350
12-30-75
NRC-2, 3, 4
Germany (2)
Japan

1888

ANCR-1262

NRC-2
NRC-3
NRC-4

MASTER

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

Aerojet Nuclear Company

IDAHO NATIONAL ENGINEERING LABORATORY

Idaho Falls, Idaho — 83401

DATE PUBLISHED—NOVEMBER 1975

PREPARED FOR THE

ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION

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

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED

DISCLAIMER

This report was prepared as an account of work sponsored by an agency of the United States Government. Neither the United States Government nor any agency Thereof, nor any of their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness, or usefulness of any information, apparatus, product, or process disclosed, or represents that its use would not infringe privately owned rights. Reference herein to any specific commercial product, process, or service by trade name, trademark, manufacturer, or otherwise does not necessarily constitute or imply its endorsement, recommendation, or favoring by the United States Government or any agency thereof. The views and opinions of authors expressed herein do not necessarily state or reflect those of the United States Government or any agency thereof.

DISCLAIMER

Portions of this document may be illegible in electronic image products. Images are produced from the best available original document.

Printed in the United States of America
Available from
National Technical Information Service
U. S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22161
Price: Printed Copy \$7.60; Microfiche \$2.25

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the Energy Research and Development Administration, nor any of their employees, nor any of their contractors, subcontractors, or their employees, makes any warranty, express or implied, or assumes any legal liability or responsibility for the accuracy, completeness or usefulness of any information, apparatus, product or process disclosed, or represents that its use would not infringe privately owned rights.

ANCR-1262

Distributed Under Categories:
NRC-2, -3, and -4
Water Reactor Safety Research
Systems Engineering
Fuel Behavior
Analysis Development
TID-4500

QUARTERLY TECHNICAL REPORT ON WATER REACTOR SAFETY PROGRAMS
SPONSORED BY THE NUCLEAR REGULATORY COMMISSION'S DIVISION
OF REACTOR SAFETY RESEARCH, APRIL – JUNE 1975

Approved by

Dr. F. H. Tingey

AEROJET NUCLEAR COMPANY

Date – November 1975

PREPARED FOR THE
U.S. ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION
IDAHO OPERATIONS OFFICE
UNDER CONTRACT NO. E(10-1)-1375

DISTRIBUTION OF THIS DOCUMENT IS UNLIMITED *rb*

FOREWORD

This report describes Aerojet Nuclear Company's (ANC) technical activities in Water Reactor Safety Programs for the fourth quarter of FY-75 (March through June 1975). These activities were conducted by ANC at the Idaho National Engineering Laboratory (INEL) under the sponsorship of the Nuclear Regulatory Commission's Division of Reactor Safety Research. The reader is advised that some of the data reported here are preliminary in nature and subject to change or revision at a later date. An attempt to identify these areas has been made in text. A comprehensive description of the ongoing programs is presented in the January through March 1975 Quarterly Technical Report, ANCR-1254. Copies of all Quarterly Reports issued by ANC are available from the Technical Information Center, Energy Research and Development Administration, Oak Ridge, Tennessee.

PREFACE

Aerojet Nuclear Company (ANC), as a prime contractor to ERDA 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 accomplished in four programs: the Semiscale Program, the Loss-of-Fluid Test (LOFT) Program, the Thermal Fuels Behavior Program, and the Reactor Behavior Program.

The Semiscale Program consists of a continuing series of small-scale nonnuclear thermal-hydraulic experiments having as their primary purpose the generation of experimental data that can be applied to the development and verification of analytical models describing loss-of-coolant accident (LOCA) phenomena in water-cooled nuclear power plants. Emphasis is placed on acquiring system effects data from integral tests that characterize the most significant thermal-hydraulic phenomena likely to occur in the primary coolant system of a nuclear plant during the depressurization (blowdown) and emergency cooling phase of a LOCA. The current program of experiments employs 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, established 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.

CONTENTS

FOREWORD	ii
PREFACE	iii
I. SEMISCALE PROGRAM	1
1. SIMULATING NUCLEAR FUEL RODS WITH ELECTRIC HEATERS FOR LOSS-OF-COOLANT ACCIDENT EXPERIMENTS	1
1.1 Electric Rod Description	1
1.2 Nuclear Fuel Rod Simulation	3
1.3 Results of Nuclear Rod Simulation	5
1.4 Post-DNB Rod Performance	8
2. REFERENCE	12
II. LOFT PROGRAM	13
1. LOFT EXPERIMENT PLANNING AND TEST EVALUATION	13
1.1 LPWR System Model Description	15
1.2 LOFT System Model	17
1.3 Hot Pin Models	20
1.4 Analysis of the LOFT and LPWR Core Behavior During a Simulated Loss-of-Coolant Accident (LOCA)	20
1.5 Discussion of Results	25
1.6 Conclusions	40
2. LOFT FUEL MODULES DESIGN, CHARACTERIZATION, AND FABRICATION PROGRAM	41
2.1 Design	43
2.2 Characterization	55
2.3 Fabrication	55
2.4 Preproduction Process Demonstration Program	72
2.5 Conclusion	72
3. REFERENCES	76
III. THERMAL FUELS BEHAVIOR PROGRAM	78
1. POWER COOLING MISMATCH (PCM-20)	79
1.1 Introduction	79
1.2 Test Rod, Experiment Test Train, and Instrumentation	80
1.3 Instrument Performance	84
1.4 Test Results	85

2.	IRRADIATION EFFECTS TEST SERIES FUEL ROD CHARACTERIZATION EXAMINATION	102
3.	CONCLUSIONS	116
4.	REFERENCES	119
IV.	REACTOR BEHAVIOR PROGRAM	120
1.	INTRODUCTION	120
2.	ADVANCED LOCA CODE DEVELOPMENT	120
2.1	Core Code Development Work	120
2.2	Plenum Code Development Work	136
2.3	Loop Code Development Work	149
3.	REACTOR FUEL MODEL DEVELOPMENT	163
3.1	FRAP-T1 Development	163
3.2	FRAP-S Development	164
3.3	Transient Plenum Temperature Model For FRAP-T1	164
3.4	Reactivity Initiated Accident Analysis	182
3.5	Fuel Elastic Modulus	201
3.6	A Fuel Thermal Expansion Model	208
4.	CODE VERIFICATION	212
4.1	Comparison of SCORE-EVET Calculations with LOFT Flow Distribution Data	213
4.2	ANC Technical Support to NRC for Industry Cooperative Programs	218
5.	REFERENCES	221

FIGURES

I-1.	Diagram of the Mod-1 electrical heater	2
I-2.	Mod-1 electrical heater rod cross section	3
I-3.	One-dimensional LOFT nuclear rod model	5
I-4.	One-dimensional Mod-1 heater rod model	6
I-5.	Comparison of Mod-1 and LOFT heat fluxes and heat transfer coefficient assuming nucleate boiling throughout blowdown	7

I-6.	Assumed core pressure and fluid temperature during blowdown	8
I-7.	Transient electrical power factor to obtain nuclear rod simulation until a DNB	9
I-8.	Comparison of Mod-1 and LOFT surface heat fluxes after DNB	11
I-9.	Comparison of Mod-1 and LOFT surface temperature after DNB	12
II-1.	Westinghouse conservative and realistic predictions of the LOFT hot spot cladding temperatures	15
II-2.	Schematic of the RELAP4 LPWR system model	16
II-3.	Heat conducting structures used in the RELAP4 LPWR system model	16
II-4.	Schematic of the RELAP4 LOFT system model	18
II-5.	Schematic of the RELAP4 LOFT system model heat structure	19
II-6.	Normalized axial power distributions in the RELAP4 LOFT/LPWR hot pin models	21
II-7.	Flow schematic for the LOFT core flow study	22
II-8.	Comparison of the LOFT and LPWR upper plenum pressures	28
II-9.	LPWR/LOFT 'Lo' flow (616°F) upper plenum pressures	28
II-10.	LPWR/LOFT 'Lo' flow (616°F) plenum temperatures	29
II-11.	Part 1 - time to reach critical heat flux for the LOFT and LPWR hot pin models (LPWR plenum conditions)	31
II-12.	Comparison of the LOFT/LPWR hot spot cladding temperatures (standard RELAP4 CHF correlations)	32
II-13.	Comparison of LOFT/LPWR hot spot cladding temperatures (Griffith-Walkush CHF correlations)	32
II-14.	Comparison of heat transfer coefficients and fluid qualities calculated by the standard RELAP4 and modified RELAP4 (Griffith-Walkush) heat transfer logic	33
II-15.	Comparison of cladding surface temperatures predicted by the standard and modified (Griffith-Walkush) RELAP4 heat transfer logic	33
II-16.	The variation in rewetting temperatures at different system pressures	34
II-17.	Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations)	35

II-18.	Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations)	36
II-19.	Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations)	36
II-20.	Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations)	37
II-21.	Part 2 - Time and location of critical heat flux in the LOFT and LPWR hot pin (dissimilar boundary conditions)	38
II-22.	LOFT core layout	42
II-23.	LOFT Type A and F fuel modules	47
II-24.	LOFT Type B and D fuel modules	48
II-25.	LOFT Type C and E fuel modules	49
III-1.	Schematic drawing of PCM-20 PWR-type test rod	81
III-2.	PCM-20 PWR-type test rod installed in single rod test hardware	83
III-3.	Sketch of grounded junction thermocouple construction used for cladding surface temperature measurement	85
III-4.	Peak test rod power versus core power during power calibration	87
III-5.	Change in axial cladding length versus peak test rod power	89
III-6.	Power history for PCM-20 Test 8-1RS	90
III-7.	Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-1RS (CHF Power Cycle No. 1)	91
III-8.	Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-1RS (CHF Power Cycle No. 2)	93
III-9.	Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-1RS (CHF Power Cycle No. 3)	95
III-10.	Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-1RS (CHF Power Cycle No. 4)	96
III-11.	Posttest photograph of PCM-20 Test 8-1RS fuel rod	98

III-12.	Sectioning diagram load follow Rod No. 837	104
III-13.	Sectioning diagram load follow Rod No. 864	105
III-14.	Sectioning diagram – water tube STD 50	106
III-15.	Pseudo-map of defect indications on water tube STD-50	107
III-16.	Pseudo-map of defect indications on water tube No. 5	108
III-17.	Longitudinal Section 837 M-1 prepared from Rod 837 showing defect of cladding	110
III-18.	Micrograph of surface defect found in fuel Rod 837 Section 837 M-1, showing bottom of pit	110
III-19.	Micrograph of defect found on OD surface of fuel Rod 837 in Specimen 837 M-3	111
III-20.	Micrograph of surface defect found on OD of fuel Rod 837 in Specimen 837 M-4	111
III-21.	Micrograph of cladding of fuel Rod 837	112
III-22.	Simplified defect chart of load follow fuel Rod No. 864	113
III-23.	Partial pseudo-map of load follow Rod 864	114
III-24.	Micrograph of cladding of fuel Rod 864 showing corrosion product on surface	115
III-25.	Micrograph of 0.0008 inch deep defect found on inside of water tube number STD-50 at 215 degree rotation in Sample S50 M-2	116
III-26.	Micrograph of grooves found on OD of water tube STD-50 in Sample S50 M-3 At 200 degree rotation	117
III-27.	Micrographs of ID defects found in water tube STD-50 in Sample S50 M-3 at 0 degree rotation	118
IV-1.	Velocity oscillations in vertical pipe under constant pressure drop	122
IV-2.	Logarithmic density profiles at t = 0.7, 1.5, and 2.3 sec	123
IV-3a.	Sloshing problem at time = 0.0 sec	124
IV-3b.	Sloshing problem at time = 0.200 sec	125
IV-3c.	Sloshing problem at time = 0.400 sec	125
IV-3d.	Sloshing problem at time = 0.60 sec	126

IV-3e.	Sloshing problem at time = 0.800 sec	126
IV-3f.	Sloshing problem at time = 1.00 sec	127
IV-4.	Sloshing problem at time = 2.00 sec	135
IV-5.	Cell configuration at solid wall	147
IV-6.	Mean velocity	148
IV-7.	Turbulent energy	149
IV-8.	Shear stress	150
IV-9.	Simplified UVUT code flow diagram	152
IV-10.	Simplified UVET solution scheme	155
IV 11.	Plenum energy flow model	165
IV-12.	Spring noding	166
IV-13.	Cladding noding	166
IV-14.	Geometrical relationship between the cladding and spring	172
IV-15.	Coolant temperature and heat transfer coefficient for hypothetical LOCA . .	176
IV-16.	Plenum gas, spring, and cladding temperatures responses to hypothetical LOCA conditions with an insulator pellet temperature (T_{ep}) of 1,700°F	177
IV-17.	Plenum gas temperature response as a function of end pellet temperature (T_{ep})	178
IV-18.	Plenum gas temperature response for a 50% change in the spring natural convection heat transfer coefficient (h_g)	179
IV-19.	Plenum gas temperature response for a 50% change in the cladding natural convection heat transfer coefficient (h_{cl})	180
IV-20.	Temperature response with and without a spring	181
IV-21.	LOCA plenum gas temperature response calculated by FRAP-T1	182
IV-22.	Global coordinate and element numbering system for an N-d.o.f. axial system (restrained)	185
IV-23.	Local force and displacement coordinates	186
IV-24.	Fuel rod idealization	188
IV-25.	Assumed fuel-clad conditions	191

IV-26. Cracked segment of fuel pellet	192
IV-27. Clad stress equilibrium	192
IV-28. Temperature history with representative ramp	193
IV-29. Fuel rod response to RIA thermal transient	194
IV-30. Fuel rod response to RIA thermal transient	194
IV-31. Fuel rod response to RIA thermal transient	195
IV-32. Fuel rod response to RIA thermal transient	195
IV-33. Fuel rod response to RIA thermal transient	196
IV-34. Fuel rod response to RIA thermal transient	196
IV-35. Fuel rod response to RIA thermal transient	197
IV-36. Fuel rod response to RIA thermal transient	197
IV-37. Fuel rod response to RIA thermal transient	198
IV-38. Fuel rod response to RIA thermal transient	198
IV-39. Fuel rod response to RIA thermal transient	199
IV-40. Fuel rod response to RIA thermal transient	199
IV-41. Comparison of Young's modulus data for UO_2 at 25°C and the fraction of porosity, from a least squares fit to Equation (135)	203
IV-42. Comparison of Young's modulus for UO_2 normalized to 100% TD as a function of temperature, with least square fits to Equation (136)	204
IV-43. Young's modulus as a function of temperature and porosity for $\text{UO}_2\text{-PuO}_2$ mixed oxides as calculated from Equation (140)	207
IV-44. Comparison of UO_2 thermal expansion data with that calculated from FHTEXP subcode	210
IV-45. Temperature dependent of PuO_2 thermal expansion calculated from Tokar et al equation	211
IV-46. Comparison of PuO_2 thermal expansion data with that calculated from FHTEXP subcode	212
IV-47. Schematic of the LOFT core model used in SCORE-EVET	214
IV-48. Typical RELAP4 data comparison; TLTA heater rod temperatures	220

TABLES

I-I.	Heat Transfer Coefficient after DNB	10
II-I.	RELAP4 LOFT/LPWR Initial Steady State System Conditions	23
II-II.	Summary of Results for LOFT Core Flow Study	26
II-III.	Results of the BACTRAC3 Sensitivity Study	39
II-IV.	LOFT Fuel Module Design Values	44
II-V.	LOFT Fuel Module Thermal-Hydraulic Design Program	56
II-VI.	LOFT Fuel Module Structural Design Program	58
II-VII.	LOFT Fuel Module Miscellaneous Design Activities	70
II-VIII.	LOFT Fuel Bundle Fabrication Process Qualifications/Demonstrations Program	73
III-I.	Nominal Design Characteristics of PCM-20 Test Fuel Rod	82
III-II.	Summary of Power Calibration Data During PCM-20 Test 8-1RS	86
III-III.	Summary of Nominal System Coolant Conditions	91
III-IV.	Post-CHF Data Summary for CHF Runs 1, 2, 3, and 4	92
III-V.	Sectioning Plan for PCM-20 8-1RS Test Rod	99
III-VI.	Comparison of Measured, Estimated, and Calculated Fuel Rod Behavior	101
IV-I.	Comparison of Calculated and Experimental Friction Factors	147
IV-II.	Nomenclature	168
IV-III.	Physical Constant Used for Sensitivity Analysis	175
IV-IV.	FRIDA Input Parameters	193
IV-V.	Inlet Velocity Distribution for SCORE-EVET Isothermal Mixing Calculations	215
IV-VI.	SCORE-EVET Velocity Calculations Compared to Exxon Data 24.45 in. above the Lower Support Plate	216
IV-VII.	SCORE-EVET Velocity Calculations Compared to Exxon Data and COBRA Calculations 38.85 in. above the Lower Support Plate	217

I. SEMISCALE PROGRAM
J. O. Zane, Program Manager

1. SIMULATING NUCLEAR FUEL RODS WITH ELECTRIC HEATERS

FOR LOSS-OF-COOLANT ACCIDENT EXPERIMENTS

D. M. Snider

The Semiscale Program (reference ANCR-1254, Quarterly Report January–March 1975) includes a series of blowdown heat transfer tests, the first of which was performed on May 5, 1975. Two additional tests were performed before the end of the quarter, and more will follow in the next quarter. An electrically heated core is employed in performing these tests with the Semiscale Mod-1 system. The core is comprised of 40 electric heater rods, each of which has physical dimensions (diameter and heated length) corresponding to the nuclear fuel rods to be used in the LOFT reactor.

The blowdown heat transfer tests are the first Semiscale Mod-1 experiments to make use of a heated core. The tests are designed to simulate the blowdown portion of a loss-of-coolant accident (LOCA). During each test, emphasis is placed on obtaining measured data for evaluating the core thermal behavior, specifically the rod heat transfer coefficients and the time to departure from nucleate boiling (DNB). To obtain the most useful information, the electric heater rods must provide heat flux at the rod surface which is basically the same as would be obtained from nuclear fuel rods under similar conditions. To meet this need, the power input to the rods must be programmed in a manner which will compensate for differences between the electric and nuclear rod thermal-physical properties.

The power control characteristic to simulate nucleate boiling heat transfer has been determined analytically. The post-DNB behavior depends on the actual time that DNB occurs, and this event must be determined experimentally. The appropriate heater rod power control to simulate post-DNB heat transfer will be determined from the experimental results of the remaining tests in the series.

1.1 Electric Rod Description

The Semiscale Mod-1 electric rod contains a helical wound constantan heater filament wire surrounded by packed boron nitride (BN) and enclosed in a stainless steel sheath. The rod is illustrated in Figure I-1, and a cross section view is shown in Figure I-2. The rod is manufactured using a dual stainless steel composite sheath. The inner sheath is creased inward at four equally spaced circumferential locations along the total length. A 0.025 in. diameter stainless steel sheathed thermocouple assembly is laser-beam tack-welded into each crease at a selected axial rod location. The outer sheath is then assembled over the creased sheath and the composite sheath assembly is redrawn. Both low and high power rods are provided. The only significant differences between high power and low power heater rods are the size of the filament wire and the number of windings of the filament per unit length of rod.

Fig. I-1 Diagram of the Mod-1 electrical heater.

Fig. I-2 Mod-1 electrical heater rod cross section.

1.2 Nuclear Fuel Rod Simulation

Physical property differences and a lower operating temperature limit for the heat-producing portion of the electric rods result in thermal performance which is different from that of a nuclear fuel rod. The boron nitride which is used as the electrical insulator represents 2/3 of the volume of the electric rod. While the density-specific heat product of the BN is approximately the same as that of UO_2 , the thermal conductivity of BN is about 10 times larger than that of UO_2 . The larger BN thermal diffusivity results in a lower electric rod center temperature (approximately 1,350°F) at a steady state operating condition than that which occurs in the nuclear rod (approximately 4,900°F) for the same power density (19.2 kW/ft) and surface boundary conditions. Due to the larger steady state temperature gradient, a nuclear rod contains more stored energy than a corresponding electric rod. Therefore, in order for the Mod-1 rods to adequately simulate a nuclear core, the transient electric power must be programmed in a manner that will compensate for the differences in stored energies.

The objective of electric rod application in the Mod-1 blowdown heat transfer test series is to reproduce the thermal behavior (surface temperature and heat flux) of a nuclear fuel rod from the inception of blowdown to the inception of DNB. This can be accomplished in two steps: first, by determining the behavior of a nuclear rod throughout the blowdown under the assumption that it remain in nucleate boiling for all time; second, by determining a power-versus-time profile which, when applied to the filament, reproduces the nuclear rod behavior in the electric heater rod. The following procedure was employed to determine the appropriate power-versus-time characteristic for the electric heater rods:

- (1) The heat fluxes from the electrically heated rod and the LOFT nuclear rod were calculated considering both rods in the same depressurizing system. To perform analytical calculations of the nuclear and electric rod behavior, one-dimensional conduction models were formulated for the rods. These models are illustrated in Figures I-3 and I-4. In the electric rod, the helical wound wire is approximated as a cylinder, and the protrusions of the inner cladding into the BN at the thermocouple locations (see Figure I-2) are ignored.
- (2) The mechanism of heat transfer from the rods was assumed to be by nucleate boiling during the entire transient depressurization. Flow nucleate boiling, as a first approximation, is insensitive to flow parameters. This provides for easy inclusion of a boiling boundary correlation into the heat diffusion problem using the system depressurization rate without direct coupling to the hydraulics. The flow boiling correlation given by Thom^[I-1] is used to describe the heat transfer coefficient until DNB is reached:

$$Q_{\text{THOM}} = [13.89 \Delta T_{\text{sat}} \exp(p/1250)]^2$$

where p is pressure in psia and ΔT_{sat} is the rod surface temperature minus the saturation temperature in °F. It was recognized that DNB will likely occur early in the blowdown, but the assumption of nucleate boiling during the entire blowdown satisfies the need of matching the electric rod performance to that of a nuclear rod until DNB, no matter when it occurs. An appropriate power transient for the electric rod was then defined so as to produce the same nucleate boiling transient heat flux from a high powered electric rod at the peak power location as would occur from a high power nuclear rod at the same location.

ANC-A-4232

Fig. I-3 One-dimensional LOFT nuclear rod model.

