted. These tests are expected to show the extent of deformation and chemical reaction during each phase of the transient.

1.5 Reactivity Initiated Accident (RIA)

The objectives of the Reactivity Initiated Accident Test Series are to (a) determine threshold energy limits for incipient fuel rod failure and prompt fuel dispersal for test environments typical of power reactor conditions, (b) determine the effects of irradiation, cluster size, coolant flow, and initial power on the threshold energy limits, and (c) measure the fuel thermal, mechanical, and gas pressure response during the test. The data are to be used for the development of an improved RIA analytical model. Irradiated and unirradiated Saxton type fuel rods and BWR type fuel rods will be used for these tests.

1.6 PCM Small Cluster

The objective of the PCM Small Cluster Test Series is to measure fuel rod interaction and failure propagation, if any, in 9- and 16-rod clusters during a power-coolant-mismatch transient. Fuel rod thermal, mechanical, chemical, and gas gap pressure responses will be measured to determine whether they differ significantly from those measured during comparable single-rod tests.

1.7 Inlet Flow Blockage

The Inlet Flow Blockage Program is concerned with the accident resulting from blockage of the inlet orifice to a fuel assembly which results in a relatively slow decrease (seconds) in both coolant flow and bundle power. For very large flow area reductions, the coolant flow is reduced, whereas the power generation in the bundle remains at a relatively high level. The objective is to determine fuel rod behavior, interaction between rods, and the potential for failure propagation to adjacent assemblies. The bundle will consist of 25 rods. By varying the coolant blockage, the thermal-mechanical and chemical behavior of the bundle will be evaluated.

2. POWER BURST FACILITY DESCRIPTION

The Power Burst Facility was designed to provide experimental data which will aid in defining the behavior of nuclear fuels in off-normal operating conditions. The testing program includes tests which will extend to the point of fuel cladding failure including fuel-coolant interactions under postulated accident conditions. These light water reactor safety tests will be performed in PBF by testing single rods and clusters of test fuel rods in a central test space in the core. The PBF reactor can be operated in three modes: (a) a steady state mode with power levels up to 40 MW, (b) a natural power burst mode which yields reactor periods as short as 1.3 msec and peak powers as large as 240 GW, and (c) a shaped burst mode (approximating square wave power shape) resulting in energy generations up to 1,500 MW-sec. Because of this versatility, PBF can provide the power and energy densities in test fuel rod clusters that are analytically derived for a broad spectrum of postulated reactor accidents. The facility contains a pressurized water flow loop that permits the control of the coolant conditions of flow rate, temperature, and pressure in the test fuel rod environment.

Any conceivable failure of the test fuel during the test (such as cladding failure, gross fuel melting, fuel-coolant interactions, fuel failure propagation, fission product release, or metal-water reactions) can be safely contained by the PBF in-pile tube without damage to the driver core.

This section provides a summary description of the Power Burst Facility and the operational capability of the reactor, test loop, and associated ancillary equipment.

2.1 Facility Description

The Power Burst Facility is located within the SPERT reactor complex at the Idaho National Engineering Laboratory. The reactor building, shown in Figure 60, consists of a reactor bay flanked by low wings on either side above grade and two basement levels below grade. The building houses the reactor vessel, a canal, the necessary auxiliary equipment, and mechanical setup and working areas.

The reactor vessel and adjacent canal, which is connected to the vessel by a removable gate, are illustrated in Figure 61. The canal will be used for transfer and temporary storage of PBF reactor fuel and the test fuel assemblies. The open-top reactor vessel provides easy access for installation and removal of test hardware.

The PBF core is approximately a right-circular fuel annulus, 52 inches in diameter and 36 inches high enclosing a centrally located, vertical test space 8.25 inches in diameter. Figure 62 is a cutaway perspective view of the core, and Figure 63 is a cross section showing the location of the different types of rods, fueled and nonfueled. Control of the reactor is achieved through use of the eight control rods and four transient rods each consisting of a cylindrical annulus of boron carbide positioned in a steel canister and operated in air-filled shrouds. The transient rods are used in conjunction with the control rods to initiate and control natural and shaped power-bursts, whereas steady state operation is maintained through use of only the control rods.

The PBF fuel, illustrated in the cross section view of Figure 64, is contained in 0.751-inch OD, Type 304L stainless steel tubes having a 28.5-mil nominal wall thickness. The ceramic fuel pellets are 0.559-inch OD and are composed of 30.6 wt% UO_2 , 61.8 wt% ZrO_2 , and 7.6 wt% CaO . The fuel pellets are surrounded by a 19-mil-thick helium gas gap which is bounded by a 48-mil-thick thermal insulator sleeve composed of CaO-ZrO_2 . The fuel rods are grouped in fuel canisters containing from 28 to 63 fuel rods, which form a close-packed, square fuel lattice with a 0.792-inch pitch.

Shim rods and reflector rods, denoted by shading and crosses, respectively, in Figure 63, are solid Type 304 stainless steel rods of the same physical dimensions as the fuel rods. The shim rods provide a means of increasing the core excess reactivity to 6.7 dollars by replacement with fuel rods, and the reflector rods serve to decrease flux peaking at the core periphery and help maintain the short prompt neutron lifetime in the PBF core.

Aerojet Nuclear Company

Fig. 60 Power Burst Facility.

Fig. 61 Reactor vessel and canal dimensions.

Fig. 62 Cutaway of PBF core.

LEGEND

- Ternary Fuel Rod
- ⊗ Stainless Steel Reflector Rod
- ⊙ Aluminum Filler Rod
- Stainless Steel Shim Rod
- ⊕ Source Rod

Canal

Fig. 63 Cross section of PBF core.

Fig. 64 Cross section of PBF fuel rod.

Filler rods, denoted by dots in Figure 63, are solid Type 6061-T6 aluminum rods and are also the same physical dimensions as the core fuel rods. The filler rods serve to balance the coolant flow rate in the canisters and lend structural strength to the core. Light water is used in the core coolant system which is designed to remove the heat generated in the PBF core during steady state operation up to 40 MW.

The reactor building has been constructed with service and maintenance cubicles for two separate coolant flow loops for the test space. The loop that is presently installed provides the capability for reproducing the coolant flow rate, temperature, and pressure environment representative of pressurized water reactors. This loop can be modified to provide the coolant flow rate, temperature, and pressure typical of boiling water reactors. The capability for installing a second coolant loop in a separate cubicle provides versatility for future testing of reactor fuel rod clusters for advanced reactor types.

Some of the pertinent physical and operational characteristics of the PBF are listed in Table VII. A detailed description of PBF is given in the Power Burst Facility Final Safety Analysis Report^[13].

3. PBF CAPABILITIES

The PBF contains a pressurized water coolant flow loop that can provide a wide range of coolant conditions in the central test space. Test fuel rod clusters will be contained in the in-pile tube (IPT), a high strength, thick-walled tube constructed of Inconel 718. The reentrant design of the IPT facilitates insertion and removal of the tube within the test space because the inlet and outlet connections to the loop coolant system are both located at the upper end of the IPT above the PBF core. Coolant flow enters the top of the IPT and is directed downward, around the outside of the fuel rod cluster, by the flow tube or shroud. At the bottom of the IPT, coolant flow reverses direction and flows upward through the fuel rod cluster inside the flow tube, and then flows out of the IPT at the top.

TABLE VII
EXPERIMENTAL ENVELOPE FOR OPERATION OF PBF^[a]

Parameter or Variable	Design Capabilities and/or Limits
Max core steady power	40 MW
Max core power in shaped burst	1,000 MW
Max core power in natural burst	240 GW nominal for design burst ^[b]
Max duration for steady power operation	48 hr
Max power for initiation of shaped burst	100 kW
Max power for initiation of natural burst	30 MW nominal
Max duration at power for shaped burst operation	Variable depending on power level; eg, 1.43 sec at 1,000 MW; 3.0 sec at 500 MW; 15.7 sec at 100 MW; 36.0 sec at 60 MW
Max core fuel temperature for steady power operation	2,100°C nominal at 40 MW
Max core fuel temperature for burst operation	2,350°C without coupling; 2,460°C with coupling
Max core fuel enthalpy for steady power operation	1,780 cal/cc nominal at 40 MW
Max core fuel enthalpy for burst operation	2,040 cal/cc without coupling; 2,465 cal/cc with coupling
Max reactivity insertion for natural burst	2.39\$ initially, nominal for 2.5 msec period; 3.67\$ nominal for design burst upon verification of conservative coupling
Max reactivity insertion for shaped burst operation	4.5\$ nominal for 2,040 cal/cc peak core enthalpy
Max transient rod speed for natural burst operation	375 in./sec
Max transient rod speed for shaped burst or steady power operation	20 in./sec
Min asymptotic period for natural burst operation	2.5 msec initially; 1.3 msec for design burst upon verification of conservative coupling values
Max energy release for natural burst operation	1,450 MW-sec nominal for design burst; 1,750 MW-sec nominal for design burst with coupling
Max reactivity coupling from test fuel failure and/or test space voiding	Reactivity coupling plus reactivity inserted with control and transient rods shall not cause core fuel enthalpy to exceed 2,465 cal/cc
Min core shutdown margin	Greater than 8\$ for initial loading; at least 3\$ and greater than worth of a single control rod for future loadings
Min water head above core for transient or high power operation	1 ft below vessel liquid-full level
Max experiment fission power	2 MW
Max experiment Pu inventory	147 grams
Max test cluster size in reentrant IPT	45 typical PWR rods nominal; 21 typical BWR rods nominal
Max test cluster size (without flow)	76 typical PWR rods nominal; 44 typical BWR
Max fission product inventory in test fuels	That resulting at end of following operation history; 2 MW for 558 days, 42 days decay time; 2 MW for 48 hr, 7 days decay time
Max loop operating temperature	650°F
Max loop operating pressure	2,200 psig
Max transient source pressure within IPT	7,500 psi

[a] Currently, PBF is authorized to operate only in the steady state mode at power levels less than 33 MW.

[b] Design burst is defined as the natural burst initiated from zero power that results in 2,040 cal/cc at the core hotspot. Nominally, this requires a 1.3 msec period burst initiated by a 3.67\$ reactivity step, with an energy generation of 1,450 MW-sec.

The loop coolant system includes a pressurizer, a pump, heat exchangers for removing the energy imparted to the coolant by the test fuel rod cluster, a flow control valve, acoustic filters and thermal swell accumulators to attenuate pressure surges that may result from fuel failure, and electric heaters to control the inlet coolant temperature. Summary descriptions of the capabilities of the IPT, the loop coolant system, and the PBF core are given in the next three subsections.

3.1 In-Pile Tube (IPT)

In preparation for fuel rod testing, the test fuel rod cluster will be placed in the IPT which is then positioned in the PBF test space and connected to the coolant loop, forming the in-pile portion of the loop. The top reentrant type IPT is illustrated in Figure 65 in radial cross section and in Figure 66 in axial cross section.

The IPT is a thick-walled, Inconel 718, high-strength pressure tube that was designed to contain the steady state operating pressure plus the pressure surges that may result from test fuel failure. The maximum steady state pressure that will be maintained in the loop coolant system is 2,200 psig, and the IPT is designed to contain source pressures within the test cluster that are 7,500 psi above this steady state pressure without yielding. The IPT design allows for doubling of the 7,500 psi source pressure at boundary walls and also includes another engineering safety factor of two for impulse loading. Thus, the static

Fig. 65 Radial cross section of in-pile tube.

Fig 66 Axial cross section of in-pile tube.

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internal yield pressure of the IPT is about 32,200 psi. Under dynamic internal pressure loading, calculations show that the IPT can contain short rise-time pressure pulses up to about 230,000 psi without rupture^[14]. A penetration in the top head of the IPT can accommodate instrumentation leads for measurement of 25 test parameters in test fuel rod clusters.

The flow tube within the IPT has a nominal inside diameter of 4.75 inches. Details of the construction of the IPT and flow tube are shown in Figures 65 and 66. Flow enters the IPT through the lower nozzle (Figure 66) and flows down the annulus between the IPT wall and the flow tube. Flow reverses at the bottom of the flow tube just above the catch basket, flows up through the central region of the flow tube, and out the upper nozzle.

The flow tube itself consists of three sections. The upper section is constructed of stainless steel and the central section is made of zircaloy for neutron economy in the test fuel. The lower section of the flow tube is designated the "catch basket". It provides a heat sink and collection basin for any fuel fragments that may settle to the bottom of the in-pile tube. It thus protects the bottom of the IPT from local overheating. Both the upper and the lower sections of the flow tube contain screens capable of retaining fuel particles larger than 0.3-inch diameter within the IPT. Fuel particles between 0.3 inch and 0.038 inch are retained by the loop strainer located downstream from the IPT.

3.2 Capabilities of the Loop Coolant System

The loop coolant system, shown in Figures 67 and 68, provides cooling water at controllable pressures, temperatures, and flow rates to simulate coolant conditions of pressurized water reactors. Coolant flow is maintained by a constant-speed, centrifugal-type pump. From the pump, the coolant flows through a section of pipe having clamped-on, resistive-type electric heaters, a flow-control valve, and a flow sensor. The coolant then flows through a thermal swell accumulator, an acoustic filter, and then into the IPT. Thermal swell accumulators and acoustic filters are placed at the IPT inlet and outlet to attenuate large magnitude ($\leq 7,500$ psi) pressure surges, which could conceivably be generated as a result of fuels testing, to a maximum value of 500 psi. These components thus reduce the magnitude of the pressure surges propagating down the loop pipe. Located downstream from the acoustic filters are crossover lines that provide capability of reversing the direction of flow through the IPT.