1.3 Results of Nuclear Rod Simulation

The surface heat fluxes from the nuclear and Mod-1 rods at the peak power locations are compared in Figure I-5 with the appropriate transient electric power supplied for 100 and 75% initial steady power levels, respectively. The transient pressure and saturated fluid temperature conditions which were approximated from typical data previously obtained from the 1-1/2-loop Semiscale isothermal tests are illustrated in Figure I-6. The appropriate power versus time profiles for the electric rod to simulate a nuclear rod in nucleate boiling are shown in Figure I-7 for both 100 and 75% initial core power. The latter is included because the first three tests in the blowdown heat transfer series have been performed at this reduced power level.

Fig. I-4 One-dimensional Mod-1 heater rod model.

Fig. I-5 Comparison of Mod-1 and LOFT heat fluxes and heat transfer coefficient assuming nucleate boiling throughout blowdown.

Fig. I-6 Assumed core pressure and fluid temperature during blowdown.

The comparison between the nuclear and Mod-1 rod heat fluxes for the high powered rod at the peak power location (Figure I-5) is good, indicating the ability of the Mod-1 rod to model a nuclear rod at a given axial position when the heat transfer is from nucleate boiling.

1.4 Post-DNB Rod Performance

The thermal responses of both the nuclear and electric heater rods were analyzed to obtain a comparison of post-DNB surface temperature transients for a set of hypothetical

Fig. I-7 Transient electrical power factor to obtain nuclear rod simulation until a DNB.

post-DNB heat transfer coefficients as shown in Table I-I and for various times to DNB. These heat transfer coefficients were selected to be conservative in relation to the severity of the thermal transient but are believed to provide a good test of the analysis. The power versus time profile of the electric rod appropriate to nucleate boiling heat transfer was retained in order to estimate the need for subsequent adjustment of the power behavior after DNB.

The surface flux response is shown in Figure I-8 for 1 second and 5 seconds time to DNB. The corresponding surface temperature response is shown in Figure I-9. (The maximum electric rod surface temperature was arbitrarily limited to 1,500°F in the calculation.)

These results show that the post-DNB thermal response of the electric rod using the power-time profile suitable for nucleate boiling does not adequately simulate that of a nuclear rod. It has been shown (Reference I-1) that a better post-DNB simulation can be obtained by increasing the electric power for a time interval after DNB. During the remaining portion of the blowdown heat transfer test series, an experimental iteration will be performed to provide a suitable simulation of post-DNB heat transfer. The future tests, which are scheduled to be performed at 100% of rated core power rather than 75% as has been used in the first three tests, are expected to provide sufficient data for this purpose.

TABLE I-I

HEAT TRANSFER COEFFICIENT AFTER DNB

<u>Time After DNB (sec)</u>	<u>h (Btu/hr-ft²-°F)</u>
0	500
0.1	300
0.2	200
0.3	100
0.5	100
1.0	10
100	10

Fig. I-8 Comparison of Mod-1 and LOFT surface heat fluxes after DNB.

Fig. I-9 Comparison of Mod-1 and LOFT surface temperatures after DNB.

2. REFERENCE

- I-1. J. R. S. Thom et al, "Boiling in Subcooled Water During Flow Up Heated Tubes or Annuli", *Proc. Instn., Mech. Engrs.*, 180, Part 3C (1966) pp 226-246.

II. LOFT PROGRAM

L. J. Ybarrondo, Program Manager

The LOFT Program objectives were described in ANCR-1254, Quarterly Report January - March FY 1975. Major activities during the final quarter of FY 1975 centered on LOFT core flow studies. This effort is described in detail in this section. Also included is a discussion on the design, characterization, and fabrication of LOFT fuel modules.

1. LOFT EXPERIMENT PLANNING AND TEST EVALUATION

R. K. Fujita

The following describes the results of the LOFT core flow study [II-1] performed to select the combinations of primary coolant mass flow rates and coolant temperatures which will result in a LOFT loss-of-coolant experiment (LOCE) performance most typical of large pressurized water reactors (LPWRs) with respect to time of critical heat flux (CHF) occurrence after blowdown initiation. The section of primary coolant mass flow rates and coolant temperatures as variables in this study were based on a generally used index of the power capability of water reactors, i.e., the ratio of the predicted CHF to the reactor heat flux, commonly referred to as the departure from nucleate boiling ratio (DNBR).

$$\text{DNBR} = \frac{q''_{\text{CHF}}}{q''_{\text{local}}} \quad (1)$$

The DNBR relation shown in Equation (1) is mainly a function of mass velocity, system pressure, fluid temperature (subcooling), system geometry, fluid quality, and the local surface heat flux. For example, the Westinghouse CHF correlation [II-2] given in Equation (2) is seen to be a function of pressure, quality, geometry, and mass velocity.

$$\begin{aligned} q''_{\text{CHF}} = & (2.002 - 0.0004302 P) + (0.1722 - 0.0000984P) \\ & \times \exp[(18.77 - 0.004129) X] \\ & \times [(0.1484 - 1.596 X + 0.1729 X|X|) (G/10^6) + 1.037] \\ & \times (1.157 - 0.869 X) \\ & \times [0.2664 + 0.8357 \exp(-3.151 De)] \\ & \times [0.8258 + 0.000794 (h_{\text{sat}} - h_{\text{in}})] \end{aligned} \quad (2)$$

Since the construction of the LOFT facility is nearly completed, the geometric parameters that may influence the core response are essentially fixed. Also the LOFT initial core power level and upper plenum pressure have been set. Therefore, when analyzing Equation (2), it can be seen that the main controllable variables that can affect the behavior of the LOFT core are the coolant mass flow rates and inlet fluid temperatures.

An analytical investigation performed by Aerojet Nuclear Company^[II-3] in 1970 concluded that "LOFT system temperature rises that are approximately close to those of LPWRs (about 65°F) provided the most favorable comparison in core behavior". The investigation also included an analysis of steady state DNBR in which it was concluded that "the most probable steady state DNBR exceeds 1.76 for all runs". The DNBR value of 1.76 was based on an early version of the Babcock and Wilcox-2 CHF correlation and a pre-LOCE primary coolant mass flow rate of 2.15×10^6 lbm/hr.

Subsequent DNBR studies performed by Aerojet Nuclear Company^[II-4] and Exxon Nuclear Company have yielded lower values for the LOFT DNBR than that reported earlier. Also refinements of the RELAP^[II-5] computer code and recent plant modifications have made this analysis necessary to provide information for comparison with results of the previous studies.

The time to reach CHF has been recognized to be an important indicator to determine the effects of all phenomena associated with LOCA conditions for the LOFT and LPWR core. There is uncertainty in the ability to predict the time at which CHF occurs in a core during a LOCA transient due to a lack of applicable transient CHF data.

Computer codes such as RELAP4^[II-5] which predict the thermal-hydraulic response of nuclear reactor systems during a LOCA usually show that CHF occurs almost instantaneously, i.e., 0.10 to 0.50 second, for large break sizes. These computer codes use steady state rather than transient CHF correlations to calculate CHF, but recent experimental tests^[II-6,-7] have shown that CHF may be delayed from 8 to 11 seconds after the start of the LOCA; whereas, more recent tests performed in the Semiscale facility indicate that the very short times, i.e., less than 1 second, to CHF may be more realistic in a fuel bundle.

The additional pre-CHF heat transfer can have a significant effect on the peak cladding surface temperatures. This effect of additional pre-CHF heat transfer on cladding temperatures as demonstrated in the typicality evaluation performed by Westinghouse Electric Corporation is shown in Figure II-1. The realistic case with CHF occurring at 15 seconds has a lower clad temperature at that time than the conservative case with CHF occurring at 0.10 second.

The delay in CHF for the realistic case resulted in a peak clad temperature of approximately 900°F less than the conservative case. The Westinghouse predictions obtained for the conservative case were based on a type of analysis used in Safety Analysis Report evaluations. A "best estimate" type analysis was used for the realistic case predictions.

A comparative analysis to determine the time to CHF in the LOFT and LPWR core, subjected to various plenum conditions, is presented in this section. The models used to predict the thermal-hydraulic responses of the LOFT/LPWR system and core behavior are also presented. In addition, the results of a sensitivity study to determine effects on time to CHF caused by variations of initial steady state core conditions are included.

Fig. II-1 Westinghouse conservative and realistic predictions of the LOFT hot spot cladding temperatures.

1.1 LPWR System Model Description

A schematic of RELAP4 LPWR system model is shown on Figure II-2. The LPWR system model consists of 44 control volumes, 59 flow junctions, and 29 heat conducting structures. The three unbroken loops are lumped into a single operating loop and the broken loop represents a double-ended break in the cold leg piping.

The LPWR core region was modeled as two parallel channels. One channel, consisting of three control volumes, represented the center fuel assembly and the other channel, also consisting of three volumes, represented the remainder of the core. Cross-flow between the two channels was not modeled.

The axial power profile is a chopped cosine, with an axial peak-to-average ratio of 1.72, resulting in power fractions of 0.266, 0.460, and 0.266 for the main core sections; 0.0018, 0.0031, and 0.0018 for the hot assembly core sections.

Twenty-nine heat conducting structures were used to model the heat transfer in the primary coolant system (PCS), such as the steam generator tubes, downcomer walls, PCS piping, core region, and the reactor vessel. Figure II-3 illustrates the locations of the heat conducting slabs with reference to the control volumes. Temperature dependent thermal conductivities, volumetric heat capacities, and linear expansion coefficients were input to the RELAP4 code in tabular form.

Fig. II-2 Schematic of the RELAP4 LPWR system model.

Fig. II-3 Heat conducting structures used in the RELAP4 LPWR system model.

The primary coolant circulating pumps are described by Westinghouse Electric Corporation pump head and torque data built into the RELAP4 code. A multiplier dependent on void fraction to correct for two-phase fluid conditions is applied to the pump head data. Two-phase corrections are not made to the pump torque data and no reverse flow resistance is used for the pumps.

Different choking options internal to the RELAP4 code are utilized in the models to describe the flow at all the flow junctions. All junctions related to the broken loop use an option which utilizes the Moody choking model to determine the flow during saturated choking conditions and the Fauske model to predict choking during subcooled flow. The Moody or sonic choking model is used for saturated flow calculations while subcooled choking is not allowed for the remaining junctions. A discharge coefficient of 0.60 was applied to correct the calculated flow through the break plane junctions.

The flow of feedwater and steam through the secondary side of each steam generator was maintained at a constant flow rate of 4,183 lbm/sec during the period of the transient investigated in this study, i.e., 2 seconds of the blowdown. Normally the steam generator throttle valves are closed 5 seconds after the transient begins.

The LPWR primary system blowdown was initiated when the valves at the break junctions, Junctions 37 and 38, are opened simultaneously with the closing of the valve located at Junction 32. Junction 32 represents the actual pipe break plane. The tripping of these valves at 0.010 second simulates the leaks into the containment volume caused by the double-ended pipe break. Also at this time, the reactor control rods were inserted into the core (reactor scram), and the power to the PCS circulating pumps were tripped off.

1.2 LOFT System Model

The RELAP4/003 computer code, Version 066, was used to model the LOFT test system. A schematic of the LOFT system model simulating a double-ended inlet piping break is shown on Figure II-4. In general, the RELAP4 modeling of the LOFT system was very similar to the modeling of the LPWR system. Major modeling differences are that fewer volumes were used to model the LOFT steam generator and the reactor core. Also the broken loop was modeled with steam generator and pump simulators rather than with the actual components.

The locations of the heat-conducting structures in the LOFT model are shown on Figure II-5. Heat transfer from the PCS piping was not modeled for the LOFT system.

The LOFT core region was modeled with three control volumes and three core heat slabs. The LOFT core has an initial power of 55.7 MW and an axial peak-to-average power ratio of 1.61. A skewed axial power profile resulted in power fractions of 0.388, 0.478, and 0.139.

Fig. II-4 Schematic of the RELAP4 LOFT system model.

Fig. II-5 Schematic of the RELAP4 LOFT system model heat structure.

The LOFT PCS circulating pumps were described by data supplied by Byron Jackson Pump Division^[a]. The pump head and torque data were corrected for two-phase fluid conditions by multipliers dependent upon the fluid void fraction.

The compressible, single-stream flow equation option with a one-dimensional momentum flux term was used to calculate the flow through all junctions except during periods of choked flow.

Moody or sonic choking is used at all the flow junctions for calculating flow choking under saturated fluid conditions. No subcooled choking was allowed at any of the junctions. A discharge coefficient of 0.60 was used to correct the calculated choked flow through the break plane junctions.

The LOFT quick-opening valves in the broken loop open at 0.005 second to begin the blowdown of the PCS. Also at this time the reactor is scrammed and the power to the PCS circulating pumps are tripped off.

[a] Pump Quadrant Curves SPERT 3 Units, Byron Jackson Serial No. 332476; Byron Jackson Pump Division, July 21, 1971.

1.3 Hot Pin Models

The RELAP4/003, Version 066 computer code was used to model the "hottest" fuel pin in the LOFT core and in a typical LPWR core. The computer models were used to predict the thermal response of the fuel pin during a LOCA transient. In particular, the cladding temperature response at the core hot spot was assumed to be the major indicator to determine the effects of all phenomena associated with LOCA conditions for the LOFT and LPWR cores.

Each hot pin model represents the hottest fuel pin and its adjacent flow channel within the center fuel assembly of the core. The models consist of 14 control volumes, 13 flow junctions, and 12 core heat slabs.

The normalized power generation rates for the LOFT and LPWR cores were calculated by the reactor kinetics model in the RELAP4 computer code. The power generation rates are observed to drop very rapidly and then decrease slightly during the remainder of the transient. The axial power profile used for each pin model is shown in Figure II-6. The LPWR has a chopped cosine profile while LOFT has a slightly skewed cosine profile.

Heat conduction from the fuel pin was based on the solution of the one-dimensional heat conduction equation. The thermal model of each fuel pin consists of six equal thickness radial regions in the UO_2 fuel, two regions in the gas gap, and eight in the Zr-4 cladding. Radially, all regions are connected by conductances based on temperature dependent material properties identical to those used in the LOFT and LPWR system models. A gap expansion model was used to modify the thermal conductivity of the gap between the UO_2 fuel pellets and the Zr-4 cladding. This gap thermal conductivity correction was used to account for the expansion of the adjacent regions but does not provide for any volume or surface area variations.

The flow area (0.00123 ft^3) of each junction and core volume was the same for both hot pin models. The incompressible flow energy equation without Bernoulli or momentum flux terms was used to calculate the flow rates at the core inlet and exit junctions. The compressible single stream energy equation was used for flow calculations at the remaining core junctions. Subcooled choking was not allowed at any junction, but when saturated choking was predicted, the Moody choking model was utilized.

The input data boundary conditions required by the hot pin models were obtained from the RELAP4 LOFT/LPWR system models. The RELAP4 system model provided the upper and lower plenum conditions and the power generation rate in the fuel for the hot fuel pin models.

1.4 Analysis of the LOFT and LPWR Core Behavior During a Simulated Loss-of-Coolant Accident (LOCA)

The analysis used to determine the similarity of the LOFT and LPWR core response during a postulated LOCA is presented in this section. The LOFT core flow study was performed in two parts as shown on the flow diagram in Figure II-7.

Fig. II-6 Normalized axial power distributions in the RELAP4 LOFT/LPWR hot pin models.

Fig. II-7 Flow schematic for the LOFT core flow study.

The first part of the analysis was directed towards an approach recommended in a study performed at the University of California to compare the time to reach CHF in the LOFT and LPWR core when subjected to identical plenum conditions. This similarity study determined that an analysis of core behavior under identical plenum conditions would be more meaningful than using dissimilar boundary conditions which may give the same time to CHF for only a given or unique accident sequence.

The plenum conditions utilized during this phase of the study were generated by a RELAP4 model of a representative LPWR. The Westinghouse designed Trojan power plant was used as the standard for the representative LPWR. Initial conditions for the LOFT/LPWR analysis are given in Table II-I.

RELAP4 hot pin models were used to determine in complete detail the thermal response of the LOFT and LPWR core when subjected to the boundary conditions obtained from the RELAP4 LPWR system model. Each hot pin model was run twice with the same boundary conditions. One run utilized the standard RELAP4 steady state CHF correlations to predict the time to reach CHF. The second run used a modification of the RELAP4 CHF correlations, i.e., the code defaulted to the Griffith-Walkush transient counterflow CHF correlation at low flow rates.

TABLE II-I

RELAP4 LOFT/LPWR INITIAL STEADY STATE SYSTEM CONDITIONS

System Model	Initial Conditions				
	Upper Plenum Temperature (°F)	Temperature Difference Across Core (°F)	Upper Plenum Pressure (psia)	Pressure Difference Across Core (psid)	Core Mass Velocity x 10 ⁶ (lbm/hr-ft ²)
LPWR	618.3	65.7	2,251.6	41.8 ^[a]	2.59 ^[b]
LOFT	616.1	71.7	2,257.0	7.8	1.15 ^[c]
LOFT	610.0	68.5	2,257.0	7.8	1.15 ^[c]
LOFT	597.1	41.0	2,266.5	16.6	1.92 ^[d]
LOFT	610.0	40.0	2,266.5	16.6	1.92 ^[d]

[a] The Trojan core pressure drop has been reduced from the original pressure drop of 41.8 psid to 24.6 psid and shown not to exhibit significant differences in LOCA blowdown from the original core pressure drop.

[b] Based on core flow area of 51.2 ft².

[c] LOFT 'Lo' flow; 95% of PCS flow (2.15×10^6 lbm/hr); core flow area is 1.78 ft².

[d] LOFT 'Hi' flow; 95% of PCS flow (3.60×10^6 lbm/hr); core flow area is 1.78 ft².

In Part I of the analysis, the LPWR plenum conditions were applied to the LOFT hot fuel pin model without changing the initial steady state flow channel conditions of the hot pin to match the LPWR conditions at the beginning of the transient. The initial pressure drop across the core for the LPWR system is 42 psid while LOFT has core pressure drops of 8 to 17 psid for core flow rates of 2.04×10^6 and 3.42×10^6 lbm/hr, respectively.

The initial fuel pin reference conditions must match the input boundary conditions at the beginning of the transient in order to provide a correct calculation of the core mass flow rate during the transient. Since the LOFT and LPWR have different initial core pressure drops during steady state operations, it is not possible to match initial boundary conditions. Therefore, a sensitivity study was performed to determine the effects on core behavior during a transient when the initial reference and boundary conditions do not match.

The sensitivity study utilized a LPWR hot fuel pin modeled by the BACTRAC3 computer code, which is described in a LOFT core flow study^[II-1]. The BACTRAC3 model of the LPWR fuel pin is identical in geometry, initial fluid conditions, and axial power profile to the RELAP4 LPWR hot fuel pin model. The study was performed by applying the identical transient boundary conditions and initial conditions to the hot pin model for each successive run. The only variable between runs was the Fanning friction factor which was used by the code to calculate the frictional pressure drop.

The second part of the analysis consisted of applying different LOFT system boundary conditions to the LOFT hot pin model. The system boundary conditions were obtained from a RELAP4 model of the LOFT reactor system described in Section II-1.2. Four different sets of plenum conditions were generated to use as boundary conditions for the LOFT hot pin model. The initial conditions of the LOFT system models are given in Table II-I. The same parameters were compared as in Part I to determine any similarity in the LOFT/LPWR clad temperature responses.

The LOFT plenum conditions, Table II-I, represent a bracketing of possible LOFT operating conditions. The 'Lo' flow case (2.15×10^6 lbm/hr through the PCS loop) is based on the flow necessary to provide a typical LPWR core temperature rise of approximately 65°F. This low flow is also the minimum flow which will give a DNBR of 1.15 for the LOFT core. The 'Hi' flow case (3.6×10^6 lbm/hr through the PCS loop) is the LOFT design flow used for normal steady state operating conditions. The hot leg temperatures (upper plenum temperatures) are those to be utilized during actual tests for their respective flow rates.

Table II-I lists three different hot leg fluid temperatures which were used as initial conditions for the LOFT system model. The BACTRAC 3 model of the LPWR hot fuel pin was also used to study the effect on core thermal response due to variations of only the hot leg fluid temperatures. This portion of the study was used to determine if the time to CCHF

is sensitive to different hot leg fluid temperatures. The runs were performed similarly to the previous BACTRAC 3 study with the exception of varying the exit fluid temperature and using a constant value for the Fanning friction factor for each run.

1.5 Discussion of Results

The results of the analysis to determine the comparative thermal-hydraulic responses of the LOFT/LPWR systems and hot fuel pin during a system blowdown are presented in this section. The significant results are summarized in the following subsections and in Table II-II.

1.5.1 LOFT/LPWR Plenum Conditions. The RELAP4 models of the LOFT and LPWR system were used to generate five different sets of plenum conditions to use as boundary conditions in the hot pin models. The initial conditions for each system model were presented previously in Table II-I.

A comparison of the four LOFT system plenum conditions against the representative LPWR plenum conditions was made to observe the system effects when varying initial conditions in the LOFT system models. A comparison of the LOFT/LPWR upper plenum pressure histories is shown in Figure II-8. All the LOFT models show the same general trends regardless of the initial core flow rate. The differences in the pressure histories particularly during subcooled blowdown are mainly a result of the initial steady state hot leg fluid temperatures. Decreasing the hot leg temperature results in an increase in the duration of the subcooled portion of the blowdown and, consequently, in a lower final pressure at the end of the subcooled blowdown.

The upper plenum pressures for the two cases that have the same hot leg fluid temperatures (610°F), but different core flow rates, are very similar for the first second of the blowdown. The 'Lo' flow case depressurizes at a slightly faster rate than the 'Hi' flow case, and at the end of 2 seconds the upper plenum pressure differs by 6% (see Figure II-8).

The LOFT 'Hi' flow (597°F) system response differs considerably from the LPWR system behavior (Figure II-2). The LOFT upper plenum pressure is approximately 250 psid lower than that of the LPWR at the completion of the subcooled blowdown. Applying the LOFT 'Hi' flow (597°F) and LPWR plenum conditions as boundary conditions to the LOFT hot pin model will show the effects on the fuel pin behavior which is subjected to two very dissimilar sets of boundary conditions.

The LPWR and LOFT 'Lo' flow (616°F) plenum conditions compare favorably with each other during the observed blowdown period. The variations in the LOFT and LPWR plenum conditions are shown in Figures II-9 and II-10. LOFT depressurized to a lower pressure during subcooled blowdown because its initial hot leg temperature (616°F) is lower than that of the LPWR temperature of 618°F. Once subcooled blowdown was concluded, the response of each system followed the same general trends.

TABLE II-II

SUMMARY OF RESULTS FOR LOFT CORE FLOW STUDY

Case (Identification Code)	INITIAL CONDITIONS				CHF Correlations	RESULTS	
	Core Flow (lbm/hr)	Plenum Temperatures (°F)				Time to CHF (sec)	CHF location from bottom of Pin (ft)
		Lower	Upper	ΔT Core			
LPWR (TPIN-6)	1.28 x 10 ⁸	553	618	66	B&W-2; Barnett	0.28	8 to 10 ^[c]
LPWR (TPIN-3)	1.28 x 10 ⁸	553	618	66	Griffith-Walkush ^[b]	0.28	8 to 10
LOFT ^[a] (LPIN-6)	2.04 x 10 ⁸	553	618	66	B&W-2; Barnett	1.25	3.2 to 4.6 ^[d]
LOFT ^[a] (LPIN-3)	2.04 x 10 ⁶	553	618	66	Griffith-Walkush	1.22	2.3 to 4.1
LOFT (LPIN-10)	2.04 x 10 ⁶ (Lo Flow)	544	616	72	B&W-2; Barnett	0.23	1.4 to 4.1
LOFT (LPIN-8)	2.04 x 10 ⁶ (Lo Flow)	544	616	72	Griffith-Walkush	0.31	1.8 to 3.7
LOFT (LPIN-14)	2.04 x 10 ⁶ (Lo Flow)	541	610	69	B&W-2; Barnett	0.22	1.8 to 2.8
LOFT (LPIN-16)	2.04 x 10 ⁶ (Lo Flow)	541	610	69	Griffith-Walkush	0.28	1.8 to 2.3

TABLE II-II (contd.)

Case (Identification Code)	INITIAL CONDITIONS				CHF Correlations	RESULTS	
	Core Flow (lbm/hr)	Plenum Temperatures (°F)				Time to CHF (sec)	CHF location from bottom of pin (ft)
		Lower	Upper	ΔT Core			
LOFT (LPIN-17)	3.42 x 10 ⁶ (Hi Flow)	570	610	40	E&W-2; Barnett	0.34	2.3 to 3.7
LOFT (LPIN-19)	3.42 x 10 ⁶ (Hi Flow)	570	610	40	Griffith-Walkush	0.32	2.3 to 3.7
LOFT (LPIN-13)	3.42 x 10 ⁶ (Hi Flow)	556	597	41	3&W-2; Barnett	1.0	0.9 to 1.4
LOFT (LPIN-11)	3.42 x 10 ⁶ (Hi Flow)	556	597	41	Griffith-Walkush	1.0	2.3 to 3.2

[a] LPWR plenum conditions used as boundary conditions.

[b] B&W-2; Barnett are used when $G > 3.0 \times 10^5$ lbm/hr-ft², Griffith-Walkush used for $G \leq 3.0 \times 10^5$ lbm/hr-ft².

[c] 12-foot LPWR core.

[d] 5.5-foot LOFT core.

Fig. II-8 Comparison of the LOFT and LPWR upper plenum pressures.

Fig. II-9 LPWR/LOFT 'Lo' flow (616°F) upper plenum pressures.

Fig. II-10 LPWR/LOFT 'Lo' flow (616°F) plenum temperatures.

The results stated above agree with the previous analysis^[II-3] that the best simulation of a LPWR by LOFT occurs when the system temperature rise approaches the LPWR value of 64°F. The higher LOFT system temperature rises of 53°F and 63°F were achieved by lowering the core flow rate to 2.69×10^6 and 2.29×10^6 lbm/hr, respectively, while maintaining a hot leg temperature of 610°F. Therefore, the LOFT system plenum conditions with hot leg temperatures of 616°F and 610°F should give similar system responses as the LPWR. The LOFT 'Hi' flow (610°F) plenum conditions are also included in this category of providing similar LPWR system responses as no significant differences exist from the 'Lo' flow (610°F) cases.

1.5.2 Part 1 of Analysis. This phase of the analysis consisted of applying the LPWR plenum conditions to both the LOFT and LPWR hot fuel pin models to determine the time to reach CHF and CHF location on the fuel pin. CHF was assumed to have occurred when the calculated surface heat flux of a core slab equaled or exceeded the predicted CHF for the slab. CHF is characterized by a sudden drop in the nucleate boiling heat transfer coefficient due to a change of heat transfer mechanism and is indicated by a temperature excursion of the heated surface. Each hot pin model was run twice, once with the standard RELAP4 CHF correlations and again with the Griffith-Walkush CHF correlations. Reference II-1 contains a detailed description of these CHF correlations.

The results of inputting the LPWR plenum conditions as boundary conditions to the LOFT and LPWR hot pin models are shown in Figure II-11. The cladding temperature histories of the earliest LOFT and LPWR heat slabs to reach CHF are shown in Figure II-12. Figure II-12 indicates that the CHF, based on the standard RELAP4 steady state CHF correlations, has been reached in 0.18 second for the LPWR fuel pin and 1.23 seconds for the LOFT fuel pin. LOFT benefited from nearly an additional second of core cooling before CHF was achieved as compared to the LPWR core.

The use of the Griffith-Walkush CHF correlation produced nearly identical CHF predictions as compared to those predicted by the standard RELAP4 CHF correlations (Figure II-13). The Griffith-Walkush correlation tended to give CHF values much higher than the correlations used prior to and after the selection of their correlation by the RELAP4 heat transfer selection logic. This prevented the heat transfer logic from oscillating between the nucleate boiling and post-CHF heat transfer correlations. Therefore, their correlation removed most of the oscillations observed in the heat transfer coefficients when using the standard RELAP4 CHF correlation (see Figure II-14). Also the oscillations of the LPWR clad surface temperatures were removed when the Griffith-Walkush CHF correlation was used (see Figure II-15).

The standard RELAP4 CHF correlations predict CHF conditions are met many times before CHF is actually maintained. Figure II-14 gives a good indication of when CHF is reached by observing the rapid drop in magnitude of the heat transfer coefficients. The post-CHF heat transfer coefficients are very small as compared to the nucleate boiling heat transfer coefficients. The Griffith-Walkush correlation resulted in a faster voiding of the flow channel; i.e., the quality approached 100% quicker than predicted by the standard model. The combined effects of slightly higher heat transfer coefficients and faster voiding of the flow channel as predicted by the Griffith-Walkush correlation resulted in very similar clad temperature responses compared to the standard RELAP4 prediction (Figure II-15).

The prediction of similar times to reach CHF by the Griffith-Walkush transient CHF correlation and the RELAP4 steady state CHF correlations agrees with results observed experimentally by Cermak^[II-8]. They concluded that the inception of transient CHF during blowdown can be predicted on the basis of steady state data. They predicted the time to CHF with the Westinghouse-3 steady state CHF correlation and then compared the prediction with the actual observed time to CHF. The results showed a slightly conservative prediction in the time to reach CHF in the rod bundle.

A rewetting of the LOFT fuel pin is predicted by the RELAP4 code (see Figure II-12). CHF was reached at 0.22 second which caused a sharp increase in the cladding temperature; but at 0.45 second, the cladding temperature leveled off and began to decrease until it reached the pre-CHF temperature. The prediction of rewetting the fuel pin by the RELAP4 code is reasonable because of a combination of localized conditions, such as low clad surface temperatures, moderate mass flow rates, low fluid qualities, and a decreasing power generation rate. The combined effect of these location conditions was sufficient to cause a change in the heat transfer mechanism, i.e., a return to nucleate boiling from the film boiling regime.

Fig. II-11 Part 1 - time to reach critical heat flux for the LOFT and LPWR hot pin models (LPWR plenum conditions).

Tong^[II-9] stated that after CHF has occurred, and under proper conditions, the core heat flux may be significantly reduced so that nucleate boiling conditions might be restored before damage to the fuel pin occurs. A condition which must be met for this to occur is that the reactor must be shut down promptly at the start of the LOCA, i.e., reactor scrammed. Also during stable film boiling, the fuel pin surface can be rewetted only if the clad temperature is below the Leidenfrost temperature.

Similarly, Holmstrom^[II-10] also stated that rewetting of the cladding can only occur when the clad temperature is below the Leidenfrost temperature, i.e., 300°F above the saturation temperature. The rewetting temperatures at different system pressures are shown in Figure II-16. Figure II-12 shows that when rewetting of the LOFT fuel pin begins, the clad temperature is less than 800°F. The system pressure is approximately 1,650 psia at the time rewet begins. Since the clad temperature is below the 910°F Leidenfrost temperature predicted by Holmstrom, rewetting of the fuel pin is a definite possibility.

Fig. II-12 Comparison of the LOFT/LPWR hot spot cladding temperatures (standard RELAP4 CHF correlations).

Fig. II-13 Comparison of LOFT/LPWR hot spot cladding temperatures (Griffith-Walkush CHF correlations).

Fig. II-14 Comparison of heat transfer coefficients and fluid qualities calculated by the standard RELAP4 and modified RELAP4 (Griffith-Walkush) heat transfer logic.

Fig. II-15 Comparison of cladding surface temperatures predicted by the standard and modified (Griffith-Walkush) RELAP4 heat transfer logic.

Fig. 11-16 The variation in rewetting temperatures at different system pressures.

1.5.3 Part 2 of the Core Flow Analysis - Dissimilar LOFT Boundary Conditions. The second part of the core flow analysis was performed similarly to the first part except different boundary conditions were applied to the LOFT hot pin model. Four different sets of LOFT plenum conditions were used in the hot pin model and the results of the pin behavior were compared for similarity with the representative LPWR pin. The initial conditions for the LOFT system models used to generate the four different sets of plenum conditions were discussed previously.

A comparison of the LOFT and LPWR hot spot cladding temperatures showed remarkable agreement in time-to-CHF for all cases with the exception of the 'Hi' flow (597°F) case; refer to Figures II-17 through II-20. Figure II-21 shows the time each core slab reached CHF. This comparison illustrates that the initial LOFT core flow rate does not affect the time for the pin to reach CHF. The time-to-CHF appears to be highly dependent on the initial hot leg fluid temperature.

Comparison of clad temperatures of the LOFT hot pin with LPWR and LOFT 'Hi' flow (597°F) plenum conditions (Figures II-12 and II-20, respectively) show similar trends. They both have been rewetted after the first entry into the CHF mode which resulted in a delay in temperature excursion of approximately 0.8 second. It is assumed the same phenomena responsible for rewetting the LOFT fuel pin subjected to the LPWR plenum condition was also responsible for rewetting the pin with the LOFT 'Hi' flow (597°F) boundary conditions (see Section II-1.5.2).

LOFT/LPWR core similarity with respect to time-to-CHF is achieved when the initial hot leg fluid temperatures in LOFT (610°F to 616°F) are close to a LPWR (618°F). The three different LOFT hot leg fluid temperatures used in this analysis (616, 610, and 597°F) cover the expected temperature ranges to be used in the LOFT test series, so the comparison made is assumed to be adequate. The time-to-CHF observed in all runs was less than 0.5 second with the exception of the runs in which rewetting of the fuel pin was predicted to have occurred. A recent Semiscale test^[II-11] also showed that the time-to-CHF occurred shortly after the start of the transient, i.e., about 0.5 second.

Fig. II-17 Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations).

Fig. II-18 Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations).

Fig. II-19 Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations).

Fig. II-20 Comparison of LOFT/LPWR hot spot clad temperatures (standard RELAP4 CHF correlations).

The LPWR core reached CHF first near the upper portion of the core (8 to 10 ft) while LOFT first reached CHF near the core midpoint (1.50 to 3.5 ft). The locations of the first heat slab to reach CHF is dependent upon the respective axial power profile. The LOFT axial power profile is skewed at the upper portion of the core so, consequently, the power density is greater in the lower core sections.

1.5.4 BACTRAC 3 Sensitivity Study of the LPWR Hot Fuel Pin. In the previous sections (II-1.5.2 and -1.5.3) it was observed that the LOFT core behavior differed considerably from a LPWR when the LPWR and LOFT 'Hi' flow (597°F) plenum conditions were applied to the LOFT hot fuel pin. The BACTRAC 3 model of the LPWR hot fuel pin was utilized to determine if variations in initial core pressure differentials and hot leg fluid temperatures would be responsible for the nontypical LOFT core response.

Reducing the initial pressure drop across the core from 38.7 to 12.2 psid resulted in a noticeable difference in time to reach CHF (see Table II-III). Lowering the initial core pressure drop decreases the time required for the core to reach CHF. The BACTRAC 3 results show that failure to match initial pin reference conditions with the boundary conditions at the beginning of the transient will affect the clad temperature response during the ensuing transient. Inspection of Figure II-12 shows that this trend occurred for the RELAP4 predictions of the LOFT/LPWR clad temperature response when the rewetting of the LOFT fuel pin was ignored.

Fig. II-21 Part 2 - Time and location of critical heat flux in the LOFT and LPWR hot pin (dissimilar boundary conditions).

The predictions obtained from the RELAP4 and BACTRAC 3 codes are different with respect to time to CHF (see Tables II-II and II-III). This is to be expected because of the differences between the codes, i.e., CHF correlations, lack of a conduction model in BACTRAC 3, two-phase pressure drop calculations, etc. RELAP4 has a one-dimensional heat conduction model to thermally represent the fuel pin. During the blowdown, the code predicted CHF occurred early (approximately 0.25 second; see Figure II-12), but shortly thereafter a rewetting of the LOFT fuel pin was predicted to have taken place. BACTRAC 3 is unable to make such predictions because of a lack of a conduction model in the code, so once CHF was predicted the run was terminated. Consequently, the BACTRAC 3 and the RELAP4 predictions cannot be compared directly, only the general trends may be comparable.

The BACTRAC 3 results of varying the initial hot leg fluid temperatures between successive runs from 618°F to 597°F did not show any changes in the time-to-CHF predictions (refer to Table II-III). Since the BACTRAC 3 code is unable to predict a rewetting of the fuel pin, the delay in time-to-CHF observed in the RELAP4 calculations for the LOFT 'Hi' flow (597°F) case, Figure II-20, cannot be predicted by BACTRAC 3. Therefore, the BACTRAC 3 results show that the time-to-CHF is independent of the initial hot leg fluid temperature.

TABLE II-III
RESULTS OF THE BACTRAC3 SENSITIVITY STUDY

A. Variation of pressure drops across the LPWR core.

Initial Conditions:

Heat flux = 3.66×10^5 Btu/hr-ft²
Exit fluid quality = -13.8%
Upper plenum pressure = 2,251.4 psia
Upper plenum temperature = 618°F

<u>Run</u>	<u>Fanning Friction factor</u>	<u>ΔP Core (psi)</u>	<u>Time to CHF (sec)</u>	<u>Slab No.</u>
1	0.035	38.7	1.00	4
2	0.005	12.2	0.67	11

B. Variation of the initial hot leg fluid temperatures.

Initial Conditions:

Fanning friction factor = 0.035
Core pressure drop = 38.7 psi
Heat flux = 3.66×10^5 Btu/hr-ft²
Upper plenum pressure = 2,251.4 psia

<u>Run</u>	<u>Exit Quality (%)</u>	<u>Hot Leg Temperature (°F)</u>	<u>Time to CHF (sec)</u>	<u>Slab No.</u>
4	-13.8	618	1.00	4
5	-16.7	610	0.99	4
6	-21.3	597	0.97	4

A delay in the time to reach CHF will occur if a rewetting of the fuel pin, as predicted in the RELAP4 code, takes place as shown for the LOFT 'Hi' flow (597°F) case. However, should the fuel pin not be rewetted, it has been shown in Figures II-17 through II-20 that the LOFT core response is similar in all cases regardless of the initial core flow rate or hot leg fluid temperatures.

1.6 Conclusions

The conclusions resulting from the LOFT core flow study are:

- (1) The LOFT system plenum conditions follow the same general trends during a blowdown transient as those of a LPWR. The best simulation of the LPWR plenum conditions by LOFT occurs when the initial hot leg fluid temperature is close to the LPWR fluid temperature.
- (2) The LOFT and LPWR core reaches CHF at approximately the same time when the LOFT initial hot leg fluid temperatures are near the LPWR temperature (610 to 618°F).
- (3) The initial LOFT core flow rates do not affect the LOFT core behavior in relation to time to CHF. (Note that any energy balance on the core shows that the core mass flow rate and inlet coolant temperature are directly related for a specified outlet coolant temperature and core power level; i.e., the product of the core mass flow rate and coolant temperature rise across the core must be constant for a given power level.)
- (4) The RELAP4 computer code predicts the LOFT core can be rewetted under certain favorable core conditions. High core mass flow rates, low liquid qualities, low clad temperatures, and decreasing power generation rates are essential for the rewetability of the fuel pins.
- (5) The LOFT core does not respond similarly to that of a representative LPWR core when subjected to the same boundary conditions.
- (6) The LOFT core reaches CHF earliest near the midpoint of the core, while the LPWR first reached CHF in the upper portion of the core. The location of the first core heat slab reaching CHF appears to be dependent upon the axial power profile.
- (7) The BACTRAC 3 sensitivity study showed that the core response, specifically the time-to-CHF, is affected when the initial core steady state operating conditions do not match the core boundary conditions at the beginning of the transient. The study also revealed that varying only the initial hot leg fluid temperatures will not change core behavior, but due to a limitation in the BACTRAC 3 computer code, i.e., no heat conduction model, a delay in CHF could not be predicted as with the RELAP4 code.

2. LOFT FUEL MODULES DESIGN,

CHARACTERIZATION, AND FABRICATION PROGRAM

M. L. Russell

The LOFT fuel modules have evolved from a comprehensive five-year design, characterization, and fabrication program which has resulted in the accomplishment of many technical activities of interest in pressurized water reactor fuel design development and safety research. This is a summary of the highlights which include:

- (1) Determination of fundamental high-temperature reactor material properties
- (2) Design invention related to in-core instrumentation attachment
- (3) Implementation of advanced and/or unique fuel bundle characterization techniques
- (4) Implementation of improved fuel bundle fabrication techniques
- (5) Planning and execution of a multimillion-dollar design, characterization, and fabrication program for pressurized water reactor fuel.

The LOFT core design evolved the core configuration shown in Figure II-22. The test instrumentation design developed the following instrumentation to be attached to the fuel modules:

	<u>Instrumentation Item</u>	<u>Quantity</u>
(1)	Fuel rod cladding thermocouples	185
(2)	Guide tube thermocouples	11
(3)	Core inlet coolant thermocouples	17
(4)	Core outlet coolant thermocouples	21
(5)	Core liquid level detectors	3
(6)	Core inlet pressure taps	2
(7)	Core outlet and upper plenum pressure taps	4
(8)	Reactor vessel upper plenum pressure detectors	2

● Poison Rod

Fig. II-22 LOFT core layout.

Instrumentation Item	Quantity
(9) Neutron flux scan tubes	4
(10) Neutron flux detectors	4
(11) Upper structure thermocouples	10
(12) Fuel module linear motion detectors	2
(13) Core outlet flow detectors	3

In addition to composing the reactor core and providing test instrumentation locations, the fuel modules also provide:

- (1) The materials and geometry features which will assure that the important heat transfer, hydraulic, mechanical, chemical, metallurgical, and nuclear behaviors are typical of those expected in large pressurized water reactors (LPWRs) during blowdown and emergency core coolant (ECC) injections
- (2) The neutron absorber material needed, in conjunction with the boric acid system, to control the reactivity of the LOFT core
- (3) A neutron source that will allow reactor startup with the neutron multiplication within the measurement capability of the reactor startup instrumentation.

2.1 Design

The design of the LOFT fuel modules spanned the calendar years 1971, 1972, 1973, and 1974 and included comprehensive programs for mechanical design, structural and thermal-hydraulic analysis, and confirmatory testing.

To fulfill the functional and design requirements the LOFT fuel module mechanical design evolved:

- (1) Modular-type units combining the basic fuel bundle (fuel rods, skeleton, spacer, grids, guide tubes, end boxes, and control rods) with: (a) the test instrumentation detectors and cable leadouts, (b) an upper support structure extending to the reactor vessel top, (c) an instrumentation penetration assembly, and (d) the neutron source.
- (2) Three basic design configurations: (a) the highly instrumented center (15 x 15 fuel rod array) module that does not contain control rods, (b) the control (15 x 15 fuel rod array with control rod assembly) module, and (c) the corner (triangular-shaped) module.

A list of selected LOFT fuel module design values is provided in Table II-IV.

2.1.1 Fuel Modules. The LOFT fuel module arrangements are shown in Figures II-23 through II-25.

The fuel module quantities are based on two core loads plus spares as follows:

- (1) First core load consisting of fuel-module types as follows: one center (Type A), three instrumented control (Type B), two instrumented corner (Type C), one noninstrumented control (Type D), and two noninstrumented corner (Type E).

TABLE II-IV

LOFT FUEL MODULE DESIGN VALUES

Mechanical Design Values

Fuel Pellet

Fuel Material	UO ₂ Sintered Pellets
Density, % of TD	93.0 $\pm$ 1.5
Enrichment, w/o Fissile, Average	4.00 $\pm$ 0.05
Diameter, in.	0.3659 $\pm$ 0.0005
Length, in.	0.600 $\pm$ 0.025
Dish	Both Ends
Dish Volume (total) % of Pellet Volume	2.0

Fuel Rod

Active Fuel Length, in.	66.00 $\pm$ 0.20
Fuel Stack Density, % TD	91.1
Diametral Pellet-to-Clad Gap, in.	0.0075 $\pm$ 0.002
Plenum Length, gross, in.	3.00
Cladding Material	zircaloy-4
Clad OD ^[a] , in.	0.422 $\pm$ 0.002
Clad ID, in.	0.3734 $\pm$ 0.0015
Clad Thickness, nominal, in.	0.0243
Clad Thickness, minimum, in.	0.0228

Fuel Assembly

Number of Fuel Rods, square	204
Number of Fuel Rods, corner	70
Fuel Rod Array, square	15 x 15
Fuel Rod Array, corner	12 x 12 triangular
Fuel Rod Pitch, in.	0.563
Fuel Rod Separation, nominal, in.	0.141
Fuel Rod Separation, minimum, in.	0.120
Fuel Assembly Length, in.	78.568
Fuel Weight, 1b UO ₂ /1b U square assembly	511/450.3
Fuel Weight, 1b UO ₂ /1b U corner assembly	175.4/154.5

Spacers

Type	Egg-Crate - Integral Spring
Material	Inconel 718
Number per Assembly	5

TABLE II-IV (contd.)