After leaving the IPT, the coolant is routed through the second acoustic filter and on to the loop strainer, which is capable of catching and retaining particles larger than 0.038 inch. A loop cleanup and decontamination system is capable of recovering those fuel particles smaller than 0.038 inch that are not retained in the strainer. Bypass lines are provided to bypass the IPT or strainer if desired. The coolant flows from the strainer to the three parallel-connected heat exchangers, and the flow through each heat exchanger can be varied by individual valves. A bypass line, with a differential pressure-control valve, is provided to control the pressure drop across the heat exchangers. The coolant from the heat exchangers and the heat-exchanger bypass flows into a recombination tee that combines the cooler liquid from the heat exchanger with the hotter liquid from the bypass. From the recombination tee, the coolant flows to the pump inlet.

Fig. 67 Major components of PBF in-pile and loop coolant systems.

The heat removal system is capable of removing up to 50 MW-sec of energy generated within the test fuel during the power burst testing and up to 2.8 MW during steady state power operation.

3.2.1 Control and Ranges of Coolant Parameters

(1) Coolant Flow. Coolant flow rates within the loop are governed by the flow-control valve operating in conjunction with the flow sensor. Control instrumentation links the valve and flow sensor so that any flow between 100 and 800 gpm can be established and automatically maintained.

(2) Coolant Temperature. Coolant temperature control is achieved by the combined operation of the loop coolant heaters and heat exchangers. Major temperature adjustments are made by manually regulating the tube-side flow rates through the heat exchangers. Once the coolant flow through the heat exchangers has been regulated to maintain the approximate temperature, fine temperature control is achieved by regulating the power to the loop heaters. The capability of the system to establish and maintain a desired temperature is estimated to be $\pm 3^{\circ}$ over the entire temperature range.

(3) Coolant Pressure. Loop coolant pressure is established and maintained by the pressurizer connected to the loop system. Electric heaters immersed within the pressurizer generate steam that exerts a pressure on the closed loop. Once the desired system pressure is reached, the pressure-control instrumentation automatically controls the pressure by regulating the power to the heaters. The capability of this system to establish and maintain the desired pressure is estimated to be within ± 25 psi over the pressure range (excluding any pressure oscillations caused by the pump). The loop coolant system was designed for 2,500 psi. The acoustic filters were designed to attenuate large pressure surges ($\leq 7,500$ psi) to a maximum of 500 psi.

The capabilities of the coolant loop system are summarized in Table VIII.

3.3 PBF Driver Core

The operational capability of the PBF driver core which supplies neutrons to the test fuel rod clusters is limited by the established maximum core fuel temperature of $2,350^{\circ}\text{C}$. This limitation on the PBF core capability determines the allowable limits for each of the three different modes of PBF operation. The three operational modes, their associated power-level limits, and how these limits affect attainable power densities in test fuel clusters are discussed.

3.3.1 Modes and Levels of Operation. The PBF reactor is designed to operate in three different modes: (a) a steady power mode, (b) a shaped power burst mode initiated at zero reactor power (actually a few watts), and (c) a natural power burst mode initiated at reactor power levels up to 30 MW. The maximum PBF steady state power level has been established at 40 MW on the basis of calculational results that indicate a maximum core fuel centerline temperature of $2,100^{\circ}\text{C}$ at this power level. The calculations indicate that the $2,350^{\circ}\text{C}$ limit

TABLE VIII

CONTROL RANGES FOR PBF LOOP COOLANT SYSTEM

Ranges of Coolant Parameters	
Flow	Static or 100 to 800 gpm
Temperature (test cluster outlet)	Ambient to 630°F
Pressure	Ambient or 50 to 2,200 psig
Control of Coolant Parameters	
Flow	+5 gpm at 100 gpm to +20 gpm at 800 gpm
Temperature	+3°F (over entire range)
Pressure	+25 psi (over entire range)
Allowable Experiment Pressure Drops	To 285 psi
Experiment Heat Removal	
Steady power	2.0 MW
Power burst	50 MW-sec
Coolant Chemistry	
pH range	5.7 to 10.2
Conductivity	1.4 to 48 micromhos
Chlorides	<0.15 ppm
Total suspended solids	<1.0 ppm
Dissolved oxygen	<0.1 ppm
Strainer Particle Retention	≥0.038 inch

on maximum allowable core fuel temperature would be reached at a nominal PBF steady state power level of 46 MW, and the 40 MW level was established to allow for calculational uncertainty^[a]. Initially, PBF will be operated so that the integrated energy for any test does not exceed 40 MW-hr. Later in the program it may be operated at 40 MW for time periods up to 48 hours depending on program requirements.

3.3.2 Experiment Power Density. Fuel rods will be tested in PBF in the test space to determine the behavior of LWR fuel rods during postulated overpower and undercooling conditions. For fuel rod cluster testing at overpower conditions, the test fuel must attain damage-producing power densities but the PBF driver core itself must sustain no damage. A measure of the relative gain in test fuel power density over the power density in the driver core is a quantity denoted the Figure-of-Merit (FOM). The FOM for a single test fuel rod is

[a] A series of lead rod tests utilizing PBF rods in the test space will be used to reduce uncertainty in fuel temperature prior to high power operation of PBF.

defined as the ratio of the average radial power density at the axial power peak in the test fuel to the maximum power density in the PBF core fuel.

The power density attainable within a given fuel rod in a test cluster is dependent on several factors, including:

- (1) The size of the fuel rod cluster, the type of fuel rods used, the fuel rod enrichments, and the pitch of the fuel rods in the cluster
- (2) The PBF core power level
- (3) The temperature and pressure of the coolant in the loop coolant system
- (4) The composition and design of the test fuel container.

The construction of PBF centers about the principle of the flux trap. The test space is strategically located at the center of the core to take advantage of the natural flux peak. In addition, the fuel lattice in the driver core has a high nonmoderator-to-moderator ratio ($\cong 2.46$ to 1.0). The resulting core is undermoderated whereas the test fuel lattice within the IPT has near optimum moderation. Thus the fast flux per unit power in the core is fairly high and the thermal flux is correspondingly low. However, within the test fuel lattice the thermal flux is much higher and the fast flux is somewhat reduced. As a result the specific power density in the test fuel is materially higher than that in the driver core. The flux profile across the core is shown in Figure 69.

For cluster testing in PBF, enriched fuels are used. The maximum size test cluster that can be tested in PBF is thus limited by flux depression effects in the central region of the test cluster. For power-cooling-mismatch tests a 19 kW/ft linear power density can be achieved in the center of a cluster of 25 fuel rods at a reactor core power of 40 MW. Higher power densities, corresponding to overpower conditions, can be obtained in smaller test clusters.

4. GAP CONDUCTANCE TEST PROGRAM

R. W. Garner

As part of the Gap Conductance Test Program, a test was performed in the Power Burst Facility to determine PBF operation and measurement capabilities for evaluating the gap conductance of LWR design test rods by the thermal oscillator technique^[15]. This test is identified as the PBF Gap Conductance Test 1-1 and is the first test of a proposed three-test series for evaluating the thermal oscillator technique.

Application of the thermal oscillator technique requires operation of the PBF driver core in a sustained sinusoidal manner to provide a sinusoidal oscillation in the test rod power density. Data obtained from the test include the test rod power density and cladding

Fig. 69 Flux distributions across PBF core.

surface temperature variations. From this information the phase lag between test rod power and cladding surface temperature is determined and the gap conductance of the test rod is calculated from the rod power to cladding temperature transfer function.

The objectives of Test 1-1 were to determine the capability of operating the PBF in the required oscillatory manner, to determine the capability of accurately measuring small variations in the test rod power and cladding surface temperatures, and to determine the types and magnitudes of problems that will be encountered in data acquisition and reduction.

Preliminary evaluation of the results obtained from Test 1-1 provides encouraging evidence of the potential application of the thermal oscillator technique for obtaining gap conductance information. Two additional tests are planned for complete evaluation of the thermal oscillator technique. These tests also will provide gap conductance information based on the $fkdT$ technique^[16], which will be used as a basis for evaluation and verification of the thermal oscillator technique.

4.1 Test Description

The PBF Gap Conductance Test 1-1 was performed on a single PWR design test rod, centrally located in the PBF in-pile tube. Test rod physical characteristics are provided in Table IX.

TABLE IX

CHARACTERISTICS OF THE TEST ROD IN GAP CONDUCTANCE TEST 1-1

Cladding OD	-	0.422 inch
Cladding ID	-	0.374 inch
Wall thickness	-	0.024 inch
Cladding material	-	Zircaloy 2
Fuel material	-	UO ₂ (20% enriched)
Fuel density	-	94% TD (10.25 g/cm ³)
Pellet diameter	-	0.366 inch
Pellet length	-	0.61 inch
Rod overall length	-	39.9 inches
Fuel stack length	-	36 inches
Pellet shape	-	Dished, length-to-diameter ratio ~1.67
Plenum length	-	2.0 inches
Fill gas	-	Helium
Internal pressure	-	550 psig

The test was performed at axial peak power densities in the test rod of 5 and 10 kW/ft with coolant conditions of about 2,200 psi pressure, 540°F inlet temperature, and 1×10^6 and 2×10^6 lb_m/hr-ft² mass flow rate, respectively. A coolant flow shroud surrounded the test rod, and the mass flow rate was measured by turbine flowmeters at both the inlet and outlet to the flow shroud.

When the appropriate test conditions had been established, the PBF core was oscillated in a sinusoidal manner by preprogrammed motion of the transient rods. At a test rod power density of 5 kW/ft the power was oscillated at ±5, 10, 20, and 30% of nominal power at frequencies of 0.033, 0.05, and 0.1 Hz. At a test rod power density of 10 kW/ft the power was oscillated at ±5, 10, and 20% of nominal power at the same frequencies.

4.2 Experimental Measurements

Data obtained during the test included the coolant conditions (pressure, temperature, and mass flow rate), coolant temperature rise between the inlet and outlet, PBF core power, and test rod cladding surface temperatures.

The coolant temperature rise and mass flow rate measurements were used to determine the test rod power density at steady state operation. The core power and test rod cladding surface temperature measurements were used to determine the phase lag during oscillatory operation.

Cladding surface temperature measurements were made at two axial locations: 16 and 22 inches above the bottom of the test rod. At each axial location, temperature measurements were made at two azimuthal orientations; zero and 180 degrees at the 16-inch location, and 90 and 270 degrees at the 22-inch location.

4.3 Data Acquisition

Sensitivity analyses have shown that application of the thermal oscillator technique for obtaining gap conductance values depends to a large extent on the ability to measure the required phase angles to an accuracy of a few degrees. One of the objectives of Test 1-1 was to evaluate the relative accuracies of data acquisition and reduction procedures for a variety of data systems that exist for PBF.

Data from the test were recorded on three different acquisition systems: an on-line transfer function analyzer, an auxiliary tape system, and the PBF surveillance system. The on-line transfer function analyzer provided immediate information on the temperature oscillation and phase angles for a single cladding surface temperature measurement. The auxiliary system recorded data on an FM tape with one signal per tape channel. The surveillance system also recorded data on an FM tape system, but the signals are multiplexed with four data signals per tape channel.

The data provided in this report are those obtained with the on-line transfer function analyzer and the evaluation is considered to be preliminary.

Data obtained on all the systems are in the process of being reduced and analyzed to determine which method of acquisition and reduction should be used in future gap conductance tests.

4.4 Preliminary Evaluation of Results

Table X provides a listing of the on-line data obtained during the test at all test conditions. Pretest calculations of the measured variables are also indicated in Table X. The calculated values were obtained from a computer program that describes fuel rod behavior (FRAP-T).

Representative preliminary results are compared graphically with the calculated values in Figures 70 through 72 for the 5-kW/ft test rod power density, and in Figures 73 through 75 for the 10-kW/ft test rod power density conditions. In all the figures the calculated curves refer to the experiment test predictions.

In Figure 70 measured cladding surface temperature oscillations are shown for two axial locations: 16 inches from the bottom of the rod, which most closely represents the location for which the calculations were made, and 22 inches from the bottom of the rod. The notations, 16 in.-0° and 22 in.-270°, indicate the axial distance of the thermocouples from the bottom of the test rod and the azimuthal orientation at a given axial level from a reference zero-degree point. As indicated in Table X, the core was not oscillated at frequencies of 0.05 and 0.1 Hz at the power oscillation amplitude of ±5%.

Oscillation of the test rod power will cause the fuel pellets to expand and contract with a resultant oscillation in the gap width, unless the gap is closed. A gap oscillation would affect the cladding temperature oscillation magnitude and phase. Although the FRAP calculations include possible gap oscillations, the agreement between the measured and calculated temperature oscillations is noticeably improved at the smaller amplitude oscillations, for which gap size variations would be reduced. In general, however, the qualitative trends of the cladding temperature oscillations are well calculated.

Figures 71 and 72 compare the calculated and measured phase lags between the power oscillations and the cladding surface temperature oscillations at the 16-inch, 0-degree location for oscillation amplitudes of ±10 and 30%, respectively. The calculated trends are in good agreement with the measured trends, although significant differences do exist in the numerical values.