Mechanical Integrity Assembly Average Design Burnup, MWd/MTM	30,000
Core Data	
Number of Fuel Assemblies	9 (5 square - 4 corner)
Number of Active Fuel Rods	1,300
Neutron Source	
Material	californium-252
Strength (700 days after delivery)	2×10^8 n/sec
Control Rod Clusters	
Poison Material	80Ag-15In-5Cd
Poison Rods per Cluster	20
<u>Thermal and Hydraulic Design Values</u>	
Reference Design Thermal Output, (MWt)/Btu/hr	$55/1,876 \times 10^8$
Effective Flow Rate for Heat Transfer, lb/hr	
Maximum	4.28×10^6
High	3.42×10^6
Low	2.77×10^6
System Pressure, Nominal, psig	2,253
System Pressure, Design, psig	2,208
Average Power Density, kW/l	~109.4
Maximum Heating Rate, kW/ft rated power	19.0
Average Heating Rate, kW/ft	7.49
Active Heat Transfer Surface, all JN Fuel, ft ²	789.93
Maximum Heat Flux, (@ overpower) Btu/hr-ft ²	644,830
Average Heat Flux, Btu/hr-ft ²	231,400
Maximum UO ₂ Temperature, °F rated power	4,700
Maximum Fuel Rod Surface Temperature, °F	664
MCHFR at Overpower Conditions ^[b]	1.30 (low flow)
Coolant Inlet Temperature, °F	
Maximum Flow	562
High Flow	557
Low Flow	551

TABLE II-IV (contd.)

Design Power Peaking Factors

Heat Generated in Fuel, %	97.4
Relative Fuel Assembly Power Factor plus	1.573
Local Power Peaking Factor	
Axial Power Peaking Factor	1.56
Total Nuclear Peaking Factor	2.45
Engineering Heat Flux Factor	1.03
Total Power Peaking Factor, F_q^N	2.53

[a] After etching.

[b] Based on W-3 correlation.

- (2) A lot of spare fuel modules consisting of the following types: one center (Type A), one instrumented control (Type B), one instrumented corner (Type C), and one center with zircaloy (instead of Type 304 stainless steel) guide tubes and pressurizable fuel rods (Type F).
- (3) Core reload consisting of fuel module types as follows: one center (Type A), three instrumented control (Type B), two instrumented corner (Type C), one noninstrumented control (Type D), and two noninstrumented corner (Type E). The reload fuel bundles also have pressurizable fuel rods.

The LOFT fuel modules include numerous examples of (a) mechanical design invention and/or development and (b) changes from contemporary design features, which are described as follows:

Fuel Rods

- (1) End Cap Seal Welds - The weld configuration is a proprietary design geometry developed by the LOFT fuel supplier which has a high demonstrated reliability.
- (2) Instrumented Fuel Rod Upper End Cap
- (3) Pressurizable End Cap - The reload and Type F fuel rods have a modified upper cap that allows the fuel rod to be pressurized to 900 psig maximum after fabrication of the fuel rods by a laser drilling and rewelding technique.

Fig. II-23 LOFT Type A and F fuel modules.

Fig. II-24 LOFT Type B and D fuel modules.

Fig. II-25 LOFT Type C and E fuel modules.

- (4) Thermocouple Attachment
- (5) Fuel Rod Assembly - The LOFT design configuration in conjunction with the supplier's proprietary comprehensive fabrication quality assurance programs is expected to provide exceptional reliability characteristics based on the supplier's fuel rod reliability record.

Spacer Grids

- (1) Intersection Joining Method - The LOFT spacer grids feature a TIG welded intersection joint compared to the brazed joints of contemporary designs. The technique is judged to provide design improvements in dimensional control and joint reliability although strength testing during the design development indicated that for tensile loading along the joint axis the welded joint was 40% weaker than a brazed joint. The structural design program indicated that the additional strength of the brazed construction was not necessary for achieving the design objectives.

Skeleton (subassembly consisting of spacer grids, guide tubes, and end boxes)

- (1) Triangular Skeleton Alignment - The triangular shape causes the control of twist and lean (runout from true position) to be difficult. The fabrication experience resulted in improvements to the fuel bundle assembly fixtures design and assembly instructions to achieve the triangular-shaped skeleton alignment objectives.

Control Rods

- (1) The control rod spiders were designed to be machined from forgings instead of the contemporary construction by brazing the fingers to the hub. Fabrication problems that were experienced included (a) unexpected porosity of the raw material even though satisfactory precautionary ultrasonic examination of the raw material had been performed, and (b) surface roughness above specified values. The solid spiders are considered to yield improvements in reliability compared to the contemporary construction.

Upper Support Structures (upper support structures provide lateral and holddown support for the fuel bundles)

- (1) Lateral Support - The lateral support is accomplished by achieving a close fit at three locations: the upper structure lower extremity and immediately above and below the reactor vessel nozzles. The close fit (room temperature) is approximately 0.010 inch between mating surfaces at the lower extremity and 0.055 inch between the mating surfaces adjacent to the nozzles. The lateral support is designed to provide the following features:
 - (a) Prevention of fuel module vibration during normal operation
 - (b) Maintenance of allowable stress limitations during (LOCE) subcooled blowdowns
 - (c) Noninterference during core loading.

Details of this design development are described in MPR letter report "Lateral Clearances for Fuel Module Upper Support Structures and Interfacing Components", May 24, 1972.

- (2) Holddown - The fuel bundle holddown is accomplished by a combination of upper structure dead weight and coil holddown springs. The coil spring force-deflection characteristics are designed to prevent the exceeding of allowable fuel bundle stress limitations during normal, transient, and planned LOCE plant operations.

Instrumentation Penetration. The instrumentation penetration provides the primary coolant pressure boundary penetration for the LOFT fuel module instrumentation. The LOFT design objectives are (a) high efficiency in area for making the individual cable or tube seals (140 cables and 2 tubes in a 7-inch diameter and 40 cables and 4 tubes in a 4-inch diameter), (b) high pressure boundary reliability, and (c) leakproof seals between dissimilar metals (titanium to Inconel, carbon steel to Inconel, and stainless steel to Inconel). The special design features that achieve these objectives are as follows:

- (1) High Seal-to-Area Efficiency - Each penetration uses three or more individual buttons which (a) can accommodate up to approximately 35 instrument cables, (b) are brazed to the instrument cable or tube, and (c) are welded to the instrumentation penetration body (pressure plate).
- (2) High-Pressure Boundary Reliability - Each penetration features two seals in series for increased pressure boundary reliability.

- (3) **Titanium-to-Inconel Seal** - The pressure boundary seal between the titanium-sheathed thermocouples and Inconel penetration is accomplished using an explosion bonded titanium - tantalum-Inconel sandwich material button that is brazed to the thermocouple using a titanium - zirconium - beryllium filler metal and TIG welded to the Inconel penetration.

Selection of the braze material was based on an extensive brazing material evaluation which included selection of a satisfactory brazing filler material from seven candidate materials. This evaluation is described in IITRI report "IITRI-B6112-1, Evaluation of Brazing Filler Metals for C.P. Titanium Reactor Instrumentation Components", October 20, 1971. Process demonstrations have indicated this design will be successful.

- (4) **Stainless-Steel-to-Inconel Seal** - The pressure boundary seal between the stainless-steel-sheathed instrument cables and Inconel is accomplished with a gold-nickel filler metal braze of the instrument cable to the Inconel button and TIG weld between button and penetration.
- (5) **Inconel-to-Carbon-Steel Seal** - The pressure boundary seal between the Inconel penetration and carbon steel reactor vessel head is accomplished using K-seal type mechanical seals which were chosen over O-rings because they were judged to provide increased reliability for this service condition between materials having differences in thermal expansion characteristics.

Fuel Bundle Instrumentation Attachment

- (1) **Thermocouple Attachment** - The LOFT design features titanium-sheathed thermocouples attached to the outside of zircaloy-clad fuel rods by laser welding using titanium filler wire. Titanium was selected over zircaloy because the technology for satisfactory fabrication of the small diameter zircaloy-sheathed thermocouples had not been developed. Noteworthy features of the weld design are:
- (a) **Alloy-Mixing** - The mixing between titanium and zircaloy is precisely controlled for a compromise between strength enhancement obtained from high-mixing and corrosion resistance resulting from low-mixing.
- (b) **Heat-Affected Zone** - The fragile thermocouple sheath (0.009 inch thick) heat-affected zone is precisely controlled to prevent penetration of the sheath during welding.

- (c) Weld Spacing - The individual weld nuggets, consisting of eight overlapping laser spots, are spaced at approximately 0.8-inch intervals to allow for differential thermal expansion between the zircaloy and titanium at temperatures expected during LOFT experiments.

Thermal cycling and corrosion testing of the design indicate that the weld design will provide the following expected lifetimes:

Exposure to reactor coolant conditions (650°F and 2,300 psig): 6,000 hours.

Thermal cycles:

400 to 1,100°F (1 cycle)
 400 to 1,400°F (5 cycles)
 400 to 1,500°F (5 cycles)
 400 to 1,700°F (5 cycles)

- (2) Guide Tube Instrumentation Assembly - The LOFT fuel bundle design requires attachment of thermocouples to the guide tubes, locating fixed flux detectors inside the guide tubes, and routing lower end box coolant thermocouples inside the guide tubes in a manner that will satisfy the following design conditions:

- (a) Differential Pressure Conditions - The components must withstand a differential pressure loading of 300 psi taken in the most adverse direction across each component, instrument, or mounting device, etc. The 300 psi loading requirement shall be evaluated assuming the same thermal conditions that exist at reactor full power (55 MWt).

- (b) Temperature Conditions

	<u>Rate of Change</u>	<u>Peak Temperatures</u>
Zircaloy components at normal steady state operating conditions		675°F
Components attached to support tubes (guide tubes)	20°F/sec	2,100°F (15 cycles only)
Components attached to fuel assembly end boxes	20°F/sec	900°F (15 cycles only)

- (c) Transient Termination - After five seconds at the maximum temperature, the instrumentation shall be assumed to be quenched by immersion in a saturated steam-water mixture at a pressure of 25 psia.
- (3) Instrument Cable Routing - The instrument cable routing features continuous protection of the cables from hydraulic buffeting and differential pressures. Some significant features include:
 - (a) The thermocouple cable transition from fuel rod to upper tie plate in a manner that minimizes exposure to high-velocity coolant and eliminates cable flexing during thermal cycling by machine-screw fastening the instrumented fuel rods to the upper tie plate. The fuel rod end cap and tie plate are fluted for passage of the thermocouples.
 - (b) The thermocouple routing protection across the tie plate surface provided by recessed channels and cover plates.
 - (c) Instrument cable routing protection up the end box sides provided by compact bundles of straight cables clamped at close intervals by staples which are match fit, lightly pressed against the cable bundle, and protruding staple ends welded to the end box from the outside. The development of this design included consideration of unsatisfactory experiences with crossing cables underneath the cable clamp and sharp or protruding cable clamp edges.
 - (d) Design of routing paths for the instrument cables featuring generous radii on all edges, avoiding unsatisfactory experience with sharp edges in contact with the fragile instrument cable sheaths.

2.1.2 Thermal-Hydraulic. The thermal-hydraulic design consists of a comprehensive program of analysis and testing for the following purposes:

- (1) Establish and confirm fuel module flow distribution and pressure drop characteristics
- (2) Determine fuel rod thermocouple effect on thermal-hydraulic behavior
- (3) Establish thermal-hydraulic operating limitations for the LOFT fuel modules.

Table II-V is a summary of the thermal-hydraulic analysis and testing tasks that comprise this program.

The flow distribution test, which employed a laser-Doppler velocimeter to obtain the velocity and turbulence intensity data, is considered an advance in technology since the measuring instrument does not disturb the flow being measured. The pressure drop test program has provided data for core exit pressure losses that are applicable to contemporary core exit flow geometries.

2.1.3 Structural. The structural design consists of a comprehensive program of analysis and testing for the following purposes:

- (1) Establish structural loading of fuel modules during LOCEs
- (2) Confirm the structural adequacy of the fuel module during anticipated handling, reactor operation, and LOCE conditions.

Table II-VI is a summary of the structural design analysis and testing tasks that comprise this program.

2.1.4 Miscellaneous. The total design program effort also included some design activities classified as miscellaneous. These activities are of interest since the purposes of some were to determine fundamental material properties at high temperatures. Table II-VII is a summary of these design activities.

2.2 Characterization

The fuel module characterization is designed to provide (a) traceability to raw materials, fabrication processes, inspection techniques, and participating personnel, (b) measurement of characteristics which could influence the fuel bundle response during its use in the LOFT experimental program, and (c) measurement of characteristics required to demonstrate that the components satisfy the agreed upon specifications. The laser-Doppler determination of the fuel bundle flow distribution characteristics, the fuel bundle strength test, the DNB and thermal-mixing test, the control rod drop tests, and the pressure drop test are also components of the characterization program and demonstrate the emphasis given to fuel module characterization.

2.3 Fabrication

The fabrication program is based on the quality assurance requirement of RDT Standard F2-2. All manufacturing and inspection operations are performed in accordance with detailed procedures and under facility conditions that assure cleanliness requirements are satisfied. Deviations from approved specifications of components or processes are formally reported and dispositioned by the appropriate technical agencies. The fuel bundle and upper structure fabrication programs at Exxon Nuclear Co. in Richland, Washington, and L&S Machine Co. in Latrobe, Pennsylvania, have been performed under a policy of full-time ANC Quality Division surveillance and periodic technical audits by ANC, MPR

TABLE II-V

LOFT FUEL MODULE THERMAL-HYDRAULIC DESIGN PROGRAM

Purpose	Performing Organization	Result	Documentation
A. Pressure Drop			
1. Preliminary predictions of LOFT fuel bundle pressure drop for use in LOFT primary system and fuel module upper support structure design.	ENC	The analysis predicted total fuel bundle flow loss coefficient of 6.62 (14.6 psi at 2.45×10^6 lb/hr-ft ² flow rate).	Exxon report, "Preliminary Pressure Drop Calculations for Jersey Nuclear LOFT Fuel Assembly", January 6, 1971.
2. Prediction of upper structure pressure drop for use in LOFT primary system design.	MPR	The analysis predicted upper structure pressure drop of 5.5 psi at 3.1×10^6 lb/hr-ft ² .	MPR report, "Upper Plenum Pressure Drop", MPR-316, Volume II, Appendix G, December 22, 1971.
3. Pressure drop test data from flow distribution test.	ENC (Battelle Northwest Laboratories)	Testing results indicated spacer grids flow loss coefficient to be 3.65 (5 grids) compared to the predicted value of 3.53.	Exxon report, "XN-74-53, Analysis of the LOFT Instrumented Fuel Assembly Flow Distribution Experiments", October 15, 1974.
4. Pressure drop test data from Type A & C fretting corrosion test.	ENC	Test results indicated significant pressure drop mismatch between Type A & C fuel bundles at core exit and total fuel bundle flow loss coefficient of 8.5% for Type A and 9.74 for Type E.	Exxon report, "Preliminary Report of LOFT 'C' Pressure Drop and Fretting Corrosion Test Hydraulic Data", March 20, 1974.
5. Pressure drop test data from Type B fuel module mockup and modified Type A & C fretting-corrosion test bundles.	ENC	Test results indicated acceptable improvement in core exit pressure drop mismatch of Type A, B, and C fuel bundles after the design changes to Type A & C components.	Exxon report to be published.
B. Flow Distribution			
1. Determine fuel bundle flow distribution characteristics in region of high thermal flux (inlet).	ENC (Battelle Northwest Laboratories)	Test results provided velocity and turbulence index factor maps of the center fuel bundle obtained upstream and downstream of the first three spacer grids and confirmed that the flow distribution becomes uniform from the diffuser effects of spacer grids.	Exxon report, "XN-74-53, Analysis of the LOFT Instrumented Fuel Assembly Flow Distribution Experiments", October 15, 1974, abstract presented in "Water Reactor Safety Program - Experimental and Analytical Program Activities", Monthly Report, December 1974.
2. Determine core inlet flow distribution from reactor vessel lower plenum flow tests using a 2/3-scale model.	CBI	Test results indicated that the individual fuel bundle flow velocities at the bottom of the fuel rods did not exceed 3% of average core flow velocity.	Combustion Engineering report "CEND-369, LOFT Reactor Vessel Flow Model Test Program Test Report", October 1974.
C. Thermal Mixing - Determine LOFT center fuel bundle interchannel thermal mixing coefficients.	ENC (Columbia University)	Test results confirmed that conventional bare-bundle interchannel thermal-mixing coefficient correlations are appropriate for LOFT predictions.	Exxon report, "XN-74-40, Thermal Mixing in LOFT Fuel Assemblies", December 2, 1974.
D. Departure from Nucleate Boiling (DNB)			
1. Preliminary prediction of LOFT fuel bundle DNB behavior for use in fuel bundle warranty limitations.	ENC	The analysis predicted that the LOFT low-flow limitation was 2.77×10^6 lb/hr based on the W-3 correlation prediction and a minimum allowable DNB ratio of 1.30.	Exxon report, "JN-72-14, Preliminary Thermal-Hydraulic Analysis for LOFT Fuel Assemblies", April 24, 1972.

TABLE II-V (contd.)

Purpose	Performing Organization	Result	Documentation
D. Departure from Nucleate Boiling (contd.)			
2. DNB test data to confirm that the LOFT fuel bundle with thermocouples provided the same DNB behavior as fuel bundles without thermocouples.	ENC (Columbia University)	Test results indicated that a trend existed in the subcooled region that departed (adversely) from the W-3 correlation prediction.	Exxon report, "XN-73-30, LOFT Fuel Departure from Nucleate Boiling Test Analysis and Results", December 1973.
E. <u>Combined</u> - Final prediction of LOFT fuel bundle thermal-hydraulic behavior based on evaluation of test program results.	ENC	Analysis incomplete.	Exxon report to be published.
F. <u>Control Rods</u>			
1. Prediction of control rod poison temperatures and guide tube flow rates.	ENC	The analysis predicted that the worst case temperature of the poison material would be 803°F maximum and that the guide tube flow rate should not exceed 1.84% of the total fuel bundle flow rate at worst case (withdrawn control rod) conditions.	Exxon report, "JN-72-15, LOFT Control Rod Cluster Design Report", July 18, 1972.
2. Prediction of control rod poison temperature during worst case planned LOCEs to evaluate potential melting (1,425°F) of the Ag-In-Cd poison material.	ANC	The analysis which assumed both helium and argon fill gas conditions predicted that the melting temperature would not reach 1,425°F until approximately 90 sec after initiation of the pipe break which corresponds to a fuel rod cladding temperature above 2,000°F	ANC LTR 1111-6, "LOFT Control Rod Poison Temperature History after a 100% Inlet LOCE with Delayed ECC Injection", June 27, 1972 and ANC letter report "Control Rod Poison Temperature History: Effect of Using Helium Instead of Argon in Control Rod Gas Space - HFR-5-72", July 11, 1972.
G. <u>Neutron Source</u> - Prediction of heat generation and cooling requirements.	ENC	The analysis predicted an equivalent heat source of 0.008 watt which could raise the capsule temperature only 1°F assuming convection cooling of the outside guide tube surface only.	Exxon report, "XN-74-48, Design Report, LOFT Neutron Source Assembly", December 1974.
H. <u>Fuel Module Thermal Expansion</u> - Prediction of differential thermal growth between the fuel module and its end supports during steady state and planned LOCE environments.	ANC	The analysis predicted a differential thermal expansion of 0.804 inch for fuel modules with stainless steel guide tubes and 0.252 inch with Zircaloy guide tubes.	ANC LTR 1111-7, "LOFT Fuel Assembly and Upper Support Structure Thermal Expansion Analysis", March 12, 1973.
I. <u>Fuel Bundle Instrumentation Routing</u> - Prediction of guide tube instrumentation component temperatures during reactor operation.	ENC	The analysis predicted that the coolant flow through the instrumented guide tubes satisfactorily maintains the instrumentation components at acceptable temperature levels.	ENC report, "JN-72-16, LOFT Instrumentation Routing Design Report", July 20, 1972.

TABLE II-VI

LOFT FUEL MODULE STRUCTURAL DESIGN PROGRAM

Purpose	Performing Organization	Result	Documentation
A. General			
1. Preliminary prediction of fuel module axial loadings during representative expected LOCEs assuming 1 msec break time.	ANC	A WHAM code analysis of the hydraulic environment and a lumped-mass-system response model analysis resulted in a prediction of a worst case loading of 8,000 lb to a 15 x 15 size fuel bundle.	ANC LTR 1.1.1.3-1, "A Preliminary Dynamic Analysis of the LOFT Reactor Fuel Assemblies", January 28, 1971.
2. Prediction of LOFT fuel module axial loadings during representative expected LOCEs assuming 1 msec break time.	ANC	A WHAM code analysis of the hydraulic environment and a SHOCK code analysis using a lumped-mass-system response model resulted in a prediction of a worst case loading of 11,668 lb to a 15 x 15 size fuel bundle.	ANC LTR 1115-25, "Dynamic Analysis of LOFT Reactor Flow Skirt-Core Filler Assembly for Nonnuclear and Nuclear Loss-of-Coolant Experiments", March 24, 1974.
B. Fuel Rods			
1. Prediction of fuel and cladding temperatures for use in other subsequent structural evaluations.	ENC	The analysis predicted a peak centerline fuel temperature of 4,642°F assuming the linear heat generation rate of 19.0 kW/ft and using the Lyons UO ₂ thermal conductivity correlation. The predicted maximum cladding temperature was 795°F at 19 kW/ft.	Exxon reports: a. "JN-72-4, Standard LOFT Fuel Rod Design Report", March 2, 1972. b. "JN-72-5, Standard LOFT Fuel Rod Design Report Addendum 1", March 17, 1972.
2. Prediction of cladding strains to confirm that strain limitations of 1% are not exceeded.	ENC	The analysis, which included consideration of fuel swelling, thermal conditions, and power history, indicated the design satisfied the 1% strain limitation.	Same as above
3. Prediction of fission gas release and resulting internal fuel rod pressure for use in subsequent structural evaluations.	ENC	The analysis, which included temperature-corrected release of all or portions of the volatile UO ₂ impurities and gaseous fission products to the fuel rod void volume, predicted a total EOL gas pressure of 2,500 psi assuming a commercial PWR lifetime requirement of 30,000 MWD/MTU.	Same as above
4. Analysis of fuel rod cladding stresses to predict: a. Instantaneous collapse pressure b. Time to creep collapse c. Primary stresses d. Thermal-mechanical effects (mechanical bending, thermal bowing, temperature gradient and flow-induced vibrations) e. Axial loads from fuel bundle skeleton differential thermal ex-	ENC	The analysis predicted the following: a. an instantaneous collapse pressure of 4,697/2,995 psig at 70 and 735°F. b. time to creep collapse of approximately 1300 hours. c. primary stresses slightly exceeding 1/3 of the minimum specified ultimate strength d. (results included in c) e. a total force of 24 lb (stress results included in c).	Same as above (Item a)

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
B. Fuel rods (contd.)			
5. Prediction of pellet stack holddown spring force requirement and spring characteristics.	ENC	The analysis results determined, the geometrical requirements for an Inconel X-750 spring that would provide a nominal 4 g spring force.	Same as above
6. Prediction of potential fuel densification effects on fuel rod behavior. Specific parameters predicted were: a. pellet stack gap size and frequency b. cladding stresses c. creation of power spikes d. fuel centerline temperature e. DNB behavior.		The analysis which used the November 14, 1972 USAEC fuel densification guidelines predicted: a. cladding collapse could occur in 1,050 hours b. centerline fuel temperature less than melting at 19 kW/ft c. precautions are necessary to preserve the 1.30 DNB margin.	Exxon report, "XN-73-28, LOFT Fuel Densification Study", October 22, 1973.
C. Control Rods			
1. Prediction of buffer spring assembly characteristics required to absorb the terminal kinetic energy of the control rod cluster assembly.	ENC	The analysis determined that two concentric Inconel X-750 springs would achieve the design objective.	Exxon report, "JN-72-15, LOFT Control Rod Cluster Design Report", July 18, 1972.
2. Prediction of buffer spring stresses during absorption of control rod cluster assembly kinetic energy.	ENC	The analysis predicted stresses approximately 5% below allowable stresses.	Same as above
3. Prediction of spider arm stresses during scram deceleration.	ENC	The analysis predicted stresses, approximately 10% below allowable stresses.	Same as above
4. Prediction of control rod elastic behavior under worst case misalignment conditions.	ENC	The analysis predicted the control rods to be flexible and accommodating to potential worst case misalignment conditions.	Same as above
5. Prediction of control rod cladding stresses and collapse characteristics.	ENC	The analysis predicted that cladding stresses from external pressure, thermal gradients, and potential mechanical bending to be less than allowables. The internal collapse pressure was predicted to be 3,140/2,775 psi at RT and 650°F.	Same as above
	MPR	This analysis which included as-built material and dimensional characteristics and more accurate analytical techniques predicted internal collapse pressures to be 3,989/3,711 psi at RT and 650°F.	ANC LTR-1111-16, "LOFT Control Rod Collapse Prediction", February 25, 1975.

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
C. Control rods (contd.)			
6. Prediction of control rod vibration characteristics.	ENC	The analysis concluded that the control rod vibration within the guide tube would not be a problem.	Exxon report, "JN-72-20, LOFT Fuel Design Report - Addenda", September 18, 1972.
7. Prediction of control rod drop time to provide information for designing buffer springs and spider webs.	ANC & ENC	The analysis predicted the control rod drop times to be 0.97 sec for 75% insertion and 2.97 sec for full insertion from full withdrawal at reactor operating conditions.	ANC internal letter BVW-4-71, "LOFT Scram Time Analysis", August 20, 1971. ENC report (see C.1).
8. Experimental determination of control rod cluster drop time and confirmation of control rod cluster structural capabilities.	ENC	The experimental data confirmed the LOFT control rod cluster structural capabilities and indicated 0.75 and 2.0 sec to 75 and 100% insertion under expected alignment conditions, 0.95 and 2.15 sec to 75 and 100% insertion under simulated worst case misalignment conditions, and terminal velocity of 5.2 ft/sec.	ENC report, "XN-74-57, LOFT Reactor Control Cluster Insertion Tests", December 1974.
D. Spacer Grids			
1. Establish spacer grid spring geometry to "force a node" when the fuel rod is vibrating, elastically accommodate manufacturing tolerances and imposed assembly deflection, accommodate fuel rod diametral changes and prevent damage to the fuel rod cladding.	ENC	A combined analytical and experimental program was employed. The analytical program was based on a beam model and the experimental program consisted of dead-weight loading a single spacer grid cell mockup. A spring configuration was developed that satisfied the design requirements.	Exxon report, "JN-72-8, LOFT Fuel Rod Grid Spacers, Design Report", April 24, 1972.
2. Predict stresses in spacer grid springs and welds due to expected vertical loads.	ENC	A linear elastic analysis using a beam model predicted the maximum stresses to occur at the intersection welds and that the maximum stresses would not exceed allowable stresses.	Same as above
3. Predict stresses in strip due to possible unbalanced spring-to-dimple load.	ENC	A linear elastic analysis using a shell model predicted a maximum stress intensity of 74,500 psi compared to the material allowable yield stress of 150,000 psi.	Same as above
4. Evaluate combined effects of vertical and unbalanced loads.	ENC	The analytical evaluation predicted a maximum combined stress intensity of 101,200 psi.	Same as above
5. Predict the fatigue behavior of the springs.	ENC	The analytical evaluation predicted the fatigue life, based on an alternating friction load (26,700 psi max), would not result in failure during the expected 10^3 to 10^6 number of cycles since the alternating stress limit for 10^6 cycles is 110,000 psi.	Same as above

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
D. Spacer grids (contd.)			
6. Determine the spacer grid weight.	ENC	The analysis predicted the weight to be 860 grams for the 15 x 15 spacer. Actual measured weights are 848.3 and 363.8 grams for the 15 x 15 and corner spacer, respectively.	Same as above (analysis only)
7. Evaluation of spacer grid response to loadings during fuel loading (fuel assembly lower tie plate contacting spacer grid top at vertical velocity of 40 in./min).	ENC	The analysis, which used a beam model and the STRESS-II code, predicted that in the abrupt stop situation the spacer strips, under direct loading, would yield locally but not fracture at the strips or intersection joints.	Exxon report, "JN-72-20, LOFT Fuel Design Report - Addenda", September 18, 1972.
8. Evaluation of spacer grid response to a lateral 6 g load applied during shipment or a 1,500 lb seismic load.	ENC	The analysis predicted maximum stress in the strips of 55,000 psi (approximately 80% of the allowable load).	Same as above
9. Experimental determination of intersection joint strength.	ENC	The experimental results indicated a strength of 387 pounds in tension compared to a 14 pounds design requirement and 1,135 pounds in shear.	Exxon report, "XN-104, Report of Experimental Determination of LOFT Grid Spacer Strength", February 1973.
10. Experimental determination of side plate weld strength to evaluate shear resistance characteristics during the fuel loading condition of Item 7 above.	ENC	The experimental results indicated a weld joint bending moment capability of 1.39 in.-lb compared to the Item 7 predicted load of 0.2 in.-lb.	Same as above
11. Experimental determination of spacer grid response to the fuel loading condition of Item 7.	ENC	The experimental results indicated the spacer grid would not fail catastrophically (fracture) up to loads of 4,000 lb compared to the Item 7 loading condition of 3,500 lb.	Same as above
12. Experimental confirmation that the spacer grid design dampens fuel rod vibration and prevents damage by the fretting-corrosion process.	ENC	The experiments, which included 1,500 hour flow environment test of full-size 15 x 15 and corner instrumented fuel bundle prototypes, demonstrated that the design was satisfactory.	Exxon report, "XN-74-61, LOFT Fretting Corrosion Test Report", December 30, 1974.
E. Skeleton			
1. Preliminary prediction of guide tube geometry needed to satisfy the LOFT fuel technical specifications.	ENC	The analysis, which considered the combined effects of bending and column instability, predicted the required guide tube wall thicknesses of 0.038 inch for 304 SS and 0.100 inch for Zr-4 compared to a desired 0.015 inch.	Exxon report, "JN-72-7, LOFT Fuel Supply Guide Tube Strength", April 4, 1972.

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
E. Skeleton (contd.)			
2. Prediction of guide tube structural response to expected axial loads (maximum) of 5,490 lb (for 15 x 15 bundle) at room temperature.	ENC	This analysis predicted that the LOFT guide tube wall thickness should be 0.017 inch to satisfy the revised (compared to Item 1) LOFT fuel technical specifications.	Exxon report, "JN-72-10, LOFT Fuel Assembly Structural Design and Analysis", May 8, 1972.
3. Prediction of stresses in the upper and lower tie plates.	ENC	This analysis, which used a beam element analysis program, predicted the expected axial loading conditions would result in a maximum tie plate stress of 9,000 psi compared to the allowable level of 14,000 psi.	Same as above
4. Prediction of guide tube mechanical attachments stresses.	ENC	This analysis, which considered the attachment of the guide tube to sleeve, the guide tube to lower end cap, and upper sleeve to nut fastening system, indicated the attachments were capable of withstanding axial loads of 2,000 lb.	Same as above for 304 SS. Exxon report, "XN-74-47, LOFT Type F Fuel Assembly Structural Design and Analyses", December 1974 for Zr-4.
5. Determination of guide tube to spacer grid locking ring attachment (4 spot welds per ring) strength.	ENC	The experimental results indicated the axial load capabilities to be 1,620 lb for the 304 SS and 1,770 lb for the Zr-4 compared to a design load of 200 lb.	Exxon report, "XN-104, Report of Experimental Determination of LOFT Grid Spacer Strength", February 1973.
6. Experimental determination of fuel bundle strength characteristics including separate determinations of tie plate and empty skeleton characteristics and fuel bundle response to worst-case misalignment conditions.	ENC	The experimental results indicated: a. tie plate (upper) yield stresses at 10,000 lb axial load b. guide tube-spacer grid (skeleton) load capabilities of 22,000 lb c. no significant deterioration of axial strength in the misaligned condition d. a fuel bundle natural frequency of 320 Herz.	Exxon report, "XN-74-28, LOFT Fuel Assembly Strength Test, Test Report", July 15, 1974.
7. Experimental determination of LOFT guide tubes mechanical properties in compression at high temperatures (1000, 1300, and 1520°F) for use in evaluating LOFT fuel bundle response during LOCEs.	ENC	The experimental results indicate mechanical properties in compression at these temperatures are lower (up to 40% for 304 SS and 10% for Zr-4) than published tensile mechanical properties data.	Exxon report, "XN-74-50, Guide Tube Compression Tests - Test Report", November 1974.
F. Neutron Source			
1. Prediction of holdown spring assembly characteristics required to provide a 65 lb force of engagement.	ENC	The analysis determined that two concentric Inconel X-750 springs would achieve the design objective.	Exxon report, "XN-74-48, Design Report, LOFT Neutron Source Assembly", December 1974.
2. Experimental confirmation of design configuration during exposure to expected reactor operating conditions.	ENC	The experiment, which included a 1,500 hour flow environment test of a prototype neutron source rod assembly demonstrated that the design was satisfactory.	Exxon report, "XN-74-61, LOFT Fretting-Corrosion Test Report", December 30, 1974.

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
G. Upper Support Structure			
1. Prediction of LOFT fuel module upper structure axial loads during normal, LOCE and LOCA conditions. The LOCE loads were predicted assuming an instantaneous break of an 8 in. Schedule 160 pipe close to the reactor vessel. The LOCA loads were predicted assuming an instantaneous break of an 11 in. ID pipe in the reactor vessel outlet nozzle.	MPR	The analysis predicted the following axial loads:	MPR report, "MPR-316, Design Report for LOFT Fuel Assembly Upper Support Structures", November 1971.
		Load (lb)	
		Normal LOCE LOCA	
		Corner Structure 185 1,340 24,610	
		Control Structure 534 3,880 71,000	
		Center Structure 384 2,800 51,100.	
2. Prediction of corner structure stress and fatigue conditions under normal, LOCE and LOCA conditions for the following components: 1. Main support tube 2. Lower and intermediate support pads 3. Core plate pins 4. Holddown spring 5. Spring deflector.	MPR	The analysis predicted that all structure components analyzed would have (a) stress conditions below the allowable stresses and in most cases substantially below (factor of 10) allowables, and (b) no cumulative fatigue usage factor.	Same as above
3. Prediction of center structure stress and fatigue condition under normal, LOCE and LOCA conditions for the following components: 1. Main support box structure 2. Main support corner bars (at reactor vessel nozzle level) 3. Instrument channel 4. Lower, intermediate, and upper support pads 5. Holddown spring 6. Adjusting nut 7. Orifice plate.	MPR	Same as above	Same as above except Item 4 (lower support pads) and Item 7 are reported in MPR letter report, "Analysis of Center Fuel Module Orifice Modification", June 10, 1974.
4. Prediction of control guide structure stress and fatigue conditions under normal, LOCE and LOCA conditions for the following components: 1. Drive shaft shroud 2. Control rod guide tubes 3. Alignment pads 4. Alignment pins.	MPR	Same as above	Same as G.1

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
G. Upper Support Structure (contd.)			
5. Prediction of control structure stress and fatigue conditions under normal, LOCE and LOCA conditions for the following components: 1. Main support box structure 2. Main support corner bars (at reactor vessel nozzle level) 3. Instrument channel 4. Lower and intermediate support pads 5. Upper core support plate pins 6. Support gussets.	MPR	Same as above	Same as above
6. Preliminary prediction of thermal stresses during LOCE reflooding (bottom injection) or deluging (top injection)	MPR	The analysis predicted that (a) stress levels would be acceptable during reflooding, (b) stress levels during deluging could limit the number of cycles to 17, and (c) additional analysis should be performed.	Same as above
7. Additional prediction of effect of LOCE top ejection on upper support structures.		This analysis predicted that unsatisfactory distortion could occur during top ejection.	MPR letter report, "Effects of Upper Plenum Injection of LOFT Fuel Module Upper Support Structures", May 10, 1973.
8. Prediction of natural frequency for the following: a. Control upper structures b. Center upper structures c. Corner upper structures d. Control rod shaft shroud e. Control rod guide structure	MPR	The analysis predicted the following natural frequencies: Control structure - 270 to 2,380 cps Center structure - 270 to 2,380 cps Corner structure - 105 to 545 cps Shroud - 360 cps Guide structure - 510 to 4,100 cps.	Same as G.1
9. Preliminary prediction of holddown spring design characteristics. This analysis included prediction of the holddown force requirements.	MPR	The analysis predicted the holddown springs should provide a holddown force of 6,500 lb for 15 x 15 fuel modules and allow up to 0.70 inch differential growth of the fuel module.	Same as above
10. Additional prediction of holddown spring characteristics to provide a holddown force of 1,000 lb for 15 x 15 fuel modules and 500 lb for corner fuel modules and spring rate of 2,000 lb/in.	MPR	This analyses predicted a set of spring geometries to meet the specified requirements. In addition, evaluations were made of fuel bundle loading during LOCE subcooled blowdown and core heatup phases and predictions of improved fuel bundle guide tube characteristics.	MPR letter report, "Alternate Holddown Spring Designs for LOFT Fuel Modules", August 22, 1972.
11. Additional prediction of holddown spring characteristics to provide a spring constant of 500 lb/in.	MPR	This analysis resulted in a recommendation to correct the holddown spring constant to 500 lb/in. and predicted a set of spring parameters that satisfied the revised spring constant requirement.	MPR letter report, "Spring Constant for Fuel Module Holddown Springs", August 29, 1972.

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
G. Upper Support Structure (contd.)			
12. Additional prediction of holddown spring characteristics to correct the differential growth requirement to 1.07 inches (center bundle) and 1.02 inches for corner and control bundles.	ANC	The analysis predicted a set of spring parameters that satisfied the requirement for holddown force and spring constant.	ANC letter report, "LOFT Reactor Fuel Assembly Holddown Springs-Saff-22-72", October 12, 1972 and "Type F Fuel Module Holddown Spring-Saff-26-72", November 27, 1972.
13. Final prediction of holddown spring characteristics to correct a misunderstanding regarding the definition of "installed length" which had resulted in a higher than specified holddown load.	ANC	This analysis resulted in the establishment of a set of spring design parameters that finally satisfied the requirement for holddown force, spring rate and differential growth.	Unpublished ANC analysis dated May 21, 1973.
H. Instrumentation Penetration			
1. Prediction of stresses during normal operating conditions for the following (5 in. diameter penetration) pressure boundary components: a. head bolts b. pressure tube c. pressure plate d. braze buttons.	ANC	The analysis predicted that the design for all configuration analyzed was adequate (within ASME Section III, Class I allowables) for the design conditions with one exception, which was corrected by increasing the quantity of head bolts.	ANC letter report, "LOFT Instrumentation Center Penetration, 5 in. - Fors-3-73", April 4, 1973.
2. Prediction of stresses during normal operating conditions for Inconel-titanium upper and lower buttons, 304 SS lower button and the following weld designs: a. pressure tube weld b. teleflux tube weld c. connector weld d. lead seal weld e. mounting sleeve weld f. standoff weld.	ANC	The analysis predicted that the design for all configurations analyzed was adequate (within ASME Section III, Class I allowables) for the design conditions with one exception, which was corrected by increasing the lead seal weld leg to at least 0.078 inch. The button analysis used a finite element model of the button.	ANC letter report, "Analysis of LOFT Instrumentation Penetrations - BVW-5-73", July 9, 1973.
3. Prediction of stresses under expected loads for the following pivot arm components: a. lower pivot pin b. upper pivot pin c. support bracket (part of upper structure) d. arm (shoulder area).	ANC	The analysis predicted the design, for all configurations analyzed, was adequate (within ASME Section III, Class I allowables) for vertical loadings up to 500 lb.	ANC letter report, "Analysis of LOFT Fuel Module Pivot Arm Assembly - BVW-4-73", May 23, 1973.

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
H. Instrumentation Penetration (contd.)			
4. Experimental determination of titanium-tantalum-Inconel explosion-bonded sandwich (Detaclad) material mechanical characteristics under the following conditions: a. simple tensile loading b. ram tensile loading c. braze cycling d. bending loading e. thermal cycling (10C cycles 135 to 650°F) f. stress-corrosion (1,300 psi stress levels for 3,000 hours in 2,500 psi, 600°F borated water simulating LOFT coolant chemistry conditions.	ANC	The experiments indicated that the Detaclad material has (a) adequate load-carrying capability for the LOFT service conditions, (b) satisfactory corrosion resistance, and (c) satisfactory dimensional and mechanical properties stability under both brazing and normal operation thermal cycling.	ANC letter report, "Transmittal of Test Results Detaclad Composite Material, GU-9-73", July 10, 1973.
5. Experimental determination of the center fuel bundle instrumentation penetration assembly response to four 500-hour cycles of exposure to 2,500 psi, 600°F borated water (simulating LOFT coolant chemistry condition) terminated by a blowdown plus a preliminary hydrostatic test at 3,125 psig.	ANC	The experimental results indicated satisfactory performance of the instrumentation assembly.	Not available
I. Fuel Bundle Instrumentation Attachment			
1. Prediction of stresses in instrumented fuel rod-to-upper-tie-plate fastener arrangement.	ENC	The analysis predicted that allowable strength limitations would not be exceeded at an instrumented fuel rod cap screw torque of 8 in.-lb at ambient conditions.	Exxon report, "JN-72-20, LOFT Fuel Design Report Addenda", September 13, 1972.
2. Prediction of stresses and fatigue life in the coolant thermocouple hot-junction protrusion (0.125 inch) into the tie plate flow channel.	ENC	The analysis predicted that the design configuration would withstand both normal operating and the specified LOCE design conditions.	Exxon report, "JN-72-16, LOFT Instrumentation Routing Design Report", July 10, 1972.

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
I. Fuel Bundle Instrumentation Attachment (contd.)			
3. Prediction of stresses in the instrument cable unsupported spans including the guide tube instrumentation assembly instrument cable coils.	ENC	The analysis, which included evaluation of flow-induced vibration, predicted that the design configuration was adequate (a) for a 300 psi transverse differential pressure except for the fuel rod thermocouples where transverse loads of this magnitude were judged to be unappropriate, and (b) the specified temperature conditions.	Same as above
4. Prediction of stresses in the instrument cable bundle tiedowns (staples).	ENC	The analysis predicted that the design configuration could withstand the 300 psi transverse differential pressure.	Same as above
5. Prediction of stresses in the liquid-level routing fixture.	ENC	The analysis predicted that the design configuration would withstand the 300 psi transverse differential pressure.	Same as above
6. Experimental confirmation of fuel bundle instrument attachment designs to withstand expected normal reactor flow conditions.	ENC	The experiment consisted of 1,500 hour flow environment tests of a full-size prototype and demonstrated that the design was satisfactory for expected normal reactor flow conditions.	Exxon reports, "XN-74-61, LOFT Fretting - Corrosion Test Report", December 30, 1974, and "XN-75-12, LOFT Fretting Corrosion Test Report, Addendum 1, Instrumentation Examination Results", March 1975.
7. Experimental confirmation of fuel rod thermocouple attachment response to a series of temperature cycling programs representing reactor conditions as follows: a. Cycle program consisting of 1 cycle, 400 to 1,100°F; 5 cycles, 400 to 1,500°F; and 5 cycles, 400 to 1,700°F. b. Cycle program consisting of 20 cycles, 400 to 745°F and 15 cycles, 400 to 1,400°F.	ENC	The experiment results indicated that improvements could be made in the weld design since some weld failures occurred during testing.	Exxon report, "XN-74-62, LOFT Instrumentation Fuel Rod and Guide Tube Thermal Cycling and Corrosion Test", December 31, 1974
8. Experimental confirmation of guide tube instrumentation assembly (4 guide tube T/C model) response to a temperature cycling program (20 cycles, 400 to 675°F and 15 cycles, 400 to 1,400°F) representing normal reactor operating and LOCE conditions.	ENC	The experiment results indicate that the design is satisfactory.	Same as above

TABLE I--VI (contd.)