The cladding temperature oscillation comparisons shown in Figure 73 for a test rod power density of 10 kW/ft show generally better agreement than for 5 kW/ft. This result would be expected because at the higher power the gap between the fuel pellet and the cladding has been reduced at the 16-inch, 0-degree location so the power oscillations do not affect the gap size as much.

Figure 73 also shows the cladding temperature oscillations measured at a position of 16 inches and 180 degrees are significantly smaller than at the 16-inch, 0-degree location.

TABLE X

RESULTS OF PRELIMINARY EVALUATION OF DATA FROM

PBF GAP CONDUCTANCE TEST 1-1

Peak Linear Test Rod Power Density (kW/ft)	Amplitude of Oscillation	Frequency (Hz)	Calculated Amplitude of Cladding Temperature Oscillations (+°F)	Measured Amplitude of Cladding Temperature Oscillations (+°F)	Calculated Phase Lag (degrees)	Measured Phase Lag (degrees)
5	+5%	0.033	1.7	1.56	44.8	34.4
		0.05	1.3		51.6	
		0.1	0.9		61.2	
	+10%	0.033	3.4	3.05	43.7	38.0
		0.05	2.7	2.33	50.3	48.0
		0.1	1.9	1.54	58.9	60.0
	+20%	0.033	6.75	5.98	43.1	39.0
		0.05	5.5	4.76	49.7	48.1
		0.1	3.8	3.13	58.1	57.4
	+30%	0.033	10.4	8.6	42.7	40.9
		0.05	8.5	6.9	49.3	47.3
		0.1	6.0	4.5	57.8	58.0
10	+5%	0.033	2.0	2.0	38.2	31.6
		0.05	1.6	1.4	44.1	43.2
		0.1	1.1	0.9	52.3	50.0
	+10%	0.033	4.07	3.6	37.5	37.4
		0.05	3.3	2.9	43.1	40.3
		0.1	2.3	1.9	50.4	50.6
	+20%	0.033	8.1	7.3	36.9	36.6
		0.05	6.6	6.1	42.6	40.8
		0.1	4.6	4.25	49.8	50.9

Fig. 70 Comparison of calculated and measured cladding surface temperature oscillations at 5 kW/ft peak linear power density in the test rod.

This result would be expected if a larger gap existed at the 16-inch, 180-degree location; for example, if the pellet were located off-center. For comparison with measurements at 5 kW/ft, measured values are also shown at the 22-inch, 270-degree location, but for the $\pm 20\%$ oscillations only. At the 22-inch, 270-degree location, the same temperature oscillation trend as a function of frequency is observed at both power levels.

Figures 74 and 75 show the phase angle relationships between the power oscillations and the cladding surface temperature oscillations for ± 5 and 20% oscillations, respectively. The preliminary data indicate poorer qualitative agreement between calculations and measurements for the 10 kW/ft power density than for the 5 kW/ft condition.

Fig. 71 Comparison of calculated and measured cladding surface temperature phase angles at 5 kW/ft peak linear power density in the test rod, $\pm 10\%$ power oscillation.

Application of the data obtained from Test 1-1 for determination of gap conductance values by the thermal oscillator method is based on a two-step process. First the cladding surface temperature transfer function is calculated as a function of gap conductance and test fuel (UO_2) thermal conductivity using the HEAT-1^[17] computer program. The transfer function is determined for each nominal power density and for each oscillation period. A typical transfer function diagram is shown in Figure 76 for the Test 1-1 test rod at a power density of 10 kW/ft and an oscillation period of 20 seconds per cycle. The transfer function was calculated for five values of the gap conductance between 400 and 4,000 Btu/hr-ft²-°F and for three values of the UO_2 thermal conductivity.

Fig. 72 Comparison of calculated and measured cladding surface temperature oscillations at 5 kW/ft peak linear power density in the test rod, $\pm 30\%$ power oscillation.

From the transfer function information, the phase angle can be determined for each value of the gap conductance and UO_2 thermal conductivity, and plots of h_{gap} versus phase angle can be developed. For the conditions of Test 1-1, representative plots are shown in Figures 77 and 78. The second step then merely involves using relationships such as shown in Figure 77, and the measured phase angle to determine the appropriate gap conductance value for the conditions of the test.

For Figures 77 and 78, only the calculations using a nominal value for the UO_2 thermal conductivity are shown. The three curves show the variation in the relationship as a function of oscillation period ($t = 10, 20, \text{ and } 30 \text{ sec/cycle}$).

Fig. 73 Comparison of calculated and measured cladding surface temperature oscillations at 10 kW/ft peak linear power density in the test rod.

Gap conductance values obtained from the on-line measured cladding surface temperature phase lags at 16-inch, 0-degree and the relationships shown in Figures 77 and 78 are provided in Table XI. Table XI shows that considerable variations occur in the individual gap conductance values for each measured phase angle, but, if the variances are assumed to be statistically distributed, the mean values, when grouped according to oscillation amplitudes, show good agreement. On the basis of the values shown in Table XI, the mean values for the gap conductances at 5 and 10 kW/ft are slightly higher than predicted by the FRAP-T computer program (570 predicted at 5 kW/ft, as compared with 798 measured and 770 predicted at 5 kW/ft, as compared with 1,312 measured).

Fig. 74 Comparison of calculated and measured cladding surface temperature phase angles at 10 kW/ft peak linear power density in the test rod, $\pm 5\%$ power oscillation.

Emphasis is placed on the fact that the data presented in Table XI are based on the on-line phase lag measurements only and have not been fully analyzed and corrected for actual system conditions. For example, preliminary comparison between cladding surface temperature oscillations and coolant mass flow rate and temperature conditions indicate that variations in the coolant conditions may influence the measured phase angles. The standard deviation values shown in Table XI were determined from the individual phase measurements from the mean, and do not take into consideration uncertainties in the calculations, such as uncertainties in the thermal conductivity, or in the measured parameters, such as the test rod power density. Uncertainty analyses and analysis of the data obtained and reduced on the other data acquisition systems are presently being performed.

Fig. 75 Comparison of calculated and measured cladding surface temperature phase angles at 10 kW/ft peak linear power density in the test rod, $\pm 20\%$ power oscillation.

Preliminary analysis of the test data indicate the following:

- (1) The test demonstrated that the PBF could be oscillated at the desired frequencies in an exceptionally stable manner and that the core power and test fuel cladding surface temperatures responded within one or two cycles to reach a stable oscillation.
- (2) The measured cladding temperature oscillations appear to be sufficient, indicating that the power oscillation magnitudes and frequencies used are appropriate.

Fig. 76 Calculated cladding surface temperature transfer function as a function of gap conductance and UO_2 thermal conductivity.

- (3) The gap conductance values obtained by the thermal oscillator technique are in reasonable agreement with FRAP calculated values. This preliminary information provides encouraging evidence of the potential usefulness of the thermal oscillator technique.
- (4) Additional analyses are necessary to provide information on the noise levels, type of analysis, and degree of effort required to extract more exact information from the data, and the accuracies required to obtain factual gap conductance information.

Fig. 77 Representative plot of gap conductance as a function of cladding surface temperature phase angle with period of oscillation as a parameter -- 5 kW/ft test rod peak linear power density.

Fig. 78 Representative plot of gap conductance as a function of cladding surface temperature phase angle with period of oscillation as a parameter -- 10 kW/ft test rod peak linear power density.

TABLE XI

PRELIMINARY EVALUATION OF TEST ROD GAP CONDUCTANCE VALUES BY THE
THERMAL OSCILLATOR TECHNIQUE FOR TEST 1-1

		5 (kW/ft)			10 (kW/ft)		
Oscillation Period (sec./cycle)	Oscillation Amplitude (% nominal)	Measured Phase Lag (degrees)	h_{gap} (Btu/hr-ft ² -°F)	h_{gap} mean Standard Deviation	Measured Phase Lag (degrees)	h_{gap} (Btu/hr-ft ² -°F)	h_{gap} mean Standard Deviation
10	5	---	---	---	50.0	1,075	$\bar{h}_{\text{gap}} = 1,162$ $\sigma = 124$
20	5	---	---	---	43.2	1,250	
30	5	---	---	---	31.6	---	
10	10	60.0	500	$\bar{h}_{\text{gap}} = 803$ $\sigma = 291$	50.6	930	$\bar{h}_{\text{gap}} = 1,377$ $\sigma = 460$
20	10	48.0	830		40.3	1,850	
30	10	38.0	1,080		37.4	1,350	
10	20	57.4	660	$\bar{h}_{\text{gap}} = 815$ $\sigma = 158$	50.9	900	$\bar{h}_{\text{gap}} = 1,347$ $\sigma = 408$
20	20	48.1	810		40.8	1,700	
30	20	39.0	975		36.6	1,440	
10	30	58.0	630	$\bar{h}_{\text{gap}} = 777$ $\sigma = 130$	---	---	---
20	30	47.3	880		---	---	---
30	30	40.9	820		---	---	---
Evaluated Steady State Gap Conductance			$\bar{h}_{\text{gap}} = 798; \sigma = 179$		$\bar{h}_{\text{gap}} = 1,312; \sigma = 345$		

IV. REACTOR BEHAVIOR PROGRAM

J. H. Ramsthaler, Program Manager

1. INTRODUCTION

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

In the area of thermal-hydraulic code development, the Reactor Behavior Program is developing a system of analytical codes for use in describing reactor system response to LOCA and ECC situations. This activity includes the development of new analysis techniques having improved and unique capability over existing codes. These new techniques will be incorporated into the existing codes where practicable or developed into a new code.

This thermal-hydraulic code development effort is broken down into the following areas:

- (1) An executive code structure using the DATATRAN modular programming system, which permits coupling of separate codes.
- (2) A three-dimensional, multirod, multichannel core code (SCORE) capable of predicting the transient thermal-hydraulic response of a nuclear reactor core during all phases of operation and transient incidents, including a LOCA.
- (3) A computer code (SPLEN) capable of predicting the turbulent two-phase flow of the coolant in large open areas of the reactor system.
- (4) An advanced loop code (SLOOP) that includes improved models and subcodes for prediction of the thermal-hydraulic behavior of the coolant throughout the reactor primary system. The principal difference between this code and existing codes is the inclusion of an unequal phase velocity and unequal phase temperature (UVUT) capability so that phenomena which occur during ECC injection, mixing, and core reflood are properly modeled.

In the area of code optimization, work is being directed toward improvement of current RELAP4 analytical techniques and models for describing reactor system response to a LOCA and ECC. The intent is to improve the code's capability of simulating phenomena associated with a postulated loss-of-coolant accident and to allow relaxation of simplifying assumptions used in previous versions of the code.

In the development of fuel codes, a premise is that the fuel rod constitutes the first barrier to release of fission products as a result of any postulated accident. Therefore, the main purpose of the fuel codes in accident analysis is to predict whether this barrier would be breached, and if so, to estimate the time, the extent, and the location of the breach. Analytical description of fuel behavior during a hypothetical accident requires knowledge of the initial conditions for all the fuel variables that would undergo change. These include temperature distribution (both radial and axial) in the fuel pellets and in the cladding; geometry of the cladding, gap, and fuel pellets; volume of the fuel plenum; restructuring and the extent of cracking of the fuel pellets; gas pressure and composition in the gap and the fuel plenum; and the mechanical strains in the cladding. All of these are affected by the degree of fuel burnup and the initial conditions of the fuel rods at the beginning of life. The geometry of the gap and the composition of the gas in the gap are particularly important for the calculation of heat transfer and the estimate of stored energy. During the time between initial loading and the time of a postulated accident, the fuel experiences slow changes in state which in a steady state code are treated as a sequence of quasi-steady events. The transient fuel behavior code, on the other hand, is used to describe transients that are fast enough to merit consideration of the rate of storage or depletion of energy, whether thermal, chemical, or mechanical.

Work is presently in progress at ANC on developing the Fuel Rod Analysis Program - Steady State (FRAP-S) code using GAPCON-THERMAL-1^[18] and the FUEL^[19] codes as the basis. The work on the FRAP-S code includes: (a) extension of thermal models to cover a wider range of fuel burnup and fuel rod operating conditions, (b) incorporation of the existing data on fuel-cladding mechanical interaction, cladding creep, and zircaloy strength and ductility, and (c) improvements in the calculation of internal pressure, fission gas release, fuel restructuring, and gap closure.

The present hot channel codes solve the transient heat conduction equation in the radial direction for both the fuel pellets and the cladding for a given number of axial space increments. Heat transfer between the fuel pellet and the cladding is governed by the gap conductance, which is correlated to gap thickness via the cladding temperature and the initial gas pressure. Temperature-dependent thermal properties, material phase changes, metal-water reaction rates, and axial and radial variations of power densities are considered in the solution of the conduction equation. Rapid effects that may occur during the postcritical-heat-flux (CHF) transient, such as fission product release and additional fuel cracking, are not explicitly considered.

The Fuel Rod Analysis Program - Transient (FRAP-T) Code, which is presently in the development stage by ANC, will unify the thermal, chemical, and mechanical aspects of transient fuel behavior. In its present form, the FRAP-T Code calculates: (a) fuel rod (pellet and cladding) radial temperature distribution for up to 20 axial positions, (b) the length of fuel stack, (c) the length of cladding, (d) cladding hoop stress and cladding hoop strain at up to 20 axial positions, (e) radial deformation of the fuel rod up to 20 axial positions, and (f) the pressure of gas in the fuel rod.