Purpose	Performing Organization	Result	Documentation
I. Fuel Bundle Instrumentation Attachment (contd.)			
9. Experimental confirmation of fuel rod thermocouple attachment response to a combination thermal and hydraulic loading cyclic program representing expected LOCE conditions as follows: a. a three cycle test consisting of heating to 1,400, 1,600, and 1,900°F cladding temperature and blowing down (4 joule weld characteristics) b. a nine-cycle test consisting of heating to 1,300°F (1 cycle), 1,400°F (5 cycles), 1,600°F (1 cycle), and 1,700°F (2 cycles) (6.5 joule weld characteristics). Test specimens include full-length samples.	ANC	The experiment results indicated that the weld characteristics of the 6.5 joule weld were satisfactory and the weld characteristics of the 4.0 joule welds were likely satisfactory based on the results of the abbreviated program.	ANC LTR-141-6, "LOFT Heater Pin Thermocouple Attachment Testing", May 17, 1973.
10. Experimental confirmation of fuel rod thermocouple attachment response to a combination thermal and hydraulic loading cycle program consisting of 21 thermal cycles by heating in steam to 1,100°F (1 cycle), 1,400°F (5 cycles), 1,500°F (5 cycles), 1,600°F (5 cycles), and 1,700°F (5 cycles); each cycle followed by bottom flooding. The 10 inch test sample has 4 simulated thermocouples representing the full range of acceptable weld characteristics.	ANC	This is a routine experimental program to confirm the stress and corrosion capabilities of the weld design. Recent results indicated that a new weld design based on 4.0 joules, 275 spot size, and 40 aperture was unsatisfactory.	ANC letter report, "Tube Furnace Thermal Cycle Tests, Mes-1-73", January 5, 1973. Recent test results to be published.
11. Experimental confirmation of fuel rod thermocouple attachment corrosion behavior under the expected LOFT environment (borated water at 2,500 psia and 650°F) conditions.	ANC	This is a routine test of the long-term weld corrosion characteristics. Recent results indicated that the expected lifetime of the welds in the LOFT reactor would be about 6,000 hours.	ANC LTR 1.4.1-29, "Autoclave Life Tests of LOFT Fuel Rod Thermocouple Welds", March 13, 1975.

TABLE II-VI (contd.)

Purpose	Performing Organization	Result	Documentation
J. Upper Structure Instrumentation Attachment			
1. Predictions of stresses during normal operating, LOCE and LOCA conditions for the following components:	MPR	The analyses predicted that the design of all components was adequate for normal operating, LOCE, and LOCA conditions with one exception which was corrected by adding a strategically located Type A density-velocity detector cable conduct support.	MPR letter report, "Design Analysis for Upper Core Support Structure Replaceable Instruments", May 21, 1973.
a. density-velocity detector mounting bracket			
b. density-velocity detector mounting flange (including fastener counter bore)			
c. Type C upper structure window-frame cutout for density-velocity detector bracket.			
d. Type A upper structure window-frame cutout for density-velocity detector			
e. Type A density-velocity detector cable conduit			
f. Type A density-velocity detector cable conduit staples			
g. Free-field pressure detector mounting flange			
h. Displacement transducer bolt preload			
i. 1/4 inch cap screw			
j. 1/4, 3/8, and 3/4 inch capscrew locking cups.			

TABLE II-VII

LOFT FUEL MODULE MISCELLANEOUS DESIGN ACTIVITIES

Purpose	Performing Organization	Result	Documentation
A. <u>Fuel Rod Cladding</u> - Determine the rate and amount of annealing of LOFT cold-worked Zircaloy cladding when exposed to temperatures (800 to 1,800°F) expected in LOCEs.	ANC	The experimental results indicated that rapid annealing would occur in the 1,100 to 1,200°F range with more rapid rates up to 1,500°F where instantaneous (less than 1 sec) annealing was indicated.	ANC LTR 1.1.1.1-4, "Zircaloy-4 Annealing Study", October 29, 1974.
B. <u>Control Rod Poison</u> - Determine the fundamental material properties of melting temperature, and coefficient of expansion near the melting temperature of the LOFT 80Ag-15In-5Cd poison material	BMI	The experimental results indicated the following properties: Melting temperature -- 1,472° to 1,562°F Volume expansion in melting -- 5.4% Thermal expansion coefficient after melting -- 3.83×10^{-2} in./in. °F (1,562 to 1,859°F) Thermal expansion below melting temperature -- 13.3×10^{-6} in./in. °F.	ANC LTR 1.1.1.1-15, "Experimental Determination of Behavior of the Ag-In-Cd Poison Rod Alloy at Expected LOCE Temperatures", February 1975.
C. <u>Control Rod Poison</u> - Determine the compatibility of the 80 Ag-15In-5Cd LOFT poison material with Type 304 SS at expected LOCE temperatures (1,400 to 2,000°F).	ANC	The experimental results indicated that the poison rod alloy and 304 SS are compatible.	Same as above.
D. <u>Fuel Bundle Components</u> - Evaluate electrical discharge machining (EDM) as a technique for machining in-core components	ENC	The combined literature information evaluation, corrosion tests and metallurgical investigation indicated the EDM is an acceptable machining technique.	Exxon report "JN-72-21, Evaluation of Electrical Discharge as a Technique for Machining In-Core Components" September 8, 1972.

Associates, Inc. (Exxon only), and ERDA (Exxon only). The performance of these fabricators was exemplary; each demonstrated skill, efficiency, pride in their product and corporate image, and responsiveness to frequent design changes and requests for technical assistance. The success of this fabrication program is indebted to these suppliers. Some interesting information from the fabrication program is as follows:

2.3.1 Laser Welding. This part of the fabrication program is of special interest because of the experience and limited success in performing an extremely difficult welding task. LOFT fuel rods require the welded attachment of fragile thermocouples having a sheath thickness of only 0.009 inch to the fuel rod. Up to four thermocouples are attached over virtually the entire fuel rod length. In some cases this required approximately 5,000 weld spots that had to be (a) positioned on the filler wire between the thermocouple and fuel rod cladding within ± 0.002 inch and (b) identical in size and thermal intensity. The welding equipment includes two identical units each consisting of a basic Korad (Korad Division of Hedron, Inc. Santa Monica, California) laser-filler model KWD which uses a YAG (Yttrium-Aluminum-Garnet) laser rod and a specially designed pneumatically controlled positioning and indexing tool provided by S/P Product Design of Costa Mesa, California.

This laser welding system has been in a continuous improvement status since late 1971. Equipment improvements include:

- Shutter positioning
- Power supply component shock mounting
- Power supply instrumentation
- Binocular sight reticle
- Optic alignment techniques
- Flash lamp support
- Tooling alignment techniques
- Preventive maintenance program
- Optical and pneumatic system cleanliness.

The laser system has recently produced instrumented fuel rods with a yield rate of approximately 80% acceptable fuel rods. The equipment usage factor is less than 50% because of malfunctions in the laser and pneumatic tooling system and other problems. The complexity of the operation is very demanding on the operators, and operator errors have accounted for approximately 5% of the rejected rods. The system is capable of producing a 4-thermocouple fuel rod in approximately 10 hours per machine.

2.3.2 Fuel Bundle Assembly. This activity has also undergone an evolutionary process to achieve the specified alignment objectives. The assembly is accomplished in a horizontal position for instrumentation handling purposes. Fuel bundle alignment was achieved only after several technical problems were solved. Some of the highlights are as follows:

- (1) The skeleton assembly involves spacer grid attachment to the guide tubes by split rings positioned above and below the spacer

grids and welded to the guide tubes. The locking-ring-to-spacer-grid separation was discovered to be important to spacer grid parallelism characteristics, and welding process improvements were required. It was later discovered that shrinkage from the locking-ring-to-guide-tube welding also contributed to the spacer grid parallelism, and welding sequence improvements were made.

- (2) The upper end box is attached to the guide tubes by mechanical (threaded) techniques. The torqueing of the nuts results in residual torque in the guide tubes which causes the free-standing skeleton or fuel bundle to twist. Process improvements were made to minimize residual torque in the guide tubes.

2.4 Preproduction Process Demonstration Program

The preproduction process demonstration program for fabrication processes was comprehensive and thorough. The program performed for fuel bundle fabrication at Exxon is summarized in Table II-VIII. Similar comprehensive programs were performed — for the upper support structures by L&S Machine Co. and for the instrumentation penetration assemblies and fuel module assembly by ANC.

The process demonstration programs were effective in identification of unsatisfactory design and fabrication conditions and confirming that the corrective action was satisfactory.

The qualification/demonstration programs for measurement techniques were also comprehensive and thorough. All equipment and inspection equipment operators are covered by a comprehensive program to maintain equipment accuracy and operator skills.

2.5 Conclusion

The total program for the design, characterization, and fabrication of the LOFT fuel modules has achieved an objective of technical activity documentation and component characterization in a manner that (a) indicates an effort to provide a fuel module that will credibly represent a LPWR fuel bundle during a LOCA and (b) determines the representative characteristics of the LOFT fuel bundles. The achievement of design objectives and success of the total fuel module design, characterization, and fabrication program will be determined by the performance of the fuel modules in the LOFT testing program. The planning and preparation activities for the interim fuel bundle inspection (requalification) and final fuel bundle examination (posttest autopsy) components of the LOFT testing program are proceeding.

TABLE II-VIII

LOFT FUEL BUNDLE FABRICATION PROCESS QUALIFICATIONS/DEMONSTRATIONS PROGRAM

Purpose	Performing Organization	Result	Documentation
1. Fuel rods			
a. End cap welds (WP-27 and -61)	ENC	Satisfactory (40 welds each)	Exxon reports, 7/8/72 (WP-27) and 9/17/73 (WP-61)
b. Rotary fuel pellet pressing	ENC	Satisfactory (507 pellets)	Exxon report, 6/2/72
c. Fuel rod assembly	ENC	Satisfactory (6 rods)	No report available
d. Fuel rod pressurization	ENC	Satisfactory after a design modification to prevent contamination of the seal weld with spatter from the Inconel pellet holddown spring.	Exxon report, "XN-145, LOFT Prepressurization Process Demonstration", August 1973.
2. Fuel Bundle Skeleton			
a. Guide tube sleeve weld (WP-39)	ENC	Satisfactory (6 samples) with rework to correct alignment	Exxon report, 2/20/73
b. Guide tube sleeve edge weld (WP-40)	ENC	Satisfactory (6 samples)	Exxon report, 1/30/73
c. Guide tube lower end weld (WP-41)	ENC	Satisfactory (16 samples) with rework to correct alignment	Exxon report, 2/20/73
d. 15 x 15 spacer grid intersection joint welds, end tab weld, and corner overlap weld (SP-114)	CARAN	Satisfactory (10 samples each) after changes were made to eliminate porosity in weld centers	Caran report, 8/19/72
e. 15 x 15 spacer grid assembly (SP-114)	CARAN	Satisfactory (1 assembly) after design changes were made to center strips in slots during welding	Caran report, 8/19/72
f. Corner spacer grid assembly (SP-117)	CARAN	Satisfactory (one)	No report available
g. Spacer grid locking ring weld (WP-50)	ENC	Satisfactory (6 samples)	Exxon report, 9/3/74
h. Instrumentation tube plug weld (WP-42)	ENC	Satisfactory (6 samples)	Exxon report, 2/5/73
3. Fuel Bundle End Boxes			
a. Upper (15 x 15) end box 90 degree corner weld (LSWP-1)	L&S	Satisfactory (2 samples)	L&S report, March 1973
b. Upper (corner) end box 45 degree corner weld (LSWP-3)	L&S	Satisfactory (2 samples)	L&S report, March 1973
c. Upper end box side plate to collar weld (LSWP-2)	L&S	Satisfactory (1 sample)	L&S report, March 1973
4. Fuel Bundle Instrumentation Attachment			
a. Guide tube thermocouple to guide tube attachment laser weld (WP-131)	ENC	Satisfactory (12 samples)	Exxon report, 3/12/73
b. Guide tube assembly support to tube weld (WP-44)	ENC	Satisfactory (16 welds)	Exxon report, 2/5/73
c. Differential pressure tube locking ring weld (WP-45)	ENC	Satisfactory (8 welds)	Exxon report, 3/1/73
d. Liquid level routing fixture gusset and lock welds (WP-46)	ENC	Satisfactory (6 samples)	Exxon report, 3/19/73
e. Coolant thermocouple hot junction attachment to tie plate (metal upset)	ENC	Satisfactory (4 samples)	Exxon report, 4/73
f. Instrument cable bundle staple weld (WP-59)	ENC	Satisfactory (6 samples) after improvements for minimizing instrument cable heating	Exxon report, 3/22/74

TABLE II-VIII (contd.)

Purpose	Performing Organization	Result	Documentation
4. Fuel Bundle Instrumentation Attachment (Contd.)			
g. Lower tie plate to T/C cover plate weld (WP-52)	ENC	Satisfactory (14 samples)	Exxon report, 2/13/73
h. Thermocouple seal weld (WP-56)	ENC	Satisfactory (12 samples)	Exxon report, 2/8/73
i. Fuel rod thermocouple attachment laser weld	ENC	Satisfactory (1 full length, 4 T/C sample per machine) after numerous process improvements over a 3 year period. To date, approximately 6 full-length PPQ units, many 10 inch process requalification units and 67 prototypes have been fabricated as part of the total process demonstration program	
j. Fuel rod/tie plate fastener system (including metal upset)	ENC	Satisfactory (3 samples)	Exxon report, 3/73
k. Guide tube instrumentation assembly	ENC	One satisfactory assembly per model	Exxon reports:
(1) Type B instrumentation tube model			(1) To be published
(2) Two coolant thermocouple model			(2) 2/8/75
(3) Four coolant thermocouple model			(3) 2/8/75
(4) Fixed flux detector model			(4) no report available
(5) One guide tube T/C model			(5) 2/8/75
(6) Four guide tube T/C model			(6) to be published
5. Fuel Bundle Assembly			
a. Upper tie plate to end box weld (WF-49)	ENC	Satisfactory (8 samples)	Exxon report, 3/1/73
b. Fuel bundle tack and retaining welds (WF-88)	ENC	Satisfactory (approximately 8)	Exxon report, 1/22/75
c. Fuel bundle assembly plug weld (including pins and dowels) (WP-89)	ENC	Satisfactory (approximately 8)	Exxon report, 10/7/74
d. Center (15 x 15) Fuel bundle prototype	ENC	Successful (1 complete unit) -- The results indicated the following improvements were necessary or desirable: 1. AEC furnished titanium sheathed thermocouple quality 2. Fuel rod thermocouple attachment technique 3. Instrument cable bundle attachment to upper end box 4. Liquid level detector alignment 5. Lower end box thermocouple positioning 6. Skeleton alignment 7. Guide tube locking ring welding	Exxon monthly report for January 1973
e. Corner instrumented fuel bundle prototype	ENC	Successful (1 complete unit) -- The results indicated satisfactory improvement of items 1 through 5 above and indicated the following improvements were necessary or desirable: 1. Guide tube locking ring welding 2. Assembly shop management	Exxon monthly report for July 1973

TABLE II-VIII (contd.)

Purpose	Performing Organization	Result	Documentation
5. Fuel Bundle Assembly (contd.)			
e. Corner instrumented fuel bundle prototype		3. Skeleton alignment tilt (caused by inherent triangular shaped sensitivity) 4. Skeleton alignment twist (caused by guide tube residual torque introduced by nut torquing) 5. Clearance and access to lower end box guide tube nuts 6. Welding head access to peripheral spacer grid locking rings	
f. Control (15 x 15) fuel bundle prototype	ENC	Successful (1 noninstrumented unit mock-up) -- The results indicated satisfactory improvement of items 5.d. 6, and 7 and 5.e. 1. and 4. above	No report available
6. Control Rod Assembly	ENC	Satisfactory (14 samples)	Exxon report 6/10/74
a. Control rod end fitting seal weld (WP-54)	ENC	Satisfactory (10 welds)	Exxon report 4/3/74
b. Control rod to spider plug weld (WP-55)	ENC	Satisfactory (3 samples)	Exxon report 6/13/73
c. Extension shaft weld (WP-58)	ENC	Successful (2 prototypes) -- The results indicated that improvements were desirable in reducing porosity in the spider raw material and spider surface finish	
d. Control rod assembly	ENC	Successful (1 complete prototype) -- The results indicated improvements were desirable for positioning the tube-to-shaft crimping tool.	Same as Item 5.e.
7. Neutron Source Assembly	ENC		
8. Fuel Bundle Packaging and Shipping	ENC	Successful [1 instrumented (15 x 15) control fuel bundle mockup] -- The results indicated improvements were desirable in shipping container cleanliness: inside (before packaging) and outside (during transport); and assuring all support equipment was installed	No report available.

3. REFERENCES

- II-1. Aerojet Nuclear Company, internal report (July 14, 1975).
- II-2. L. S. Tong, *DNB Prediction for Axially Nonuniform Heat Flux Distribution*, Westinghouse Electric Corporation, Atomic Power Distribution, WCAP-5584-R1 (April 1966).
- II-3. Aerojet Nuclear Company, internal report (September 1970).
- II-4. Aerojet Nuclear Company, internal report (September 1970).
- II-5. K. V. Moore and W. H. Rettig, *RELAP4 - A Computer Program for Transient Thermal-Hydraulic Analysis*, ANCR-1127 (December 1973).
- II-6. C. D. Morgan et al, *Analytical and Experimental Investigation of Heat Transfer During Simulated Cold-Leg Blowdown Accidents*, The Babcock and Wilcox Company (October 1972).
- II-7. ORNL, *PWR Blowdown Heat Transfer - Separate Effects Program*, Oak Ridge National Laboratory (June 19, 1974).
- II-8. J. O. Cermak et al, "The Departure from Nucleate Boiling in Rod Bundles During Pressure Blowdown", *ASME Journal of Heat Transfer* (November 1970) pp 621-627.
- II-9. L. S. Tong and J. Weisman, "Thermal Analysis of Pressurized Water Reactors", *American Nuclear Society*, Monograph Series on Nuclear Science and Technology (1970).
- II-10. H. Holmstrom, H. Ollikkala, H. Reijonen, "Loss-of-Coolant Accident Analysis Considerations for the LOVILSA NPS", *Paper presented at the Enlarged HPG Meeting at Geilo* (March 16-21, 1975).
- II-11. Aerojet Nuclear Company, internal report (May 1975).

THIS PAGE
WAS INTENTIONALLY
LEFT BLANK

III. THERMAL FUELS BEHAVIOR PROGRAM

J. G. Crocker, Program Manager

The objective of the Thermal Fuels 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 will 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.

The primary means by which program objectives are reached is by performance and analysis of in-pile tests conducted in the Power Burst Facility (PBF). The range of transient events to be investigated in PBF tests occur in the following postulated accidents:

- (1) An accident in which a break in the primary cooling system occurs, or loss-of-coolant accident (LOCA)
- (2) An accident in which an overpower or undercooling situation, or both, occurs, i.e., a power-cooling-mismatch (PCM) accident
- (3) An accident in which a very rapid and large reactivity increase occurs, which leads to a power excursion, or a reactivity initiated accident (RIA).

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 describe (a) test results from Power-Cooling-Mismatch Test 8-1RS and (b) a description of the examination of Saxton^[III-1] irradiated fuel rods which is being conducted as part of the Irradiation Effects Test Series.

1. POWER COOLING MISMATCH (PCM-20)

Z. Martinson

1.1 Introduction

The primary objective of the PCM-20 Test Series is to provide in-pile experimental data on the behavior of pressurized water reactor (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.

This report describes the first in a series of in-pile tests to investigate post-CHF fuel rod behavior. The test was identified as PCM-20, Test 8-1 RS.

Nearly all CHF investigations have used electrically heated rods rather than nuclear fuel rods. The use of electrically heated rods should yield adequate data for determining the onset of CHF, although electrically heated post-CHF rod behavior is not typical of nuclear fuel rod post-CHF behavior.

The specific purposes of the PCM-20 Test Series are: (a) to verify existing analytical models for calculating the behavior of single PWR fuel rods under abnormal operating conditions, (b) to improve the analytical descriptions of the physical processes that occur during a PCM accident, and (c) to develop new analytical models or methods where none exist. To meet these objectives, PCM-20 tests will be performed to:

- (1) Characterize the behavior of unirradiated PWR fuel rods at power densities from the normal operating range through the range where CHF is exceeded
- (2) Determine the modes, mechanisms, and consequences of fuel rod cladding failure under various test conditions of overpower or undercooling.

The primary objectives of PCM-20, Test 8-1 RS were to determine:

- (1) The test fuel rod power at which CHF occurs on the test rod
- (2) The establishment of any unexpected levels of heat transfer equilibrium after initially reaching CHF
- (3) Any shift in the test rod power to achieve CHF and any shift in the axial location of CHF occurring from the repeated cycling in and out of CHF

- (4) The time and test rod power at which detectable cladding axial deformation, if any, starts
- (5) The thermal and mechanical response and the post-CHF behavior of the fuel rod under these severe temperature transients
- (6) The adequacy of the measuring instruments for this type of test.

For an overpower accident in a commercial PWR power plant, CHF is expected to occur at a maximum linear power density of about 21 kW/ft. The initial CHF-type test, 8-1RS, was designed to have the onset of CHF occur at about 20 kW/ft. The PBF active core length is only three feet as compared to the 12-foot length of a commercial PWR power plant. The system coolant conditions for the PBF, therefore, had to be changed from typical PWR values, i.e., inlet temperature of 550 to 570°F, a coolant mass flow rate of about 2.25×10^6 lb/hr-ft², and a coolant pressure of about 2,200 psi. Nominal coolant conditions for Test 8-1RS were: inlet temperature of 622°F, a coolant mass flow rate of 1.05×10^6 lb/hr-ft², and a coolant pressure of 2,080 psia.

1.2 Test Rod, Experiment Test Train, and Instrumentation

The PCM-20 PWR-type test rod illustrated in Figure III-1 consists of a 36-inch long stack of 20% enriched UO₂ fuel pellets that are 0.366 inch in diameter and 0.61 inch long. The fuel pellets are contained in zircaloy cladding that has a 0.422 inch outside diameter and a 0.024-inch wall thickness. Zircaloy end caps are welded to the cladding tube to complete the fuel rod assembly. The fuel rod fill gas is helium. Table III-I lists the nominal design characteristics of the test fuel rod.

The instrumented fuel rod is installed in the loop experiment test train, the main portion of which is illustrated in Figure III-2 along with the remaining test instrumentation. A detailed description of the upper and lower test train assemblies is contained in Reference III-2.

The test hardware is designed to facilitate removal and replacement of the fuel rod and shroud after each test. Instruments attached to the fuel rod are removed with the rod and shroud assembly. Instruments not directly attached to the fuel rod remain in the test train and are reused. The fuel rod is held rigidly at the top and is free to expand downwards.

1.2.1 Test Instrumentation. The test instrumentation consisted of the following:

- (1) Four tungsten-rhenium thermocouples (TC) for measurement of the fuel rod cladding surface temperature located 90 degrees apart at 25, 27, 29, and 31 inches from the bottom of the fuel stack.
- (2) A (0 to 3,000 psi) pressure transducer to measure fuel rod internal pressure.

Fig. III-1 Schematic drawing of PCM-20 PWR-type test rod.