Aspects of fuel rod behavior that are presently modeled include the following:

- (1) Coupling of temperature, internal gas pressure, and mechanical deformation
- (2) Radiation heat transfer across the gap
- (3) Phase change of fuel and cladding, energy required for heat of fusion, volume increasing at melting
- (4) Effect of axial variation of gap temperature and gap thickness on pressure
- (5) All the modes of heat transfer from the cladding to coolant previously considered in the HEAT-1 Code.

The scope of each of the thermal-hydraulic code development, fuel rod model development, and computer code verification activities is discussed in the following paragraphs.

1.1 Core Code (SCORE)

The objective of this task is to develop a three-dimensional multirod, multichannel core code capable of predicting the transient thermal-hydraulic response of a nuclear reactor core during all phases of operation and transient incidents, including a LOCA. The present version of the code is based on the homogeneous two-phase flow equations with friction models representing the rods. The final version of the code will be based on the unequal phase velocity and unequal phase temperature equations. The core code will also be capable of independent operation with specific boundary conditions for design studies and comparison with experimental results.

A MICRO version using laminar velocity or turbulent eddy viscosity correlations, and a MACRO version using friction factors, will be developed so that both local and core-wide flow phenomena can be analyzed. The code will be capable of calculating subchannel crossflow and mixing caused by partial channel blockage as well as core-wide phenomena such as free convection and hot channel flow due to radial core power variation.

A three-dimensional front model across which a discontinuous change in fluid properties occurs will be included to model the core flow during reflooding.

Effort has been focused on developing an equal velocity, equal temperature (EVET) core code. The basic code solves the three-dimensional, compressible, transient, volume-averaged Navier-Stokes equations for a homogeneous, two-phase fluid. In order to produce a more general code, a wide variety of constitutive models will be programmed into the code as subroutines.

A differential slip velocity model will be added to the code in order to improve upon the homogeneous flow assumption. The model will be applicable to multidimensional flows. A method to track a fluid interface will also be developed in order to improve the model for the refill and reflood portions of the accident. A differential temperature model will be added in order to improve upon the current equal temperature assumption. The geometry capabilities of the code will be extended to include irregular boundaries.

A capability will be developed to analyze core reflood multidimensionally, including liquid entrainment and fallback. Provision for considering grids and inlet orificing will be included in the code to account for the large effects these devices have upon core flow. Heat transfer and friction factor correlations and flow regime maps will be modified to account for three-dimensional effects.

1.2 Plenum Code (SPLEN)

The objective of this task is to develop a computer code capable of predicting the turbulent two-phase flow of the coolant in large open areas of the reactor system where multidimensional effects are important. In particular, the code will be applied to flows in the downcomer, lower plenum, and upper plenum. The code will also be used for calculations of the turbulent flows in expansions, tees, single subchannels, and other geometries. In general, the plenum code is a derivative of the core code development, but includes addition of a two-parameter turbulence model for simulation of microturbulence effects.

Currently, a two-parameter turbulence model is being incorporated into the basic three-dimensional EVET code in an explicit numerical manner. Wall boundary conditions are being modeled and coded to avoid the use of a large number of computational cells in the wall region. Data comparisons with channel flow and an abrupt expansion are being made.

1.3 Loop Code (SLOOP)

The objective of this task is the development of an advanced loop code that includes improved models and subcodes for prediction of the thermal-hydraulic behavior of the coolant throughout the reactor primary system. The principal difference between this code

and existing codes is the inclusion of an unequal phase velocity and unequal phase temperature capability so that phenomena which occur in ECC injection, mixing, and core reflood are properly modeled. The basic approach involves development of a mechanistic model having provisions for nonequilibrium effects.

This includes application of the conservation and constitutive relations to each phase separately, with appropriate cross-linking terms included in the differential relations. The advantage of this approach is that greater physical insight into the fluid mechanisms is provided, and once the empirical factors for interphase transfer of mass, momentum, and energy are established, the models can be used with increased confidence to predict flow in regimes for which data are lacking.

1.4 Executive Code

An executive code structure is being developed using the DATATRAN 2 modular programming system which permits coupling of separate codes. Manual data transmission between codes is eliminated along with the associated lost time and potential for error. Linking modules will be written to couple the various program modules.

1.5 Code Optimization

The code optimization activity is concerned with the improvement of existing analytical techniques and codes for describing reactor system response to a LOCA and ECC. The primary effort is directed toward adding models to the RELAP4 computer code that will improve the code's capability of simulating phenomena associated with a postulated LOCA. Addition of these models to RELAP4 will allow relaxation of simplifying assumptions used in earlier versions of the code.

Two major versions of the RELAP4 code are being developed: RELAP4-BE is the code version providing the best-estimate prediction of the consequences of a LOCA and includes best-estimate models to make it applicable to prediction and comparisons of experiments and verification analysis. RELAP4-EM is the code version providing conservative LOCA analytical predictions. Only the RELAP4-BE work is included as a part of this report.

1.6 Fuel Rod Model Development

The fuel model codes are to be developed to the point that accurate predictions can be made for the fuel pin material property and dimensional changes under long-term operation, the cladding temperature and structural deformation under postulated accident conditions, and occurrence of cladding rupture.

FRAP-S and FRAP-T will be available to all users through the Argonne Code Center. At present, the first version of FRAP-T, the FRAP-T1 Code, has been transmitted to Argonne. FRAP-S is scheduled for transmittal to Argonne early in Fiscal Year 1976.

FRAP-T is a FORTRAN IV computer code developed to describe the transient behavior of nuclear fuel rods during accidents such as a loss of coolant and a power-coolant mismatch. FRAP-T includes the coupled effect of thermal, mechanical, internal gas, and material properties in the analysis of fuel rod transient behavior. The code developed to date (FRAP-T MOD001) is the first of a series of fuel rod codes planned for release at one-year intervals, with each succeeding version incorporating the advancements made in fuel rod response analysis models during that year. The code presently is programmed and running on the IBM-360/75 computer and the CDC 7600. A steady state fuel rod analysis code, FRAP-S, is being developed to generate the steady state operating parameters required as initial conditions to FRAP-T. Both codes are being developed with common subcodes and compatible input-output features.

FRAP-T is a modular code with each type of computation and analytical model, such as internal gas pressure, embodied in a separate module or subroutine. This configuration is designed to allow maximum flexibility in developing and modifying the code with minimum impact on the unmodified portion of the code.

A major component of FRAP-T is the subcode MATPRO. This subcode is comprised of modular function subprograms and subroutines which define the material properties required by the computational subcodes of FRAP-T. Each function subprogram or subroutine defines one material property.

The developmental process of FRAP-T includes a verification effort designed to test the analytical capability of the code. Experimental data on fuel rod response parameters such as centerline temperature and clad deformation are compared with FRAP-T calculated values.

A configuration control procedure is used to maintain a consistent and complete definition of FRAP-T. This procedure consists of a maintained file for all changes to the code and code identification by version number on all output.

FRAP-T development effort during the first quarter of 1975 consisted of adding newly developed models and debugging the preliminary version of FRAP-T MOD002 (FRAP-T2). This preliminary version of FRAP-T2 contains the following improvements over FRAP-T MOD001 (FRAP-T1): (a) an improved gap conductance model which considers cracked pellet effects, (b) a localized cladding swelling model, (c) an improved fuel-cladding interaction subcode, and (d) a transient plenum temperature model.

FRAP-S is a FORTRAN IV computer code developed to describe the steady state and long-term burnup response of oxide fuel rods in light water reactors (LWR). In addition, the code is designed to generate parametric data required as initial conditions for transient accident analyses using FRAP-T.

The code under development (FRAP-S MOD001) is the first in a series of steady state fuel rod codes planned for release at one-year intervals, with each succeeding version incorporating the advancements made in fuel rod response analysis models during the preceding year.

FRAP-S has been developed primarily from the FUEL^[18] code with major additions to the basic code from current nuclear codes such as GAPCON-THERMAL-1^[19] and new models developed as a part of this effort.

FRAP-S also is a modular code with each type of computation and analytical model, such as internal gas pressure, contained in a separate module or subroutine.

Similar to FRAP-T, a major portion of FRAP-S is the subcode MATPRO. The FRAP-S developmental process, like FRAP-T, includes a verification effort and a configuration control procedure.

1.7 Computer Code Verification

The Reactor Behavior Program's code verification activity has two main purposes: (a) to reveal modeling deficiencies and (b) to arrive at dependable uncertainty bands for the calculated results obtained with a given code. Subsidiary verification activities include: (a) obtaining the measurement accuracy requirements for purposes of verification; (b) reviewing and verifying estimates for the uncertainty bands of recorded data, and (c) performing code sensitivity analyses to ascertain which physical processes or which system components strongly influence the final results to a degree that warrants a high priority modeling (analysis) and/or experimental effort.

Analysis verification is performed on three levels. The first level involves testing the computer code, or subcodes, against closed-form solutions pertaining to some highly simplified or idealized geometries and conditions or corroborating the basic modeling schemes using available test data.

In the second level, the results calculated using the code are compared with data obtained from the Separate Effects Tests in which only one component or phenomenon is viewed at a time, or a small portion of the total system is studied. The third level includes comparison with integral system test data (such as Semiscale and LOFT), sensitivity analyses pertaining to the code uncertainties in input data used in calculations, and evaluation of the uncertainties in the final calculated results obtained in the use of the codes. Work has been initiated in which needs for experimental data and analysis development for LOCA evaluation will be defined.

Present emphasis is directed to the PWR LOCA design basis accident. In addition, other postulated accidents require systematic evaluation of analysis and experimental needs, e.g., small breaks and BWR LOCAs, reactivity initiated accidents, and anticipated transients without scram (ATWS). The principal code being verified is RELAP4-BE.

Effort on fuel behavior codes includes verification of the FRAP-S and FRAP-T codes, development of experimental requirements for fuel behavior tests, management of the Halden and Postirradiation Examination (PIE) Programs, and preparation of material properties data for the fuel codes. As additional experimental data are made available from Nuclear Regulatory Commission-sponsored and other fuel testing programs, data comparisons with FRAP are performed. FRAP is modified as appropriate to permit improved predictive capability, and the revised code is again "verified" by data comparisons. This iterative process results in code refinement and an increased confidence in code capability.

2. TWO-DIMENSIONAL HEAT FLOW STUDY

R. L. Benedetti

An investigation into the need for a two-dimensional heat conduction model for the FRAP codes was completed during the first quarter. The following paragraphs describe the work and conclusions obtained from the study.

The temperature distribution of a fuel rod generally is assumed to be two-dimensional (radial and axial, R-Z) for both steady state and transient conditions. Thus, in fuel rod analysis codes, the R-Z temperature distribution generally is calculated using a one-dimensional radial conduction solution stacked in the axial direction, i.e., no conduction in the axial direction.

The stacked one-dimensional approach is taken because the solution technique is simple and generally takes less time to solve than a two-dimensional solution. In addition, less active core storage is required when calculating temperature distributions. The total temperature data stored is the same as would be required for a two-dimensional solution. By using the stacked one-dimensional method only, the temperature data of that particular level of the fuel rod for which temperatures are being calculated is needed in active core. However, the errors incurred by neglecting axial conduction needed to be established. In addition, axisymmetric (R-Z) and axially stacked radial temperature analysis has been applied to analysis of fuel rod conditions which were known to be three-dimensional. For example, azimuthal temperature variations brought about by radiation heat exchange and azimuthal power variations have been neglected. The magnitude of these variations also needed to be established. If azimuthal temperature variations are significant the possibility of their being modeled by one-dimensional radial calculations stacked in the azimuthal direction warrants consideration.

The dimensionality requirements for fuel rod thermal analysis were investigated in this study. For the investigation, two-dimensional R-Z and R- Θ studies were made of actual and hypothetical fuel rod conditions. All possible fuel rod conditions could not be studied. However, it is believed that sufficient sample cases were studied to provide the following type of information concerning fuel rod thermal modeling:

- (1) The error incurred by neglecting axial conduction when modeling PCM and LOCA conditions
- (2) The effect of azimuthal variations in power density on steady state and transient temperature distribution
- (3) The effect of neglecting conduction in the azimuthal direction
- (4) The dimensionality requirements for radiation heat transfer for LOCA and flow blockage accidents in BWRs.

The bulk of this investigation was concerned with the dimensionality requirements for modeling radiation heat transfer in BWR canisters, item (4). A code capable of modeling the response of an entire BWR canister two-dimensionally was not available. The TOODEE^[20] code was therefore modified to model, two-dimensionally, the thermal response of a single BWR rod under radiation heat transfer conditions. Rod response for 100% flow blockage and for jet pump LOCA conditions was modeled.

Only those conditions which exist up to the time rod failure occurs were addressed in this study. No attempt was made to determine the dimensionality requirements needed under possible ballooned or melted rod conditions brought about by an accident.

2.1 Summary

The TOODEE code was used to establish the spatial dimensional approximations that are acceptable for thermal conduction models used in fuel rod analysis codes. Seven case studies were made. Steady state and transient temperature responses of fuel rods were studied under axial and azimuthal power variations and azimuthal surface heat transfer variation due to radiation.

Axial conduction was studied by modeling PCM conditions for a PCM-20 test series fuel rod in the Power Burst Facility. When considering typical axial nodal spacing, the neglect of axial conduction was found to have a negligible effect on both steady state and transient temperature response.

Azimuthal temperature variation due to radiation in BWR rods was studied under 100% flow blockage and LOCA conditions. The TOODEE code was modified to calculate radiant energy exchange at the surface of one rod given the temperatures of adjacent rods and the canister wall in a 7x7 BWR cluster. The thermal response of a single corner rod was modeled by specifying surrounding rod and canister temperatures as calculated by MOXY^[21] for the two cases studied. For a LOCA and for 100% flow blockage

conditions, the corner rod temperature was calculated to have maximum^[a] azimuthal cladding temperature variations of 60 and 120°F, respectively. A temperature variation of this magnitude on the outer rods of a BWR cluster will affect significantly the energy balance of the total rod bundle. In addition, it was found that emissivity played an important factor in producing azimuthal temperature variations. Low values of emissivity produced only small temperature variations. High values of emissivity, as expected under reactor conditions, cause large temperature variations.

The effect of neglecting conduction in the azimuthal direction was also studied. The 100% flow blockage case was rerun with conductivity set to zero in the theta direction. Cladding temperatures produced in this manner differed by 30°F from those calculated with azimuthal conduction.

The effect of azimuthal variation in power density was studied for conditions found in PBF PCM-20 test clusters and for the case of a hypothetical 10% azimuthal power variation in power reactor rods. The power variation in the side rod of the PCM-20 nine-rod PWR cluster was used (40% azimuthal power variation) for the PBF case. This power variation was found to cause large fuel and clad temperature variations, particularly for PCM transient conditions (300°F in the clad; 500°F in the fuel).

The magnitude of azimuthal variation in power density expected in power reactors was not available. Therefore, a scoping study was done to determine the effect of a hypothetical 10% power variation on fuel rod performance. Transient conditions for LOCA, flow blockage, and PCM accidents were simulated. For the flow blockage and LOCA cases, the values of coolant heat transfer, coolant temperature, fuel rod dimensions, and transient power were those previously used for the radiation studies. For the PCM case, BWR/4 rod specifications were used and the rod power was maintained at the initial steady state level of 17.44 kW/ft. To simulate the PCM, the rod surface heat transfer coefficient was reduced to 400 Btu/hr-ft²-°F. The 10% power variation caused cladding and fuel temperature variation of approximately 45 and 100°F for the PCM and flow blockage cases, respectively. For the LOCA case, a maximum cladding temperature variation of only 15°F was calculated. In all three cases, only small variations in fuel and cladding steady state temperatures were calculated, 15°F in the fuel and 1 to 2°F in the cladding.

2.2 Conclusions

The following conclusions regarding fuel rod thermal modeling requirements were drawn from this study:

Accurate two-dimensional R-Z temperature results can be produced by stacked one-dimensional radial calculations for:

- (1) LOCA, PCM, and flow blockage accidents with axisymmetric power and surface heat transfer coefficients
- (2) LOCA analysis with 10% or less azimuthal power variation.

[a] Maximum temperature variations are those calculated when the rod average temperature is maximum or reaches 2,200°F.

Two-dimensional $R-\Theta$ thermal modeling is required to model fuel rod temperature response when:

- (1) Rod power azimuthal variations are 10% or greater
- (2) Modeling BWR fuel rod bundle accident response.

Cladding emissivity can strongly affect the azimuthal temperature distribution found in BWR canisters. Low values of emissivity result in small azimuthal temperature variations. High values of emissivity cause large temperature variations.

3. FRACAS - FUEL-CLADDING MECHANICAL INTERACTION SUBCODE

M. P. Bohn

A new fuel-cladding interaction model was made operational in subcode form during the first quarter of 1975. The subcode, FRACAS, has been linked to FRAP-S1 and FRAP-T2 and is being checked out for computational accuracy and numerical stability. Documentation on FRACAS is in progress. The following paragraphs present a brief review of the FRACAS subcode and how the subcode fits into the overall fuel rod model development effort.

3.1 Purpose

The analysis of stress and strain in the cladding of nuclear reactor fuel rods is of paramount importance for predicting failure under different normal and off-normal operating conditions. The mechanical behavior of the fuel rod cladding and the fuel pellets contained within the cladding is coupled inseparably with the thermal-hydraulic response of the fuel rods and the surrounding coolant. Without considering simultaneously all the mutual interactions which occur, it is not possible to realistically analyze the fuel rod behavior.

The subcode FRACAS contains the set of subroutines that predict the stress and deformation in the fuel rod cladding. These subroutines perform a multi-axial elasto-plastic analysis of the cladding deformation for an arbitrary (externally specified) uniaxial, stress-strain law. The equilibrium and kinematic strain-displacement equations are taken from the membrane theory of thin shells.

3.2 General Considerations

In analyzing the behavior of fuel rods, three physical situations are encountered. First, the fuel pellets and cladding are not in contact initially, and a fission gas is present in the gap between the two. Thus the problem to solve is one of a cylindrical shell (the cladding) with specified internal and external pressures and a specified cladding temperature distribution. This situation is called the "open gap" regime. Elementary calculations,

however, show that the temperature drop across the cladding is not large. During steady state operation, for example, a drop of 60 to 100°F is typical. During a LOCA, the drop is even less. Hence it is reasonable to compute the thermal strains in the cladding on the basis of a single average cladding temperature.

Second is the situation where the fuel pellets (which are considerably hotter than the cladding) have expanded so as to be in contact with the cladding. Further heating of the fuel results in "driving" the cladding outward. This situation is called the "closed gap" regime. Elementary calculations (which are supported by the available experimental evidence) indicate that the deformation of the fuel pellets is due almost entirely to thermal expansion and is affected very little by the stresses in the fuel. The temperature drop from the center of the fuel pellet to the outside of the pellet is quite large, as high as 2,000°F in steady state operation. Hence, in computing the thermal expansion of the fuel, it is necessary to consider the actual temperature distribution.

Finally, a third situation occurs in which a number of pellets not in contact with the cladding are trapped between the lower end of the fuel rod and a fuel pellet that is in firm contact with the cladding. The axial expansion of the stack of trapped fuel pellets is imparted to the cladding. Here the problem to solve is one of a thin cylindrical shell with not only prescribed internal and external pressures but also a prescribed total change in length. This situation is called the "trapped stack" regime.

To realistically predict the strains and stresses in the cladding, it is necessary that a cladding mechanical behavior subcode consider all three regimes described above. Furthermore, the cladding should be analyzed as an anisotropic media, for it is known^[22] that, because of texture differences, the zircaloy cladding exhibits directionally dependent material properties. A review of the existing mechanical subcodes, as described in the next section, revealed that no single existing subcode was entirely adequate for direct incorporation into either FRAP-S or FRAP-T.

3.3 Review of Existing Mechanical Models

Prior to the inception of the work to develop a mechanical fuel-cladding interaction model for use in FRAP-S and FRAP-T, a complete review of existing fuel rod codes was made. The mechanical models reviewed were in CYGRO-3C^[23,24,25], LIFE-II^[26,27], GAPCON-THERMAL^[28], FMODEL^[29], BEHAVE-II^[30], SAS-2A (DEFORM)^[31,32], PELEN^[33], COMETHE III^[34-37], FREADM^[38], PECT-2^[39,40], and SATURN^[41,42]. This list was essentially complete at the time that the review was performed (May 1974). One of the purposes of the review was to determine the state of the art of current fuel rod mechanical models and to determine if any mechanical behavior subcodes existed that could be incorporated directly into FRAP-S and FRAP-T.

To be considered for incorporation into the FRAP codes, the subcodes should meet the following requirements:

- (1) The subcode should be modular and should accept arbitrary (externally specified) material properties. Thus, the mechanical analysis should be written to accept an arbitrary elastic-plastic-creep constitutive equation, rather than be restricted, as by a bilinear stress-strain relation or power law relation.
- (2) The subcode should be capable of predicting all the phenomena which are currently known to be important in realistically predicting the mechanical behavior of commercial oxide fuel pressurized water reactors. These include, as a minimum, the following:
 - a. General elastic-plastic behavior in the open gap, closed gap, and trapped stack regimes of fuel-cladding mechanical interaction
 - b. Axial fuel rod length changes
 - c. Axial ratchetting behavior
 - d. Time-dependent primary (transient) and secondary creep
 - e. Radiation-induced creep and swelling
 - f. Fuel melt at center of pellet.
- (3) The subcode should be computationally efficient.

The last requirement presented the most stringent restriction on the possible candidacy of the available subcodes. The fundamental design philosophy underlying the development of the FRAP-S and FRAP-T codes was that all significant coupling would be taken into account where possible. Thus, there is feedback from the deformation calculations for the fuel and cladding to the thermal calculations, i.e., the determination of the temperature distribution in the fuel. This then enters into the determination of the internal gas pressure, the fuel expansion, and the fuel creep rate. All of these, in turn, affect the deformation calculations for the fuel and cladding. To take all these coupled effects into account, a considerable amount of iteration must be performed, and consequently there must be a premium placed on the calculational efficiency of each individual model or subcode.

Only limited information was available on the COMETHE III, SATURN, PELEN, and PECT-2 codes, as these were still in the process of development. The PELEN code is evidently a two-dimensional version of CYGRO-3C based on the finite element approach in both its mechanical model and its heat conduction model. When released, its two-dimensional capability could prove useful for studies on the effects of pellet shape and size on cladding deformation. Such information is obtained, however, only at the expense of

computation time. It remains to be seen whether such a detailed mechanical model can be incorporated into a fully coupled (thermal-hydraulic-mechanical) analysis of a fuel rod operating history without resulting in prohibitively long computer times. Of all the codes reviewed, only PELEN has the two-dimensional capability. The remaining codes perform a one-dimensional analysis (in the radial direction) by making the assumption that the state of stress and deformation corresponds to that of axisymmetric generalized plane strain. Several of the codes utilize a number of axisymmetric plane strain elements "stacked" in the axial direction to approximate axial variations in deformation.

The mechanical models contained in CYGRO-3C, LIFE-II, SAS-2A, and BEHAVE-II are the most comprehensive and are the codes currently in general use by the major power plant vendors. CYGRO-3C is the most complete, as it includes a fuel cracking model, anisotropic plastic strain calculations, fuel swelling due to irradiation, and also a cladding axial strain calculation including friction between fuel and cladding. However, the "trapped stack" regime is not considered, and the stress-strain equations and creep rate equations are restricted to being in the power law form. Finally, users have experienced instabilities when running the code for typical PWR power histories.

The LIFE-II code also includes friction effects between the fuel and the cladding, as well as creep effects. However, the code is limited to four ring elements to model both the fuel and the cladding. Considering the steep temperature gradients across the fuel pellets, the restriction to three nodes in the fuel appears unrealistic. Finally, although LIFE-II considers both elastic and thermal deformation as well as time-dependent creep, no time-independent plastic strains are considered. Since experiments have shown that the interplay between plastic strains and creep strains has a significant effect in determining allowable rates of ramp power increases, the exclusion of plastic strains seems to be a definite limitation. It is to be noted, however, that the code is in a continuing state of evolution, and hence these limitations may be of an interim nature. (LIFE-III was not available for review.)

The BEHAVE-II code has both a cracking model for the fuel as well as a model relating the Poisson's ratio of the fuel to the porosity of the fuel. However, the code assumes from the outset that there is no gap between the fuel and the clad. BEHAVE-II is the only fuel rod behavior analysis code which makes this assumption, and Halden experiments^[43] have shown that the peak strain in the clad is quite sensitive to the initial gap between fuel and cladding. The rationale behind this assumption is evidently that the as-fabricated gap is essentially indeterminate because of fabrication tolerances as well as cracking of the fuel as the reactor is initially brought up to power. Because of this cracking, it is felt that there will be no gap during the major portion of the operating history of the fuel rod^[30]. Regardless of the validity of these assumptions with respect to a fuel rod in a commercial pressurized water reactor, it would seem that the no-gap assumption would introduce a considerable uncertainty into a comparison of the code's predictions with experiments in which the tolerances and initial gaps are closely measured for the purposes of code verification.

The DEFORM-II mechanical subcode in the SAS-2A fuel rod analysis code comes the closest to being suited for use in the FRAP codes. It uses a numerical quadrature solution to

the integral equation governing the axisymmetric plane strain cylinder equations, which is a very efficient computational technique. It also recognizes the "trapped stack" situation. In addition, it permits arbitrary stress-strain and creep strain relations. However, it is limited to isotropic materials, and it assumes that Young's Modulus E and Poisson's ratio ν are constant throughout the fuel and depend only as a radial average on the fuel temperature. While this simplifies the numerical solution, these restrictions are quite severe where large temperature gradients exist in the fuel; indeed, as the melt temperature is approached near the centerline of the fuel pellet, the stiffness (E) must approach zero. Finally, it has no provisions for cracking of the fuel.

Based on the limitations described above, it was evident that no one existing subcode met the necessary requirements for incorporation into the FRAP codes. Hence, an effort was begun to develop the simplest mechanical model that could meet the design requirements. In particular, it was decided to use a shell theory for the cladding rather than a multinode, thick-walled cylinder theory. Further, since experiments^[44] have indicated that when the gap is closed there is little relative motion between the fuel and cladding, it was assumed that no slip occurs between the fuel and cladding while they are in contact. For the initial model, it was decided to neglect the deformation of the fuel caused by stresses imposed on the fuel by the cladding. The effect of this is to predict cladding strains that are slightly too high, and thus the approximation is conservative.