TABLE III-I

NOMINAL DESIGN CHARACTERISTICS OF PCM-20 TEST FUEL ROD

Parameter	Value
Fuel	
Material	UO ₂ sintered pellets
Pellet OD	0.366 inch
Pellet length	0.61 inch
Pellet enrichment	20 wt%
(wt%) U-235 in total U	
Density	93% theoretical density
Fuel stack length	36 inches
End configuration	dished
Cladding	
Material	zircaloy-4
Tube OD	0.422 inch
Tube wall thickness	0.024 inch
Tube ID	0.374 inch
Yield strength (0.2% offset)	81,000 psi
Tensile Strength	108,000 psi
Fuel Rod	
Overall length	44.7 inches
Fill gas	100% helium
Fill gas volume	10.19 cm ³
Initial fill gas pressure	550 psig
Diametral gap	0.008 inch
Equivalent heated diameter	0.75 inch
Hydraulic diameter	0.28 inch
Flow area	0.25 inch ²
Peak-to-average flux ratio	1.35
Inlet pressure	2,080 psig
Inlet temperature	622°F

- (3) An inlet turbine flowmeter located in the lower shroud extension for measuring test rod coolant flow rate.
- (4) Two platinum resistance thermometers located at the shroud inlet and outlet and connected to read the coolant temperature rise directly.
- (5) Two pairs of chromel-alumel (Type K) thermocouples mounted at the shroud inlet and outlet and connected to provide two additional measurements of the coolant temperature rise.
- (6) A chromel-alumel (Type K) thermocouple located at the inlet to the shroud for coolant inlet temperature measurements.

Fig. III-2 PCM-20 PWR-type test rod installed in single rod test hardware.

- (7) A (0 to 3,000 psi) pressure transducer to measure the coolant pressure.
- (8) A (0 to 10,000 psi) pressure transducer to detect any in-pile tube system overpressure resulting from fuel rod failure.
- (9) A cobalt flux wire mounted outside the test rod shroud to measure integrated neutron flux.
- (10) A cobalt self-powered neutron detector (SPND) mounted 29 inches from the bottom of the fuel stack and attached outside of the shroud to measure neutron flux.
- (11) A linear variable differential transformer (LVDT) to measure changes in axial fuel rod length.

1.3 Instrument Performance

The test instrumentation described in Section 1.2.1 provided valid data signals throughout the test with the exception of the two platinum resistance bulb thermometers used to measure coolant temperature rise, the fuel rod internal pressure transducer and the cobalt SPND. In addition, the cladding surface thermocouples while indicating the occurrence of CHF did not provide an accurate indication of the true cladding surface temperature.

The two resistance bulb thermometers and the SPND evidently failed due to moisture infiltration into the transducer lead cables at a splice, causing a short to ground within the cable.

The fuel rod internal pressure transducer suffered a calibration shift after being attached to the fuel rod by welding. The transducer also indicated a slow drift in output signal during the test.

The posttest metallurgical examination indicates that the cladding surface thermocouples acted as cooling fins, resulting in depressed cladding temperatures near the thermocouples. At a given axial elevation, the cladding that was away from the thermocouples reached temperatures several hundred degrees higher than that indicated by the thermocouples.

The cladding surface thermocouples were made of W5% Re/W26%Re solid 5 mil wires. The thermocouple wires are strung with crushable BeO insulator beads and then loaded in zircaloy tubing swaged to 46 mil OD. The BeO insulation near the thermocouple junction end is removed to a depth of about 50 mils and a short piece of 20 mil OD zircaloy wire is inserted, forcing the junction against the zircaloy tubing. The tip is then welded closed. Figure III-3 shows the junction end of the thermocouple. The end of the thermocouple sheath was laser welded for about 100 mils to the zircaloy fuel rod cladding. Small zircaloy

- Sheath Material: Zircaloy - 2 ● Insulation: Compacted BeO
- Thermoelements: W5 Re / W26 Re; Solid, 0.005" Diameter
- Junction: Grounded Plug Type

ANC - A - 4302

Fig. III-3 Sketch of grounded junction thermocouple construction used for cladding surface temperature measurement.

straps, attached at 2-inch intervals, secure the thermocouple leads to the cladding surface. The four cladding surface thermocouples were positioned 90 degrees from each other. Thermocouple extensions consisting of 46 mil OD zircaloy tubing extended from each thermocouple junction to an elevation 20 inches from the bottom of the rod.

1.4 Test Results

The initial part of the test consisted of bringing the loop to desired temperature, pressure, and flow. The nuclear testing consisted of a power calibration portion, followed by four power cycles to determine conditions for the onset of CHF, and to investigate post-CHF fuel rod behavior. The results of the test are presented in the following sections.

1.4.1 Power Calibration Test Results. The primary objective of the power calibration portion of the test was to determine the test rod power generation relative to the PBF core power as indicated by the core ion chamber currents. The test hardware was designed and instrumented to enable the determination of the test rod power by a thermal-hydraulic energy balance across the test rod. The energy balance equation is of the form

$$Q_{in} = \dot{m} (h_{out} - h_{in}) + \dot{m} \left[\frac{v_{out}^2}{2 J g_c} - \frac{v_{in}^2}{2 J g_c} \right] + \dot{m} \left[\frac{z_{out} g}{J g_c} - \frac{z_{in} g}{J g_c} \right] + H_L + Q_{out} - Q_Y \quad (1)$$

and requires measurement of the test rod coolant flow rate, pressure, inlet temperature, and coolant temperature rise for determination of the test rod power. A detailed description of the method is given in Reference III-3.

A small correction factor of approximately 3% was applied for heat losses through the shroud. Coolant conditions during the power calibration portion of the test were 590°F inlet temperature, 2,150 psig loop coolant pressure, and 8.8 gpm flow through the shroud. Power calibration measurements were taken at 10 reactor power levels. Results of the power calibration portion of the test are summarized in Table III-II and shown in Figure III-4. A power calibration conversion factor of 1.31 kW/ft/MW of reactor core power was determined from the enthalpy balance. FRAP-T1^[III-4] predictions of fuel centerline temperature and the change in axial cladding length during the power calibration portion of the test are also included in Table III-II. The predicted fuel centerline temperatures are about 10% higher than the measured values. The predicted cladding axial length change is about a factor of two less than the measured change. A plot of the cladding axial length

TABLE III-II

SUMMARY OF POWER CALIBRATION DATA DURING PCM-20 TEST 8-1RS

Ion Chamber (ma)	Indicated Reactor Power (MW)	Test Rod Power (kW/ft)	Fuel Centerline Temperature (°F)		Cladding Axial Length Change (mils)	
			Predicted	Measured	Predicted	Measured
0	0	0	590	590	0	0
0.19	3.4	5.0	1,170	1,090	6	7
0.31	5.6	7.6	1,457	1,360	9	11
0.39	7.1	9.5	1,690	1,560	12	16.5
0.48	8.8	11.6	1,940	1,750	13	23
0.56	10.3	13.6	2,180	1,900	15	31.5
0.58	10.6	14.2	2,240	2,080	15	32.5
0.65	12.0	15.8	2,400	2,230	16	37.5
0.74	13.5	17.8	2,560	2,400	17	43
0.82	15.1	19.7	2,860	2,580	19	42.5
0.87	15.9	20.7	2,960	2,660	20	40
0.59	10.7	13.9	2,210	2,050	15	17

Fig. III-4 Peak test rod power versus core power during power calibration.

change versus test rod power is shown in Figure III-5. The cladding change in length reached a maximum and then started to decrease. The change in slope is attributed to UO_2 plasticity at high fuel temperatures.

1.4.2 CHF Testing Results. Four separate power cycles were made to investigate the post-CHF behavior of the test fuel rod. In each power cycle incremental power increases of about 1 kW/ft were made for rod powers above 13 kW/ft^[a]. The power was held constant for about 10 minutes at each level. A plot of the test rod power history of the four CHF cycles is shown in Figure III-6. The maximum rod powers reached in each of the power cycles were 19, 21.8, 23.1, and 24.3 kW/ft, respectively. A summary of the nominal system conditions for each power cycle is given in Table III-III. For the first two CHF power cycles an automatic plant shutdown occurred if the test rod cladding surface temperature reached the trip point of 1,000°F. For the final two CHF power cycles, the trip point was increased to 1,500°F.

1.4.2.1 CHF Power Cycle No. 1. Departure from nucleate boiling (DNB) first occurred at the test rod peak power of 15.9 kW/ft. This DNB occurrence was indicated by an increase and subsequent decrease in the fuel centerline temperature. Coincident with this fuel centerline increase and subsequent decrease were slight increases and decreases in cladding surface temperature, fuel rod length and fuel rod internal pressure. A composite plot of test rod behavior for the first power cycle is shown in Figure III-7.

Transition boiling up to 740°F^[b] was sustained for about 12 seconds after the rod power was increased to 17.1 kW/ft. A summary of maximum measured cladding surface temperature, fuel centerline temperature, internal rod pressure, and cladding-axial length change during each step of the power cycle is given in Table III-IV.

About six minutes after the test rod peak power was raised to 18.0 kW/ft, DNB was attained on the test fuel rod cladding surface. Transition boiling up to 810°F was sustained for about 30 seconds as indicated by the 25-inch^[c,d] cladding surface thermocouple. The fuel centerline temperature returned to a value about 100°F lower than it had been prior to CHF, indicating possible fuel restructuring. The power level was held at 18.0 kW/ft for about 19 minutes with no further occurrence of CHF. The cladding axial length gradually decreased about seven mils during this 19-minute period.

[a] All linear power generation rates in kW/ft refer to the axial peak power rating of the fuel rod. The axial power profile has a peak-to-average ratio of 1.35.

[b] All measured values reported are "indicated" by instrument. Actual value of unperturbed parameter may be different.

[c] The 27-inch thermocouple was not functioning properly.

[d] The thermocouple locations are the distance from the bottom of the active fuel.

Fig. III-5 Change in axial cladding length versus peak test rod power.

ANC - A - 6155

ANC - A - 6154

Fig. III-6 Power history for PCM-20 Test 8-1RS.

TABLE III-III.

SUMMARY OF NOMINAL SYSTEM COOLANT CONDITIONS

CHF Power Cycle No.	Inlet Coolant Temperature (°F)	Inlet Coolant Flow (gpm)	Mass Flow (lb/hr-ft ²)	System Coolant Pressure (psig)
1	621	5.1	0.97×10^6	2,080
2	622	5.6	1.06×10^6	2,080
3	621	5.5	1.05×10^6	2,080
4	622	5.4	1.02×10^6	2,080

Fig. III-7 Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-1RS (CHF Power Cycle No. 1).

TABLE III-IV

POST-CHF DATA SUMMARY FOR CHF POWER CYCLE 1, 2, 3, and 4

CHF Power Cycle No.	Fuel Rod Power At Flux Peak (kW/ft)	Number of Indicated Temperature Fluctuations	Maximum Cladding Surface Temperature (°F)[a]	Maximum Fuel Centerline Temperature at 29 inch (°F)	Maximum Fuel Rod Internal Pressure (psig)	Cladding Length Change After CHF Occurred (mils)
1	15.9	1	657 ^[b]	2,450	1,340	2
	17.1	1	740 ^[b]	2,560	1,380	4
	18.0	1	810 ^[b]	2,750	1,410	5
	19.0	2	940 ^[b]	3,100	1,500	17
2	19.9	6	850 ^[b]	2,870	1,500	7
	20.7	3	860 ^[b]	3,375	1,540	13
	21.8	5	980 ^[b]	3,525	1,580	20
3	20.1	11	688 ^[b]	2,915	1,510	7
	21.0	0	652 ^[b]	2,390	1,520	2
	22.1	13	795 ^[c]	4,040	1,600	20
	23.1	1	1,240 ^[b]	4,155	1,685	50
4	17.9	11	670 ^[c]	2,080	1,500	0
	18.9	3	670 ^[c]	2,150	1,520	0
	20.1	12	695 ^[c]	2,380	1,530	0
	21.0	5	700 ^[c]	2,480	1,560	0
	22.1	5	700 ^[c]	2,590	1,600	1
	23.1	15	695 ^[c]	3,630	1,670	15
	24.3	10	1,530 ^[b]	4,120	1,770	90

[a] Measurement accuracy is questionable.

[b] Thermocouple located at 25 inches.

[c] Thermocouple at 29 inches.

CHF occurred again during the test rod power level increase to 19.0 kW/ft. Transition boiling up to 860°F was sustained for about 45 seconds as indicated by the 25-inch thermocouple. The fuel centerline temperature reached 3,100°F during CHF, and then decreased to 2,350°F, about 85°F lower than it had been prior to CHF.

About eight minutes after the first temperature transient at 19.0 kW/ft, a transition boiling fluctuation occurred again. Transition boiling up to 940°F was sustained for about 45 seconds as indicated by the 25-inch thermocouple. The fuel centerline temperature increased to 2900°F and then decreased to a value about 40°F lower than it had been prior to the temperature transient. The test rod power level was then held at 19.0 kW/ft for about five minutes with no further occurrence of CHF. The power level was then decreased completing the first power cycle to CHF. The indicated cladding length, internal fuel rod pressure, and fuel centerline temperature returned to a value of 5 mils less than the original length, 900 psig, and 670°F immediately following shutdown.

1.4.2.2 CHF Power Cycle No. 2. For the second CHF cycle the test rod power was increased in nominal 1 kW/ft increments from 13 kW/ft to 21.8 kW/ft. A composite plot of the test rod behavior for CHF Power Cycle 2 is shown in Figure III-8, and summarized in Table III-IV. The first departure from nucleate boiling occurred at a peak rod

Fig. III-8 Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-1RS (CHF Power Cycle No. 2).

linear heat generation rate of 19.9 kW/ft. Six cladding surface temperature oscillations up to 850°F took place over a span of seven minutes. Since the cladding axial length, fuel centerline temperature, and internal rod pressure signals also fluctuated, it was concluded that the cladding temperature oscillations were indicative of general cladding temperature increases in that region and not just a local fluctuation at the thermocouple location. The cladding surface temperature fluctuations are typical of transition boiling which occurs just beyond the CHF condition before film boiling is fully developed. The maximum fuel centerline temperature, 1,830°F, was reached during the sixth fluctuation. The rod power was then increased to 20.7 kW/ft, and three temperature fluctuations occurred over a span of three minutes. The largest cladding temperature fluctuation was 850°F at the 25-inch thermocouple. The cladding surface temperature then remained in nucleate boiling (652°F) for nine minutes. As the rod power was increased to 21.8 kW/ft, a cladding surface temperature fluctuation up to 710°F occurred during the power escalation. About 45 seconds later a cladding temperature fluctuation reached the control system shutdown setpoint of 1,000°F and the reactor was shutdown. Following the scram the fuel centerline temperature, internal pressure, and cladding axial expansion decreased to values of 670°F, 900 psig, and the original length, respectively.

1.4.2.3 CHF Power Cycle No. 3. During the third CHF power cycle the test rod power was incrementally increased from 13 kW/ft to 23.1 kW/ft. A composite plot of observed test rod behavior is shown in Figure III-9, and summarized in Table III-IV. The onset of CHF occurred as the power was being increased from 19.1 to 20.1 kW/ft. The largest of 11 fluctuations at 20.1 kW/ft reached 688°F cladding temperature. The peak rod power was then increased to 21.0 kW/ft. No indications of CHF were observed during this period of operation. After ten minutes the rod power was increased to 22.1 kW/ft. About one minute after reaching 22.1 kW/ft, a temperature fluctuation up to 795°F occurred lasting about 45 seconds. The thermocouple at 29-inches then indicated a series of 12 temperature fluctuations, whereas the 25-inch thermocouple indicated steady nucleate boiling. The 25-inch thermocouple had previously indicated larger temperatures than the 29-inch thermocouple. The LVDT, centerline fuel temperature, and internal rod pressure transducer also indicated that oscillatory transition boiling took place.

After 11 minutes the rod power was increased to 23.1 kW/ft. After five minutes at this power level, the cladding temperature rose to an indicated 1,240°F at 25 inches, 735°F at 27 inches, 750°F at 29 inches, and 670°F at 31 inches. The fuel rod remained in film boiling for a period of three minutes until the reactor power was manually decreased.

The fuel centerline thermocouple, LVDT, and the internal rod pressure transducer indicated that over the CHF region of the rod, stable film boiling had been reached. The temperature fluctuations indicated by the thermocouples, however, demonstrated that local intermittent film boiling was occurring at the thermocouple junction locations.

1.4.2.4 CHF Power Cycle No. 4. During the fourth CHF power cycle the test rod power was again increased to 17 kW/ft, and then increased in 1 kW/ft increments. At 17.9 kW/ft the first onset of CHF was observed on the 29-inch thermocouple where departure from nucleate boiling temperatures up to 670°F were recorded. Indications of DNB were also observed at 18.9 kW/ft on the 25- and 29-inch thermocouple traces (up to 670°F). At 20.1 kW/ft the largest cladding temperature was 695°F at the 29-inch thermocouple location. The LVDT did not indicate any detectable change in length with these small temperature rises. After 11 minutes the peak rod power was increased to 21.0 kW/ft. Small temperature oscillations up to 700°F at the 29-inch location persisted, although the 25-inch trace showed steady nucleate boiling. The rod power was then increased to 22.1 kW/ft with no apparent change in rod behavior. After 11 minutes the power was increased to 23.1 kW/ft. During this power increase the cladding temperatures and the LVDT fluctuated over a small range. The maximum values of the measured parameters are given in Table III-IV.

During the ten minutes the rod power was at 23.1 kW/ft, the 25-inch cladding temperature was nearly stable at 650°F while the 27-inch cladding temperature was fluctuating up to 695°F. The LVDT indicated cladding length changes up to 15 mils. As the rod power was increased to 24.3 kW/ft the cladding temperatures began to rise. A composite of the fuel cladding temperature at 25-inch and 29-inch, fuel centerline temperature at 29-inch, cladding axial length change, and fuel rod internal pressure is presented in Figure III-10. Indicated temperature fluctuations every 10 to 15 seconds continued for about five

Fig. III-9 Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-IRS (CHF Power Cycle No. 3).

Fig. III-10 Fuel rod power, cladding surface temperature, fuel temperature, axial length, and internal pressure versus time during Test 8-1RS (CHF Power Cycle No. 4).

minutes until cladding temperature at the 25-inch thermocouple location reached the control system shutdown trip point of 1,500°F and the reactor was rapidly shut down. The five largest cladding temperature oscillations at the 25-inch location reached 1,380, 1,410, 1,255, 1,360, and 1,540°F. An average cladding temperature of about 900°F was maintained for 4.5 minutes at the 25-inch location. The cladding temperature at the 27-inch location reached an indicated peak of 720°F. The 29-inch thermocouple indicated a maximum fluctuation of 790°F, and the 21-inch thermocouple a maximum of 670°F. The fuel temperature reached an equilibrium value of 4,120°F and was nearly stable during the time the cladding temperature was oscillating. The LVDT indicated a nearly stable 90 mil change in cladding length. The internal fuel rod pressure reached a maximum pressure of 1,770 psig and was also stable. The LVDT, the fuel centerline thermocouple, and the fuel rod internal pressure transducer signals indicated that near stable film boiling was occurring. The oscillatory temperature behavior suggested by the cladding surface thermocouples may have been a local perturbation at the thermocouple location possibly because of void effects and not indicative of the unperturbed fuel rod cladding behavior.

After the reactor was shut down, the internal fuel rod pressure decreased to about 1,000 psig and then slowly increased, indicating that the fuel rod had developed a leak to the coolant. The slow leak continued for about 2.5 minutes and then the internal pressure suddenly increased to system pressure of 2,080 psig in about 10 seconds. Shortly thereafter, fission products were detected in the loop and various radiation alarms were activated. Further testing was terminated and loop cooldown initiated in preparation for loop cleanup. The test rod total fission energy release was about 310 kW/hr during the power calibration phase and four CHF power cycles. The cladding surface temperature was above nucleate boiling temperatures (652°F) for a total of approximately 17 minutes during the four CHF power cycles.

1.4.3 Preliminary Posttest Examination. After removal from the in-pile tube the fuel rod was transferred to a hot cell where the rod was taken from the shroud. Figure III-11 is a view of the rod after removal from the shroud.

It was found that the cladding had collapsed onto the fuel pellets over a region extending from 19 to 30 inches from the bottom of the rod. Cladding indentations were formed at pellet interfaces over the hot region of the fuel rod. The four thermocouple junctions were still welded to the cladding surface.

The cladding had undergone extensive metal-water reaction and the rod was extremely brittle. A break located at 24-1/4 inches from the bottom of the rod was found when the rod was first removed from its shroud. Another break, located at 21-1/4 inches, occurred as a result of the hot cell handling operations.

Because of the brittle nature of the rod, neutron radiography and gamma scanning were deleted from the posttest examinations. To prevent further damage, the three fuel rod sections were encapsulated separately to minimize handling damage during sectioning and examination. The sections were installed in a secondary stainless steel tube, shimmed as required to provide coaxial alignment and then embedded in epoxy. The sectioning was done as detailed in Table III-V. All samples are located with reference to the bottom end cap.

Fig. III-11 Posttest photograph of PCM-20 Test 8-1RS fuel rod.

Results of the metallographic examination will be discussed in the next quarterly report.

1.4.4 Analytical Evaluation of Posttest Calculations. FRAP-T1^[III-4] is a computer code developed to predict the transient response of nuclear fuel rods during abnormal operating conditions. The coupled effects of mechanical, thermal, internal gas, and material property response on the behavior of the fuel rod are considered. FRAP-T1 is coupled to a material property subcode, MATPRO, which is used to provide gas, fuel, and cladding properties to the FRAP-T1 computational subcodes. The code is restricted to zircaloy-clad uranium oxide fuel rods.

The FRAP-T1 code (MOD002EXP-04A; MAT PRO MOD 02) was used to calculate the fuel rod behavior for PCM-20 Test 8-1RS. The thermal-hydraulic conditions for the last step of CHF power cycle No. 4 at a peak rod power of 24.3 kW/ft were input to the code. In the calculation the peak test rod power was linearly increased from 17 kW/ft to 24.3 kW/ft in 10 seconds.