Finally, where possible, it was decided to separate the creep strain calculations from the plasticity (time-dependent) calculations. This will result in a considerable savings in computation time, since different time steps can be used for the two separate calculations.

The subcodes which are described herein for the plasticity deformation calculations have been incorporated and checked out in the FRAP-S steady state fuel rod analysis code. The development of a parallel package of creep strain calculations is in progress.

3.4 Overview of the FRACAS Subcode

The FRACAS subcode consists of six individual subroutines, each independent of the others. The model contained in each subroutine can be modified or replaced without requiring changes to any other part of the subcode.

Deformation and stresses in the cladding in the open-gap regime are computed in subroutine CLADF. The model considered is that of a thin cylindrical shell with specified internal and external pressures and a prescribed uniform temperature.

Calculations for the closed gap regime are made in subroutine COUPLE. The model considered is a thin cylindrical shell with prescribed external pressure and a prescribed radial displacement of its inside surface. The prescribed displacement is obtained from the fuel thermal expansion models contained in FRAP-S and FRAP-T. Since it is assumed no slip takes place when the fuel and cladding are in contact, the axial expansion of the fuel is transmitted directly to the cladding, and the change in axial strain in the shell is also prescribed.

Calculations for the trapped-stack regime are made in subroutine STACK. The model considered is a thin cylindrical shell with prescribed internal and external pressures and a prescribed total change in length of the cylinder. In contrast to CLADF and COUPLE, which solve for the stresses and strains at only one axial location at a time, subroutine STACK simultaneously solves for the stresses and strains in all axial nodes that are being strained axially by the "trapped stack" of fuel pellets.

The decision as to whether or not the gap is open or closed, and whether or not to call COUPLE, STACK, or CLADF, is made in the executive subroutine FCMI (Fuel-Clad Mechanical Interaction). This is the only subroutine that must be called by FRAP-S to initiate the fuel-cladding interaction analysis. At the completion of this analysis, FCMI returns either a new gap size or a new interface pressure between fuel and cladding for use in the next iteration of the thermal calculations.

In each subroutine (COUPLE, STACK, and CLADF), an elastic-plastic solution is obtained. Two additional subroutines, STRAIN and STRESS, compute changes in yield stress with work-hardening, given a uniaxial stress-strain curve. This stress-strain curve will be obtained from the material properties package subcode MATPRO. Subroutine STRAIN computes the effective total strain and new effective plastic strain given a value of effective stress and the effective plastic strain at the end of the last loading increment. Subroutine STRESS computes the effective stress given an increment of plastic strain and the effective plastic strain at the end of the last loading increment. Depending on the work-hardened value of yield stress, loading can be either elastic or plastic, and unloading is constrained to occur elastically. (Isotropic work-hardening is assumed in these calculations.)

3.5 Conclusion

The package of subroutines contained in FRACAS calculates the cladding stresses and strains in the three regimes of interaction between the fuel pellets and the cladding. Creep strain calculations are not considered in these subroutines. However, a parallel set of subroutines that calculate creep and stress relaxation effects in the three regimes of fuel-cladding interaction are currently in preparation.

It should also be reiterated that the FRACAS subroutine package, as currently written, considers the cladding to be locally isotropic and homogeneous. However, the subroutines have been written in such a manner as to minimize the effort required to incorporate anisotropic material behavior, and the modification to include anisotropic elastic and plastic material properties are underway.

The plasticity assumptions deserve some further comment. The assumption of isotropic work-hardening is best suited to problems with monotonic load histories, that is, with little or no strain reversal occurring during the load history. When strain reversal must be considered, the kinematic work-hardening assumption would be more appropriate, but the implementation of this theory is mathematically more complex, as well as making the inclusion of anisotropic material behavior more difficult. In an off-normal (accident) condition, the plastic straining should certainly be predominantly monotonic. Hence, the

isotropic work-hardening hypothesis in the "steady state" code, FRAP-S, is expected to be appropriate as long as the fuel rods are internally pressurized and not subject to compressive plastic strains attributed to the external coolant pressure. Since current design concepts of commercial pressurized water reactors utilize pressurized fuel pins, the use of the isotropic work-hardening hypothesis in FRAP-S is deemed appropriate.

4. A CRACKED PELLET GAP CONDUCTANCE MODEL – COMPARISON OF FRAP-S CALCULATIONS WITH MEASURED FUEL CENTERLINE TEMPERATURES

P. E. MacDonald, J. M. Broughton

This report describes a cracked pellet gap conductance model developed for use in the FRAP-S code. Comparison of model predictions using the proposed cracked pellet model and the Ross and Stoute model^[45,46] with fuel centerline temperature data is presented.

Fuel pellets crack extensively upon irradiation due both to thermal stresses induced by power changes and to accumulation of gaseous fission products. Therefore, the distance between the fuel and cladding will be circumferentially nonuniform, varying between that calculated for intact operating fuel rods and essentially zero (fuel segments lying against the cladding wall). Since uranium dioxide pellets primarily tend to crack radially, the pellet thermal conductivity will be only slightly influenced by this phenomenon. However, the fuel-to-cladding gap thermal conductance will be strongly increased and will generally correspond to a value reflecting both contact and open gap conductance.

Efforts to calculate fuel temperatures for rods with large, hot gaps, based on conductance across an annular gap between the cladding and the fuel using the Ross and Stoute model in FRAP-S, give poor agreement with available in-pile high-burnup temperature measurements. Calculated fuel temperatures are much higher than observed, and the calculated effect of fill-gas conductivity is greater than that measured. This disagreement between predictions and measurements is not surprising because pellet cracking in effect moves the gap thermal resistance toward the center of the pellet and replaces, to a certain extent, gap gas conduction with contact conduction. Calculated fuel temperatures using the cracked pellet gap conductance model in FRAP-S compare well with data throughout burnup.

4.1 Analytical Model

FRAP-S calculates the interrelated effects of fuel and cladding temperatures, rod internal pressure, fuel and cladding elastic and plastic deformation, release of fission product gases, fuel swelling, cladding growth, cladding corrosion, and crud deposition as functions of time and specific power. The fuel rod power history is approximated by a series of steady state power levels. The length of the rod is divided into a large number of axial segments, each assumed to operate at an average set of conditions over its length. The axial flux (power) shape is input and may be varied as a function of time. Fuel and cladding

temperatures, fuel swelling and thermal expansion, cladding growth, cladding stresses and strains, and fission gas releases are calculated separately for each axial segment. The fission gas release and fuel and cladding deformations are then integrated over the length of the fuel rod and added to previous power step values to obtain the rod internal pressure. This pressure is fed back into the fuel and cladding elastic and plastic deflection calculations in subsequent iterations.

Figure 79 presents a summary of the major interactions dealt with by FRAP-S. Because of the large number of interactions and feedbacks among the thermal conditions and the physical behavior of a fuel pin, several iterative processes are nested in the code calculations. The major iterative loops are:

- (1) At each axial increment, the gap and fuel temperatures, the fuel thermal expansion, and the cladding strain are iterated until convergence is obtained
- (2) At each specified time step, the total rod condition is recalculated with iterations continued until gas release and rod internal pressure are unchanged.

Fig. 79 Intermodal coupling in FRAP-S1.

If a nominal open hot gap is calculated, a fraction of the fuel pellet is assumed to be in contact with the cladding and an effective fuel-to-cladding gap conductance is calculated as follows:

$$h_{\text{gap}} = (1-F) h_o + F h_c \quad (1)$$

where

- h_{gap} = net gap conductance, $\text{W/m}^2\text{-}^\circ\text{K}$
- h_o = open gap conductance, $\text{W/m}^2\text{-}^\circ\text{K}$
- h_c = contact gap conductance, $\text{W/m}^2\text{-}^\circ\text{K}$
- F = fraction of pellet circumference in contact with the cladding.

The open gap conductance is determined from

$$h_o = \frac{k_{\text{mix}}}{\Delta r + \delta} \quad (2)$$

where

- k_{mix} = thermal conductivity of gas mixture, $\text{W/m}^2\text{-}^\circ\text{K}$
- Δr = nominal hot radial gap width, m
- δ = root mean square of the fuel and the cladding surface roughness. A value of 4.4×10^{-6} meters is normally used.

The zero pressure contact conductance, h_c , is determined directly from the equation for contact gap conductance to be discussed later in this report. Radiation heat transfer is assumed negligible.

The fraction of pellet circumference in contact with the cladding, F , is assumed to be an inverse power law function of the ratio of the nominal (uncracked) hot radial gap and the hot pellet radius as follows:

$$F = \frac{1}{a_1 \left(\frac{\Delta r \cdot 100}{R_F} \right)^{a_2} + 1.429} + 0.3 \quad (3)$$

where

- R_F = hot radius of the fuel pellet, m
- a_1 and a_2 = empirical constants determined from data.

The relationships for a_1 and a_2 as a function of burnup are described by Equations (4) and (5), respectively.

$$a_1 = 100 - 98 F' \quad (4)$$

$$a_2 = 4 - 0.5 F' \quad (5)$$

where

$$F' = 1 - \frac{1}{\left(\frac{x - 600}{1000}\right)^4 + 1}$$

x = burnup in MWd/MTU.

The fraction of pellet-cladding contact, Equation (3), is plotted in Figure 80 for both beginning and end of life. The burnup relationship, F' , is plotted in Figure 81. Equations (4) and (5) combined with Equation (3) yield a family of curves between the two curves shown

Fig. 80 Fraction of pellet circumference in contact with the cladding as a function of the ratio of the nominal hot radial gap to the hot pellet radius.

Fig. 81. Fractional effect of burnup as a function of burnup.

in Figure 80. Thus the fraction of the pellet circumference in contact with the cladding is calculated to increase with both decreasing nominal hot radial gas gap and burnup. The asymptotic value of pellet-cladding contact for large gaps, $F = 0.3$, and the empirical constants a_1 and a_2 were determined empirically by comparison of FRAP-S predictions with the fuel centerline temperature data.

When the fuel and cladding are in contact, the Mikic theory^[47,48] of thermal contact conductance, with modifications as proposed by Jacobs and Todreas^[49] for ceramic fuel elements, is used. The governing equation for contact conductance is therefore,

$$h_c = C_1 p^n + \frac{k_{mix}}{\delta} \quad (6)$$

where

C_1 = 2.7 for stainless steel cladding and 3.4 for zircaloy-2 and zircaloy-4 cladding

p = pellet-cladding contact pressure, psi

n = 1 for $0 \leq P \leq 1,000$ psi and $1/2$ for $P > 1,000$ psi.

The value of the exponent, n , is governed by the behavior of the fuel and cladding at the interface of the contact points. An exponent of 1.0 is valid only if the surface peaks of one of the materials are flowing plastically. This type of behavior is consistent with the Ross and Stoute^[45] theory of contact conductance. If the contact points of both materials are

behaving elastically, the correct value for the exponent, n , is approximately $1/2$. The experimental results of Fenech and Rohsenow^[48] support this value and also indicate that for metal-ceramic pairs the transition pressure from plastic to elastic flow is about 1,000 psi.

The parameter $\frac{k_{\text{mix}}}{\delta}$ in Equation (6) accounts for heat conduction through the gas in the gaps between contact points.

The cladding-fuel gap conductance is also, in part, dependent on the thermal conductivity of the gap gas mixture as shown in Equation (2). The relationship used in FRAP-S for calculating the thermal conductivity of a monatomic gas mixture is based on the work of Brokaw^[50], and the thermal conductivity equations of the individual rare gases are based on the correlative work of Ganchi and Saxena^[51]. The values published by the Westinghouse Electric Corporation^[52] for the thermal conductivity of air, nitrogen, and hydrogen are also included.

The thermal conductivity of the gas mixture is important in determining contact gap conductance as well as open gap conductance in a fuel rod. However, when the ratio of the mean free path of the gas molecules to the characteristic dimension (gap) of the body exceeds about 0.01, the Knudsen domain, normal heat flow formulas become inaccurate. Heat transfer from a solid to a gas in the Knudsen domain is then dependent on an accommodation coefficient which is defined as the ratio of the actual energy interchange to the maximum possible energy interchange between a surface and a gas. Generally, the accommodation coefficient of a heavy gas such as argon or xenon is near unity and is therefore neglected. But the change in heat transfer in the Knudsen domain cannot be neglected when the gap is filled with a light gas such as helium. Dean^[53] has presented the following factor by which the bulk thermal conductivity of helium should be divided when temperature drops are calculated for the Knudsen domain:

$$f = 1 + \frac{\alpha_1 + \alpha_2 - \alpha_1 \alpha_2}{\alpha_1 \alpha_2} \cdot \frac{4}{c_p + c_v} \cdot \frac{k}{\mu} \cdot \frac{\lambda}{\Delta r} \quad (7)$$

where

- α_1 = accommodation coefficient of the fuel
- α_2 = accommodation coefficient of the cladding
- c_p = constant pressure heat capacity
- c_v = constant volume heat capacity
- k = gas thermal conductivity
- μ = gas viscosity
- λ = the mean free path.

The mean free path can be written as

$$\lambda = \frac{3\mu}{\rho} \sqrt{\frac{1.22 \times 10^{-2} \text{ m}}{KT}} \quad (8)$$

where

m = mass of the molecule

K = Boltzman constant

ρ = density of the gas, $\frac{p}{RT}$

R = ideal gas constant

p = gas pressure

T = gas temperature.

If λ and ρ are substituted into Equation (7), and c_p and c_v are assumed not temperature dependent, Equation (7) can be rewritten as

$$f = 1 + C \frac{k \sqrt{T}}{p(\Delta r + s)} \quad (9)$$

A value for C of approximately 10^{-3} has been used in this study.

4.2 Comparison of Predictions With Experimental Data

Fuel centerline temperature comparisons between experimental data and FRAP-S predictions, using both the cracked pellet and the Ross and Stoute^[45,46] gap conductance models, are shown in Figures 82 through 100. Only well characterized rods with known power levels and measured system coolant conditions or cladding temperatures were considered. The following fuel rod conditions are reflected in the data: pellet density (92 to 96% TD), cold diametral gap (2 to 13 mils), and initial internal gas composition (helium and fission gas). Burnup ranged from 0 to about 15,300 MWd/MTU.

The data comparisons are grouped according to initial fill gas and burnup: (a) fission gas filled test rods with little burnup, (b) helium filled rods at beginning of life, and (c) helium filled rods at various burnups. The initial fuel rod conditions for each comparison are summarized on the figure showing that comparison.

Figures 82 through 85 present temperature data from Reference 54 for five capsules. Although the data were taken at relatively low power levels, typical commercial fuel rod dimensions and coolant conditions (except for initial gas composition) are represented. The

Fig. 82 Measured and predicted fuel centerline temperature versus power for Rod 22-15, Reference 54.

calculated values obtained using the cracked pellet model are in much better agreement with data from those Reference 54 test rods initially filled with fission gas than are the values calculated using the Ross and Stoute model. Predictions from both models are in reasonable agreement with the data for helium-filled rods. These results lend support to the concept that pellet cracking and relocation effectively increase the gap conductance and that a significantly greater proportion of the heat is transferred through mating asperity points than by gas gap conduction, even in rods with relatively large initial pellet-to-cladding gaps.

The comparisons of model predictions with data presented in Figures 86 and 87 show centerline temperature data from helium-filled rods that were irradiated in the Halden HBWR to about 4,200 MWd/MTU^[55]. These rods experienced periodic ramps to moderate power levels (approximately 13 kW/ft at the location of temperature measurement) throughout the irradiation period. Figures 86 and 87 show reasonable agreement between the prediction and the measurement for both gap conductance models. In Figure 86, hard fuel-cladding contact is predicted for a major portion of the power ramps (above 5 kW/ft). The FRAP-S gap conductance model calculated slightly increased centerline temperatures and changes in temperature with power as compared to the Ross and Stoute model. This

Fig. 83 Measured and predicted fuel centerline temperature versus power for Rods 22-2 and 22-9, Reference 54.

result is obtained because of the assumption in the Mikic model of elastic behavior of the mating asperity points at contact pressures above 1,000 psi. Figure 88 compares predictions obtained using both models with measured fuel centerline data reported in Reference 56. Excellent agreement is also shown for these large gap helium-filled rods.

Fuel centerline temperature data from the first four days of irradiation (four power ramps) of the Halden test assembly IFA-226 and model predictions are shown in Figures 89, 90, 91, and 92. The centerline temperature at a given power increased (compared with the preceding power ramp) during the first three power ramps and then stabilized in all four rods. The high power rods, Rods AO and AK (Figures 90 and 92, respectively), have slightly lower centerline temperatures at power than the lower power rods, Rods AA and AE (Figures 89 and 91, respectively). This difference in temperature at power would indicate that pellet cracking and relocation resulting from the higher powers experienced during the first ramp of Rods AO and AK resulted in increased gap conductance after temperature stabilization. Rods AA and AO (Figures 89 and 90, respectively) were fabricated nearly identically and thus the reduction in temperature at power cannot be attributed to the influence of initial dimensions or fuel density. Pellet cracking and relocation effects also appear to negate the difference expected among rods of different initial gap width. The centerline temperatures of the large gap rod, Rod AE (Figure 91), are similar to those of the standard rod, Rod AA (Figure 89), after the first power ramp. Figure 93 presents Rod AK

Fig. 84 Measured and predicted fuel centerline temperature versus power for Rod 22-7, Reference 54.

centerline fuel temperature measurements taken during both the increasing and decreasing portions of the first and second power ramps. The hysteresis effect at 10 to 14 kW/ft apparently is due to cracked pellet healing at high temperatures, followed by reformation of the pellet-to-cladding gap upon power reduction, followed in turn by the reinitiation of pellet cracking during temperature changes at low power levels or during the subsequent power ramp. FRAP-S predictions, obtained using both gap conductance models, are in good agreement with these data after the temperatures stabilize (Ramps 3 and 4).

Fig. 05 Measured and predicted fuel centerline temperature versus power for Rod 22-4, Reference 54.

Comparisons of model predictions with IFA-226 fuel centerline temperature data at various burnups to 15,300 MWd/MTU are shown in Figures 94 through 100. These data indicate that burnup effects on pellet cracking and relocation (and possibly UO_2 thermal conductivity which is not considered) reduce the temperature at a given power in a nonlinear fashion. The effects of burnup do not become noticeable in the experimental data until about 1,000 MWd/MTU of burnup and become constant at approximately 10,000 MWd/MTU. Figures 94 through 100 also suggest that, as burnup increases, the Ross and Stoute gap conductance model becomes progressively more conservative. At a burnup of about 15,300 MWd/MTU, the Ross and Stoute gap conductance model overpredicts measured fuel centerline temperatures by at least 1,000°F. The calculated release of fission product gases degrades the fill gas conductivity at high burnup and, therefore, substantially decreases the Ross and Stoute calculated gap conductance values. FRAP-S predictions obtained using the cracked pellet gap conductance model compare well with the IFA-226 temperature data throughout burnup, suggesting that increased pellet cracking and relocation with burnup should compensate for the degradation in gap gas thermal conductivity.

Fig. 86 Measured and predicted fuel centerline temperature versus power for Rod HBB, Reference 55.

Fig. 87 Measured and predicted fuel centerline temperature versus power for Rod HBC, Reference 55.

Fig. 88 Measured and predicted fuel centerline temperature versus power for the IFA-4 rod, Reference 56.

Fig. 89 Measured and predicted fuel centerline temperature versus power for Rod AA at 0 to 100 MWd/MTU.

Fig. 90 Measured and predicted fuel centerline temperature versus power for Rod A0 at 0 to 100 MWd/MTU.

Fig. 91 Measured and predicted fuel centerline temperature versus power for Rod AE at 0 to 70 MWd/MTU.

Fig. 92 Measured and predicted fuel centerline temperature versus power for Rod AK at 0 to 100 MWd/MTU.

Fig. 93 Measured fuel centerline temperature versus power for Rod AK during the first two power ramps.

Fig. 94 Measured and predicted fuel centerline temperature versus power for Rod AA at 1,110 MWd/MTU.

Fig. 95 Measured and predicted fuel centerline temperature versus power for Rod AO at 1,700 MWd/MTU.

Fig. 96 Measured and predicted fuel centerline temperature versus power for Rod AO at 2,900 MWd/MTU.

Fig. 97 Measured and predicted fuel centerline temperature versus power for Rod AA at 6,700 MWd/MTU.

Fig. 98 Measured and predicted fuel centerline temperature versus power for Rod AA at 10,900 MWd/MTU.

Fig. 99 Measured and predicted fuel centerline temperature versus power for Rod AA at 12,500 MWd/MTU.

Fig. 100. Measured and predicted fuel centerline temperature versus power for Rod AA at 15,300 MWd/MTU.

5. ANALYSIS OF FUEL BEHAVIOR DURING REACTIVITY INITIATED ACCIDENTS

L. B. Thompson, D. L. Hagrman, P. E. MacDonald

Reactivity initiated accidents (RIAs) caused by potential control rod drop or ejection are of concern in reactor safety research because fuel rod failures and possible failure propagation might result. Large and sudden reactivity insertions can lead to cladding meltthrough or prompt fuel dispersal. These phenomena were studied in the Special Power Excursion Tests (SPERT)^[57,58] and Transient Reactor Test (TREAT)^[59-63] programs under static coolant conditions. An experimental and supporting analytical program at the Idaho National Engineering Laboratory will evaluate a number of parameters in a manner more applicable to operating reactors. The experimental portion of this program is being planned for the Power Burst Facility at INEL.

This section describes a computational study seeking to determine important variables for RIA experiment design. It examines the role in RIAs of initial reactor conditions, total energy insertion, and fuel rod design parameters. The calculational approach is outlined and typical results for the time dependence of several physical variables are presented. Emphasis is placed on describing the nature of the phenomena and the coupling between major variables. Comparisons of predictions with SPERT data are made and deficiencies of the present models for use in RIA calculations are pointed out. The relative effect of potential experiment parameters upon maximum cladding temperatures is also discussed.

5.1 Calculational Model and General Predictions

The calculational results for RIAs discussed in the following sections were produced by the Fuel Rod Analysis Program - Transient (FRAP-T) developed at INEL. FRAP-T incorporates coupled models for thermal and mechanical behavior and contains an extensive material properties package. It predicts the time dependence of numerous variables at a specified number of axial fuel rod positions for any transient power history. The code has been used to describe fuel rod behavior during loss-of-coolant accidents, power-coolant-mismatch accidents, and inlet flow blockage accidents in addition to RIAs.

The primary input data required by FRAP-T are: power history, fuel rod radial and axial power profiles, descriptions of the fuel rod cold state (including internal gas composition and pressure), initial inlet coolant enthalpy, mass flow rate history, pressure history, and flow channel information. Histories of the coolant enthalpy and rod-to-coolant heat transfer coefficient can either be entered as input data or calculated by the code.

Figures 101 through 105 illustrate the general relationships among several variables as predicted by FRAP-T. The situation shown corresponds to an enthalpy insertion of 8.4×10^5 J/kg (200 cal/g) in a BWR-6 rod under hot standby conditions (pressurized, heated coolant, and 10^{-6} of full power – an average of 8.3 kW/ft). The initial internal fuel rod pressure is assumed to be one atmosphere. All variables are plotted at the hottest rod elevation of six axial locations that were considered on the rod.

Fig. 101 Transient power and the resulting BWR-6 fuel rod temperatures for an 8×10^5 J/kg (200 cal/g) reactivity initiated accident from hot standby reactor conditions.

Figure 101 shows a power history^[64] that would produce a total UO_2 enthalpy insertion of 8.4×10^5 J/kg of UO_2 . The power burst was limited by Doppler feedback and a scram bank insertion was assumed, but the further limiting effects of void formation were

Fig. 102 Radial pellet temperature profiles before, during, and after peak cladding temperatures are reached during an 8×10^5 J/kg (200 cal/g) RIA from hot standby reactor conditions.

not considered. The fuel centerline and surface temperatures rise sharply with the power due to localized energy generation. The cladding surface temperature increase is delayed, however, due to the initial existence of a fuel-cladding gap. During this early time period, the radial temperature profile in the fuel is relatively flat, peaking near the pellet surface, and interior radial heat conduction is minimal. The fuel surface temperature then drops sharply due to gap closure and the resulting increase in gap conductance, but rises again as heat is conducted from near the pellet surface. The cladding surface temperature reaches a peak between 0.7 and 0.8 second, dropping only slowly thereafter due to the presence of flow film boiling and a low surface heat transfer coefficient (HTC).

Although not apparent in Figure 101, the centerline temperature gradually increased after about 0.4 second; up to a time of two seconds, the highest fuel temperatures exist beyond the pellet centerline (the maximum temperature occurs at about seven tenths of the pellet radius at a time slightly less than 0.5 second). Figure 102 shows representative temperature profiles in the fuel. Early in the accident, the temperature is nearly constant except for peaking toward the pellet surface due to a slight central flux depression. In the interior of the pellet, heat conduction is small and directed toward the centerline. Only energy stored near the pellet surface can affect the cladding and coolant during the short time prior to the establishment of maximum cladding temperature. Knowledge of these trends will help in understanding the relative influence of potential experiment parameters on fuel behavior during an RIA.

Fig. 103 The relationships between pellet-cladding gap width, gap conductance, and cladding hoop strain.

Figure 103 shows the relationships between gap width, gap conductance, and cladding hoop strain. The fuel expands more than the cladding because of its much higher temperature; the gap closes, and the gap conductance reaches high values [a maximum of about $85,000 \text{ W/m}^2\text{-}^\circ\text{K}$ ($15,000 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$)]. Fuel-cladding pressure maintains high gap conductance values. After the interfacial pressure equals the cladding yield stress (at about 0.15 second), the fuel and cladding surfaces expand radially together. The maximum cladding hoop strain is about 0.017.

Figure 104 shows the fuel rod surface-to-coolant HTC and the cladding surface temperature. The HTC is constant at $18,000 \text{ W/m}^2\text{-}^\circ\text{K}$ ($3,150 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$) when the heat transfer mode is forced convection to the liquid. When the surface goes into nucleate boiling, the HTC quickly rises to a maximum of $99,000 \text{ W/m}^2\text{-}^\circ\text{K}$ ($17,500 \text{ Btu/hr-ft}^2\text{-}^\circ\text{K}$). Flow transition boiling follows, and the energy transfer mode becomes flow film boiling with an HTC of about $1,900 \text{ W/m}^2\text{-}^\circ\text{K}$ ($340 \text{ Btu/ft}^2\text{-hr-}^\circ\text{K}$). At this point, the fuel-cladding gap is nearly closed and the cladding temperature begins to increase rapidly. The early pre-CHF regime is so brief that these results are insensitive to the surface heat transfer

Fig. 104 The relationship between surface heat transfer coefficient and cladding surface temperature during an 8×10^5 J/kg (200 cal/g) RIA from hot standby reactor conditions.

correlation during that time. However, post-CHF rod to coolant heat transfer correlations do significantly affect a number of calculated variables.

Figure 105 shows the fuel total axial displacement and the internal gas pressure predicted for this RIA. The fuel stack elongation may be overestimated as indicated in Section 5.2. The increase in internal gas pressure is due to the plenum volume change caused by the elongation of the pellet stack and also to a predicted rod compression caused by the external coolant pressure. The present lack, in FRAP-T, of cladding stretching and transient gas release models partially offset one another with respect to internal pressure predictions and are areas in which development is planned.

Fig. 105 The relationship between fuel axial displacement and gas gap pressure during an 8×10^5 J/kg (200 cal/g) RIA from hot standby reactor conditions.

5.2 Comparison of FRAP-T Predictions With SPERT Data

FRAP-T has been applied to published data from power excursion experiments to test the capability of the codes to predict the response of fuel rods to rapid and large over-power transients. From 1968 to 1970, SPERT tests were conducted in the Capsule Driver Core (CDC). The CDC was used to provide self-limiting power bursts, with periods of about 5 msec, to study the energy deposition required for fuel rod failure. The core was approximately three feet in diameter and consisted of six-foot-long UO_2 fuel rods. A central test space of 0.09-m diameter and 1.6-m length held a capsule containing the test rod. The test rod was at room temperature and atmospheric pressure when a power transient was initiated by ejection of a control rod.

In SPERT Test GEX-692, a 3.5-cm rod having a 2.0-cm active fuel length was subjected to a power burst that resulted in a fuel enthalpy insertion of 9.0×10^5 J/kg (216 cal/g)^[65]. The very low coolant pressure and temperature of the SPERT experiments, however, are outside the range of the rod-to-coolant heat transfer correlations presently coded in FRAP-T. As a result, HTC histories must be assumed in order to examine such cases.

Figures 106 through 109 compare FRAP-T results with the SPERT experimental data. A constant surface HTC of $1,500 \text{ W/m}^2\text{-}^\circ\text{K}$ ($265 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$) was used. Cladding surface temperature is reasonably well predicted, internal gas pressure and cladding expansion are underpredicted, and fuel expansion is significantly overpredicted. The latter may be caused by the present lack in FRAP-T of a fuel plastic deformation model and the fact that the fuel-cladding mechanical interaction model now in FRAP-T does not produce axial stretching of the cladding or compression of the fuel stack due to fuel-cladding differential thermal expansions when the gap is closed. Such phenomena in SPERT experiments could have produced additional cladding elongation and reduced fuel stack elongation.

Agreement with the experimental cladding temperatures (Figure 106) would be improved if the HTC was decreased slightly with time -- a situation that would be expected to occur. However, because of the very early onset of film boiling, ignoring the high precritical heat flux heat transfer coefficients apparently does not lead to error. The internal gas pressure results (Figure 109), which show significantly lower calculated results than the measured data, are at least partially due to the fact that the fuel contained large amounts of impurities and the FRAP-T models do not consider impurity release. (These pressure data and calculations were taken from a FRAP-T comparison with SPERT Test GEP-487, as these data were not available from Test GEX-692^[57,65].)

Fig. 106 Comparison between SPERT Test GEX-692 cladding temperature data and FRAP-T calculations assuming a constant surface heat transfer coefficient of $1,500 \text{ W/m}^2\cdot\text{°K}$ ($265 \text{ Btu/hr}\cdot\text{ft}^2\cdot\text{°F}$). The enthalpy insertion was $9.0 \times 10^5 \text{ J/kg}$ (216 cal/g).

Fig. 107 Comparison between SPERT Test GEX-692 cladding axial displacement data and FRAP-T calculations assuming a constant surface heat transfer coefficient of $1,500 \text{ W/m}^2\cdot\text{°K}$ ($265 \text{ Btu/hr}\cdot\text{ft}^2\cdot\text{°F}$). The enthalpy insertion was $9.0 \times 10^5 \text{ J/kg}$ (216 cal/g).

Fig. 108 Comparison between SPERT Test GEX-692 fuel axial displacement data and FRAP-T calculations assuming a constant surface heat transfer coefficient of $1,500 \text{ W/m}^2\text{-}^\circ\text{K}$ ($265 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$). The enthalpy insertion was $9.0 \times 10^5 \text{ J/kg}$ (216 cal/g).

When higher energy SPERT tests are considered, larger surface heat transfer coefficients must be employed in FRAP-T to predict the experimental surface temperature data. A value of nearly $6,000 \text{ W/m}^2\text{-}^\circ\text{K}$ ($1,000 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$) is required to fit cladding temperatures of Test 543^[57] in which the enthalpy increase was $1.5 \times 10^6 \text{ J/kg}$ (360 cal/g). This trend is opposite to that predicted by heat transfer correlations under pressurized and heated coolant conditions, where, as the rod surface heat flux increases, a larger film forms and the HTC normally decreases. Radiation does not appear to be a large enough factor to explain the required HTCs. Even with an emissivity of unity, a $1,700^\circ\text{C}$ rod surface temperature produces a radiation heat flux of about $8 \times 10^5 \text{ W/m}^2$, which is small compared to the flux of more than $70 \times 10^5 \text{ W/m}^2$ that is predicted to be necessary. An extremely precipitous drop in gap conductance would be required to explain the data without a large HTC, as a 50% decrease has very little effect on maximum temperatures.

Fig. 109 Comparison between SPERT Test GEP-487 internal fuel rod pressure data and FRAP-T calculations assuming a constant surface heat transfer coefficient of $1,500 \text{ W/m}^2\text{-}^\circ\text{K}$ ($265 \text{ Btu/hr-ft}^2\text{-}^\circ\text{F}$). The enthalpy insertion was $9.0 \times 10^5 \text{ J/kg}$ (216 cal/g).

Uncertainties in the experimental energy input or cladding temperature data could reduce the magnitude of the HTC that is required to fit the data; however, free convective effects may also be involved.

5.3 Maximum Cladding Temperatures

As determined by FRAP-T, the maximum cladding temperatures that are predicted to occur during an RIA are among the most significant indicators of accident severity. The relative influence of parameters that affect maximum surface temperatures, the effect of initial reactor conditions, and the effect of the potential experiment variables are discussed in the following sections.

5.3.1 Effect of Initial Reactor Conditions. Three boiling water reactor conditions have been considered as the starting point for an RIA: cold startup, hot standby, and full power. In the first two cases, the power is considered to be 10^{-6} of full power (full power assumed to be 8.3 kW/ft). Under cold startup conditions, the coolant inlet temperature was assumed to be 21°C under a pressure of 6.9×10^5 Pa (100 psia); under hot standby and full power conditions, the coolant was assumed to be at 279°C (535°F) inlet temperature at 7.2×10^6 Pa (1,040 psia) pressure. In all cases, a “design” flow of 5.9×10^6 kg/m²-sec (1.2×10^6 lbm/ft²-sec) was assumed. The power profile^[64] shown in Figure 101 was used and the design parameters for BWR-6 rods were employed.

Figure 110 compares cladding surface temperature caused by an 8.4×10^5 J/kg (200 cal/g) enthalpy insertion under the three reactor conditions. Although the full power case is the most severe in Figure 110, the decreased control rod worth at full power decreases the possibility of large enthalpy increases. In a study of possible enthalpy insertions, the hot standby case has been found to be potentially more severe than a 10% full power case^[64,66]. The heat transfer coefficients given in Figure 110 for the hot standby and full power cases are the approximate values calculated by the code at peak cladding temperature. The HTC's then gradually drop by about 15% during the remaining period shown in the figure.

Fig. 110 Cladding surface temperature as a function of time for an 8×10^5 J/kg (200 cal/g) RIA from three initial reactor conditions.

Since, as in the SPERT cases, the HTC correlations in FRAP-T presently do not apply to the cold startup conditions, HTC histories must be input. The coefficient found to fit the SPERT data of Figure 106 ($h = 1,500 \text{ W/m}^2\text{-}^\circ\text{K}$) was used for one curve in Figure 110 and a somewhat larger value was used for the second cold startup case. Although the HTC is more dependent on the bulk coolant properties than on the fuel rod condition, a larger value than $1,500 \text{ W/m}^2\text{-}^\circ\text{K}$ might be expected with design flow than in the static coolant case of SPERT. The use of a constant coefficient does not invalidate the result due to the very early occurrence of critical heat flux in an RIA history or the expected gradual drop in the HTC after peak temperatures are reached.

Figure 111 shows the effect of the fuel pellet enthalpy increase on the maximum cladding temperatures achieved. At enthalpy increases above $1.2 \times 10^6 \text{ J/kg}$ (290 cal/g), FRAP-T predicts that an RIA from hot standby will produce virtually the same maximum cladding temperatures as the same energy insertion from full power. The fuel rod surface-to-coolant heat flux is much higher in the early stages of the full power case. By the time maximum cladding temperatures are reached, this higher flux has virtually eliminated the difference between full power and hot standby cladding temperatures. This difference then slowly increases with time because a greater heat flux from the interior of the pellet exists in the full power case.

Fig. 111 Maximum cladding temperature as a function of UO_2 enthalpy increase for RIAs from hot standby and full power reactor conditions.

Some caution is necessary in drawing conclusions from the higher enthalpy results of Figure 111 due to the presence of fuel melting. Although peak cladding temperatures for higher enthalpy insertions are the same in the two cases shown, the stored energy in the fuel is at least 3.3×10^5 J/kg (80 cal/g) greater in the full power case. Even from hot standby, a 1.67×10^6 J/kg (400 cal/g) insertion produces molten fuel to a distance of more than four-fifths of the pellet radius with a maximum temperature greater than $3,500^\circ\text{C}$. FRAP-T at present lacks models for molten fuel behavior. Fission gas bubble expansion and hydrostatic effects due to the sudden creation of a central melt could alter the predicted results for large enthalpy insertions.

5.3.2 Relative Effect of Enthalpy Increase and Other Variables. Table XII lists potential experiment variables in the approximate order of their significance according to FRAP-T for a 1.0×10^6 J/kg (250 cal/g) RIA from BWR hot standby conditions. The maximum cladding temperature was used as the criterion for testing the relative effect of these variables. The UO_2 enthalpy increase is found to be the most significant variable in determining the consequences of an RIA. The rod-to-coolant film boiling heat transfer coefficient is also found to be highly important. Fuel rod diameter is significant, depending upon the specific thermal-hydraulic boundary conditions imposed. For the most part, according to FRAP-T, rod design parameters have relatively small effects on maximum cladding temperatures.

The high temperatures in an RIA cause very early gap closure, a high gap conductance, and early film boiling. Original gap width and factors affecting the gap heat transfer lose importance. The small time involved means that only energy that is near the pellet surface can affect the cladding surface. Longer times for heat conduction from the pellet interior are required (Figure 103). As a result, the key factors are the enthalpy in the outer portion of the fuel and the rate that this enthalpy is transferred to the coolant.

Of the first group of parameters in Table XII, only fuel pellet enthalpy has a positive influence coefficient with respect to maximum cladding temperature; that is, an increase in maximum enthalpy increases the maximum temperature. An increase in heat transfer coefficient or rod diameter causes a decrease in expected maximum temperatures.

Of the second group of parameters in Table XII, the gap width, cladding thickness, coolant pressure, and pellet density have positive influence coefficients with respect to the maximum cladding temperature. The other variables have negative influence coefficients with respect to the maximum temperature – increasing their value decreases the expected maxima. The effect of burst width is found to be small in the burst time range shown in Figure 101.

If the comparisons discussed are made at times much different from that of the maximum cladding temperature, the order of some variables in Table XII may change. The following example will offer an illustration: If the fuel and cladding roughness is increased

TABLE XII
FRAP-T INPUT PARAMETERS AFFECTING
THE MAXIMUM CLADDING TEMPERATURE
IN A REACTIVITY INITIATED ACCIDENT^[a]

Most Important Variables:

- Fuel pellet enthalpy increase
- Rod-to-coolant heat transfer coefficient
- Fuel rod diameter

Less Important Variables

- Coolant mass flow rate
- Initial pellet-cladding gap width
- Cladding thickness
- Coolant pressure
- Power burst width
- Pellet density

[a] A 1.0×10^6 J/kg (250 cal/g) RIA from BWR hot standby reactor conditions was assumed in this study.

(from a "base case" value) for a FRAP-T calculation, the gap conductance after fuel-cladding contact is decreased nearly in proportion. Early in the accident the pellet surface temperature is higher compared to the "base case" and the cladding surface temperature is lower. The heat flux across the gap is larger due to the larger temperature differences, however, and by the time maximum cladding temperatures are reached, the increased-roughness case and the "base case" give approximately the same results.

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