TABLE III-V

SECTIONING PLAN FOR PCM-20 8-1RS TEST ROD^[a,b]

Section Location from Bottom of Rod (inches)	Type Section ^[c]	Purpose
2	T	Remove bottom end cap
38-1/2	T	Remove top end cap
7-3/4 to 8-1/4	T	Examine fuel and cladding properties at low exposure
8-1/4 to 8-3/4	T	Obtain chemical burn-up sample
17-3/4 to 18-1/4	T	Examine fuel and cladding properties at moderate exposure
18-1/4 to 18-3/4	T	Obtain chemical burn-up sample
18-3/4 to 19-1/2	L	Examine sharp temperature transition zone
20-3/4 to 21-1/4	T	Examine fuel and cladding properties at high exposure
Location to be determined 22-3/4 to 23-1/2	L	Examine end of centerline void
24-1/4 to 25	L	Examine cladding collapse between pellets
25-7/8 to 26-3/8	T	Examine surface TC-1 condition
26-3/4 to 27-1/2	L	Examine possible defect in cladding
27-3/4 to 28-1/4	T	Examine surface TC-2 condition
29-3/4 to 30-1/4	T	Examine surface TC-3, centerline TC, and strap condition
30-1/4 to 31	L	Examine collapse between pellets
31-7/8 to 32-3/8	T	Examine cladding, fuel, TC, and holddown strap properties
33-1/2 to 34	T	Obtain chemical burn-up samples

[a] Attempt fractography at breaks in rod via photography and silicone rubber replication.

[b] In addition to conventional metallography also determine micro-hardness.

[c] T = Transverse and L = Longitudinal.

The geometrical model to describe the 36-inch long by 0.422-inch diameter fuel rod in the FRAP-T1 calculations consisted of 15 axial nodes and 11 radial nodes. The 72 mil centerline thermocouple hole was simulated by specifying a zero power generation for that region for the full length of the fuel stack. The four 46 mil OD cladding surface thermocouples were not included in the calculational model, since FRAP-T1 has no provision for explicitly including the thermocouples.

The calculations were made for the following set of conditions.

Experiment Flow - 5.4 gpm

Inlet Temperature - 622°F

System Pressure - 2,080 psig

Flow Shroud ID - 0.704 inch

Plenum Volume - 10.2 cm³

The B&W-2 CHF^[III-5] correlation and the W-3 CHF^[III-6] correlation were each combined with the following post-CHF correlations: Groeneveld (5.7 Version)^[III-7]; Groeneveld (5.9 Version)^[III-7]; and Tong-Young^[III-8] as part of the FRAP-T1 model.

The results from these calculations are compared with measured and preliminary estimates of fuel rod cladding temperatures from posttest examination of the fuel rod in Table III-VI. Results presented in Table III-VI show that the FRAP-T1 code prediction, using the Groeneveld (5.7 Version) post-CHF correlation was in reasonable agreement with the preliminary test results. The FRAP-T1 predictions using the Groeneveld (5.9 Version) correlation were conservative with respect to the preliminary test results.

1.4.5 Discussion. The extent of metal-water reaction indicates that the actual cladding surface temperature was in excess of 2,000°F. The maximum indicated temperature from the cladding surface thermocouples was 1,530°F during the last temperature oscillation. The discrepancy is attributed to the presence of the 46 mil OD thermocouple sheath leads and method of installing thermocouple junctions to the cladding. The four thermocouple leads acted as cooling fins tending to decrease the temperature at the thermocouple location. The thermocouple sheath has a wall thickness of about 8 mils. Therefore, the thermocouple junction is located at least 10 mils from the cladding surface, so that lower than actual cladding temperatures were detected for large cladding temperature gradients than exist during film boiling heat transfer. The post-CHF oscillatory cladding temperature behavior, as indicated by the cladding thermocouples, may be due to fluctuations in the thickness of the steam layer surrounding the cladding, which resulted in intermittent nucleate boiling and film boiling occurring at the thermocouple locations.

After film boiling conditions occurred, the cladding surface that was not perturbed by the thermocouples remained in stable film boiling. The fuel centerline temperature, axial length change, and internal rod pressure data indicate that the unperturbed cladding surface temperature was in stable film boiling during the highest power levels of CHF power cycle No. 3 (23.1 kW/ft) and power cycle No. 4 (24.3 kW/ft). Improved thermocouples and installation techniques are currently being developed.

TABLE III-VI

COMPARISON OF MEASURED, ESTIMATED, AND CALCULATED FUEL ROD BEHAVIOR

Parameter	Maximum Measured During Test	Estimated From Fuel Rod Posttest Condition	FRAP-T1 Predictions Using These CHF and Post-CHF Correlations					
			W-3 and Tong-Young	W-3 and Groeneveld 5.7	W-3 and Groeneveld 5.9	B&W-2 and Tong-Young	B&W-2 and Groeneveld 5.7	B&W-2 and Groeneveld 5.9
Cladding surface temperature at 25 inches (°F)	1,530	2,200	1,875	2,150	2,525	1,850	2,175	2,520
Maximum cladding surface temperature	---	2,560	2,300	2,450	2,900	2,100	2,350	2,800
Fuel centerline temperature at 29 inches (°F)	4,155	Less than UO ₂ melting (5,144)	3,800	4,250	4,550	3,800	4,275	4,525
Maximum fuel centerline temperature (°F)		Less than UO ₂ melting	5,600	5,950	6,300	5,200	5,800	6,100
Fuel rod internal pressure (psig)	1,770	Less than 2,080	1,630	1,630	1,650	1,630	1,630	1,630
Axial length change after CHF (mils)	90	Not possible	220	315	510	110	230	285

It is of interest to note that high cladding temperatures associated with film boiling were not sustained until the peak rod power was increased to 23.1 kW/ft. Moderate temperature oscillations associated with transition boiling were detected at least three kW/ft less linear heat rating than that required for film boiling. Predictions using the FRAP-T1 code with the Groeneveld (5.9 Version) post-CHF correlations were in reasonably good agreement with the preliminary results of the test.

The preceding statements apply only to this particular PCM experiment and no recommendation of the particular correlations for use in commercial PWRs is implied or intended. Following several PCM-type experiments a Model Verification Report is expected to be published that will address recommended code and correlation application. The results from the PCM-20 Test Series will be used to verify analytical models. Later tests in the series will investigate the behavior of small clusters of fuel rods. It should be cautioned that the direct application of the results from this single rod test to predict fuel rod behavior in a commercial PWR during a postulated over-power accident would be questionable.

2. IRRADIATION EFFECTS TEST SERIES

FUEL ROD CHARACTERIZATION EXAMINATION

W. C. Francis, G. W. Gibson, M. R. Martin, W. J. Quapp

The objectives of the Irradiation Effects Test Series is to evaluate the behavior of irradiated fuel rods under PCM conditions. Included will be analyses of radiation-induced mechanical property changes 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^[III-1].

For these tests it is necessary that the fuel rods be completely characterized and contain no unknown defects. Characterizing information is needed both before and after in-pile testing. As part of the effort designed to obtain this information the pulsed eddy current (PEC) examination of the cladding was used^[a]. This technique was developed at Argonne National Laboratory^[III-9,-10,-11] and used successfully by the General Electric Company^[III-12] for inspection of stainless steel cladding. The technique provides a method of (a) detecting microcracks located in the inner and outer surfaces of the cladding, (b) detecting fuel-cladding interaction, and (c) determining cladding thickness. In addition, the system provides information on fuel rod diameter variation and fuel rod bowing.

[a] A complete description of the Pulsed Eddy Current system that was developed and is being used at INEL is contained in ANCR-1259, Inspection System for Zircaloy Clad Fuel Rods, which is scheduled for distribution in October, 1975.

Standards were fabricated for use in calibrating the system. The standards included measurements of transverse and longitudinal defects, wall thicknesses, and diameter variations.

As part of the characterization examinations, two irradiated Saxton fuel rods (identified as 837 and 864) and one unfueled water tube^[a] (STD-50) were chosen for destructive examination. The selection of these rods and the tube was made chiefly on the results of the pulsed eddy current examinations which had been conducted on the rods. The destruction examination was carried out to provide correlation of PEC defect indications with physical evidence, and to provide fuel/cladding metallography, cladding fluence and fuel burnup analysis, and burst test specimens. Sectioning diagrams for all three items are shown in Figures III-12, -13, and -14. Both of the rods had been irradiated in Core III of the Saxton reactor. Rod 837, which had been prepressurized with helium to a pressure of 365 psia, was irradiated to an average burnup of 17,130 MW/MTU. Rod 864 had an internal atmosphere of air at 15 psia and had been irradiated to an average burnup of 17,880 MW/MTU.

The determination of specimen positions for Rods 837 and 864 was based on the defect indications and their circumferential extent on the original PEC charts. The specimen positions on water tube STD-50 were based on a trial mapping technique developed specifically for this application. Two examples of this technique (termed "pseudo-maps") are shown in Figures III-15 and -16. Correlations of size, extent, and position of defect indications are greatly simplified by this method of data presentation, and a detailed overview of the condition of the rod is made available which could not be achieved by any other method used to date.

Correlation of PEC defect indications with physical evidence was generally good. The "defects" consisted of wear marks from the spacer grids, scratched and scuffed surfaces from either fabrication or handling, adherent patches of corrosion products which survived the decrudding operations, and subsurface concentrations of hydride. None of the PEC defect indications examined (with one exception noted below) would constitute a cladding-integrity problem. The majority of mechanical-deformation related "defects" (as opposed to crud or hydride) were 0.001 inch or less in depth, but showed considerable relative width. This implies that the magnitude of the PEC indication may be sensitive to "defect" width as well as depth. Similarly, small patches of adherent corrosion products result in momentary liftoff of the PEC sensors causing a "blip" on the chart. Concentrated hydride located just below the OD surface of the cladding appears to have also caused a "defect" signal.

A major indication was found in the PEC scans of fuel Rod 837 at a position approximately 12-1/2 inches above the bottom end cap. This "defect" extended from 340

[a] The "water tubes" were zircaloy-clad tubes used in the unfueled locations of the extended burnup assemblies of Saxton Core III as spacers in some of the fuel assemblies. Holes were drilled in the top and bottom of the tubes to permit the passage of water.

Fig. III-12 Sectioning diagram load follow Rod No. 837.

Fig. III-13 Sectioning diagram load follow Rod No. 864.

Fig. III-14 Sectioning diagram - water tube STD 50.

Fig. III-15 Pseudo-map of defect indications on water tube STD 50. M-designations show areas destructively examined.

Fig. III-16 Pseudo-map of defect indications on water tube No. 5.

to 80 degrees on the PEC scans. Similar indications, however, did not appear on the knife-blade profilometer scans made at an earlier date, prior to extensive decruding. Figures III-17 and -18 show the affected area and the extent of wall thinning and corrosion product buildup observed.

The cladding thickness at the thinnest point was approximately 0.002 inch. This condition represents grounds for rejecting the rod from future testing, and was readily apparent on the PEC scans. The cause is believed to have been a galvanic cell, established between the zircaloy cladding and a carbon steel support rack, with the decruding solution as the electrolyte. Particularly, in view of the lack of such an indication on the previously taken profilometer scans, there is good reason to believe that the rod did not come from the Saxton reactor in this condition. Metallographic Specimen 837 M-1 was cut from Rod 837 at this defect location.

Specimen 837 M-3, a transverse specimen centered at 1.75 inches, revealed several very small OD scratches (approximately 0.002 inch deep) which are beyond the resolving power of the PEC system, and one OD groove approximately 0.001 inch deep by 0.004 inch wide in the area of interest (Figure III-19).

Transverse Specimen 837 M-4 was removed from an area which corresponded to the spacer grid and showed probable indications of wear, with lesser indications in other areas. Examination revealed one of the worn areas (Figure III-20) as a flat-bottomed groove approximately 0.0005 inch deep and 0.006 inch wide. The remaining indications appear to have resulted from subsurface hydride, concentrated in the outer 0.003 inch of material (Figure III-20). Very shallow grooves were apparent approximately 90 degrees from those shown in Figure III-21.

A typical summary chart from Rod 864 is shown in Figure III-22. Surface (OD) indications are observed extending about three inches at the 20 to 23 inch region (measured from the bottom of the rod). Similar chart scans taken at 10 degree increments around the rod indicated that this region extended nearly around the rod and was quite pronounced over about 150 degrees of the circumference. Other OD indications appear scattered over much of the length of the fuel rod. Figure III-23 attempts to illustrate graphically the extension surface indications observed in the PEC charts. This pseudo-map covers only the region from 320 to 50 degrees, and includes all of the possible indications which could be identified. By comparison with the calibrating standards, the depth of these indications ranged primarily from 0.002 to 0.004 inch with a few in the 0.006 inch range. Only a few scattered ID indications were noted, all below the threshold for significance. Cladding thickness changes of 0.0006 to 0.0008 inch, particularly in the 230 to 300 degree region, were readily associated with the OD indications. Throughout much of the length, diametral variations are pronounced (± 0.010 inch). It was because of the extensive indications observed in the PEC scanning that Rod 864 was selected for destructive examination. Some indication of the sensitivity of the PEC scanning is illustrated in Figure III-24. In this figure is shown a thin (approximately 0.002 inch thick) layer of corrosion product found on the surface of the fuel rod.

Defect in Clad 75-B-1

Mounting Material

Zircaloy

UO₂

Zircaloy

Mounting Material

5.5X

As Polished

Fig. III-17 Longitudinal Section 837 M-1 prepared from Rod 837 showing defect of cladding.

75-B-2

Mounting Material

Corrosion Product

Zircaloy

UO₂

200X

As Polished

Fig. III-18 Micrograph of surface defect found in fuel Rod 837 Section 837 M-1, showing bottom of pit.

75-B-30

Mounting Material

Zircaloy

200X

As Polished

Fig. III-19 Micrograph of defect found on OD surface of fuel Rod 837 in Specimen 837 M-3.

75-B-27

200X

As Polished

Fig. III-20 Micrograph of surface defect found on OD of fuel Rod 837 in Specimen 837 M-4.

Fig. III-21 Micrograph of cladding of fuel Rod 837.

Three of the five metallography specimens from Rod 864 were taken for the express purpose of correlating with PEC-derived data. Specimen 864 M-1, a transverse specimen centered at 22.65 inches from the bottom of the rod, was selected to view the area described above. Examination of this area (Figure III-24) revealed isolated accumulations of corrosion product buildup in several areas, and shallow hydriding less than 0.0015 inch below the outside surface. None of the remaining specimens exhibited either of these phenomena. The corrosion products observed were generally between 0.015 and 0.025 inch across (viewed transversely), and had obviously survived the decrudding treatment. It has been demonstrated (on other rods) that either condition can result in "defect" indications.

Metallography Specimen 864 M-3, also a transverse specimen, was taken from an area which showed the greatest ovality on the diametral portion of the scans. Although not significant, defects were also indicated in this area. The section showed a variation of approximately 1.5% in the diametral measurements, which is in good agreement with the scanner results. No defects were observed during the polish/examination cycles, although a comparatively small amount of shallow hydriding was observed.

A longitudinal specimen, centered at 2.5 inches from the bottom of the rod, was taken in an area where the PEC traces showed a step as opposed to a blip or spike indication. The step in the traces applied to both the OD and ID traces and occurred on the 70, 80, and 90 degree scans. Although it was felt that the step was probably an electro-mechanical anomaly, the area was selected to verify the assumption. As expected, no indications were found.

Fig. III-22 Simplified defect chart of load follow fuel Rod No. 864.

Fig. III.23 Partial pseudo-map of load follow Rod 864.

75-B-40

Fig. III-24 Micrograph of cladding of fuel Rod 864 showing corrosion product on surface.

No indications of cracks, corrosion buildup, etc., were found in any of the samples other than the regions where the PEC indicated defects to be located.

Three metallographic samples were taken from the water tube in the areas marked in Figure III-15. Specimens S50 M-1 and S50 M-2, covering regions of extensive OD and ID defects respectively, were prepared with longitudinal surfaces exposed. Specimen S50 M-3 was a transverse section through an area containing several apparently unrelated defects.

The region of interest in Specimen S50 M-1 showed a small scuff mark (approximately 1/16 inch across by 1/8 inch long) on the OD surface prior to mounting. Examination of the polished metallography specimen indicated extremely localized variations in the OD surface ranging from 0.001 inch below to 0.002 inch above nominal surface.

A disturbed area on the ID surface of Specimen S50 M-2 was found to extend only 0.008 inch into the wall at its deepest point (Figure III-25).

The size of the defect seems too small compared to the indications on the PEC scan and pseudo-map. It appears that either the deepest part of the defect was polished through during the repetitive polish/examine cycle used in metallography or the length of the disturbed area produced a relatively large indication. A second, very small ID defect (approximately 0.0005 inch deep) with no surrounding distortion, located approximately 180 degrees from the first defect, was found. This defect appears, however, to have been too

75-B-20-2

Mounting Material

Inside Surface

Zircaloy

200X

As Polished

Fig. III-25 Micrograph of 0.0008 inch deep defect found on inside of water tube number STD-50 at 215 degree rotation in Sample S50 M-2.

small for the PEC to pick up, since no indication could be found on the PEC traces. With the exception of some surface roughening (which was interpreted as noise on the PEC system) on a portion of the ID surface of Specimen M-2, this is the only example of a defect observed in metallography that did not appear in the PEC charts.

Specimen S50 M-3 was taken to evaluate the OD indication shown on the pseudo-map (Figure III-15) at 200 degrees and approximately 28 inches from the bottom of the rod. The defect indication proved to be two flat-bottomed longitudinal grooves. Each groove (Figure III-26) was approximately 0.005 inch wide by 0.00075 inch deep, and separated from its companion by 0.008 inch. The ID indications proved to be relatively minor deformations with an apparent longitudinal orientation. The 0-degree/28-inch indication corresponds to the irregularities shown in Figure III-27. Micrographs are only a few thousandths of an inch apart, and are typical of what was observed in the other two areas.

The other metallographic examinations showed no fuel-cladding chemical interaction in the fueled rods, and the central void formation in fuel Rod 864 appears to be the result of void migration at temperatures below the fuel melting temperature. Some evidence was found, however, which showed partial fusion of adjacent pellet faces in Rod 837.

3. CONCLUSIONS

The pulsed eddy current system has proved to be an excellent technique for nondestructive examination of irradiated fuel rods. The contribution of the PEC

75-B-13

Fig. III-26 Micrograph of grooves found on OD of water tube STD-50 in Sample S50 M-3 at 200 degree rotation.

examination to the overall fuel rod characterization program is considerable and does provide important information on the mechanical condition of a fuel rod from which a selection of test rods for IES testing can be made.

With few exceptions, the cladding examined was relatively free of any large defects which might compromise use in the IES tests.

Unfueled zircaloy tubes irradiated in the same environment as fueled tubes show appreciably less damage, emphasizing, as may be expected, the added damage associated with the mechanical, chemical, and thermal effects of fuel rod irradiation.

Defect indications observed by PEC were resolved to be primarily small grid wear marks and corrosion on the outer surface. Bowing, however, on some rods and water tubes was quite pronounced, in some cases exceeding 0.020 inch peak-to-peak. Cladding thickness variations noted were small, less than 0.001 inch for most of the tubes and only a few fuel rods had up to 0.003 inch variation.

Fuel-cladding mechanical and chemical interaction was not observed by the PEC system. It must be noted, however, that suitable standards were not available for this type of calibration.

75-B-15

Mounting Material

Inside Surface

Zircaloy

200X

As Polished

75-B-15-2

Mounting Material

Inside Surface

Zircaloy

200X

As Polished

Fig. III-27 Micrographs of ID defects found in water tube STD-50 in Sample S50 M-3 at 0 degree rotation.

4. REFERENCES

- III-1. Saxton Nuclear Experimental Corporation, *Application to U.S. Atomic Energy Commission for Reactor Construction Permit and Operating License, Final Safeguards Report*, NT 10094 (1961) (Amendments and additional information regarding the Saxton Experimental Reactor Program are filed under DOCKET-50146).
- III-2. T. G. Odekirk, *Detailed Test Plan Report for PBF Test Series PCM-20: The Behavior of Unirradiated PWR Fuel Rods Under Power-Cooling-Mismatch Conditions*, ANCR-1095 (April 1974).
- III-3. R. K. McCardell et al, *Power-Coolant Mismatch Series Power Calibration Test*, ANCR-1248 (July 1975).
- III-4. J. A. Dearien and L. J. Siefken, *FRAP-T: A Computer Code for the Transient Analysis of Oxide Fuel Rods, Volume I, FRAP-T Analytical Models and Input Manual*, I-243-3-51.1 (July 1974).
- III-5. J. S. Gellerstedt et al, "Correlation of Critical Heat Flux in a Bundle Cooled by Pressurized Water", of *Two-Phase Flow and Heat Transfer in Rod Bundle Symposium*, ASME Winter Annual Meeting (November 1969) pp 63-71.
- III-6. L. S. Tong, "Critical Heat Fluxes in Rod Bundles", of *Two-Phase Flow and Heat Transfer in Rod Bundles Symposium*, ASME Winter Annual Meeting (November 1969) pp 31-46.
- III-7. D. C. Groeneveld, *An Investigation of Heat Transfer in the Liquid Deficient Regime*, AECL-3281 Rev. 1 (December 1968), Rev. 2 (August 1969).
- III-8. L. S. Tong and J. D. Young, "A Phenomenological Transition and Film Boiling Heat Transfer Correlation", *Heat Transfer, 5th International Heat Transfer Conference*, Japan, IV (1974) pp 120-124.
- III-9. C. J. Renken, *Progress Report on Nondestructive Testing by Electromagnetic Methods*, ANL-6414 (July 1962).
- III-10. C. J. Renken, *A Pulsed Electromagnetic Test System Applied to the Inspection of Thin-Walled Tubing*, ANL-6728 (March 1964).
- III-11. T. H. Busse, R. R. Wood, N. S. Beyer, *Nondestructive Inspection of EBR-II Fuel Jacket Tubing Using Electromagnetic Techniques*, ANL-7334 (May 1967).
- III-12. R. R. Asamoto, R. G. Bacon, A. E. Conti, G. P. Wozadlo, *Evaluation of Irradiated Fuel Rods with Pulsed Eddy Current*, GEAP-B928 (December 1972).

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 code development, fuel code development, code verification, and code optimization. The program produces advanced 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.

2. ADVANCED LOCA CODE DEVELOPMENT

Development of the SCORE, SPLEN, and SLOOP codes continued during the final quarter of FY 1975. In the core code development effort a numerical technique for phase interface tracking under low speed homogeneous two-phase flow was developed. In the plenum code area, a two-parameter differential model of turbulence was developed, as well as a numerical scheme for solving the resulting equations. Under loop code development, a scheme has been outlined concerning incorporation into the UVUT code of the computation of additional two-phase flow theories. These include: (a) unequal phase velocity unequal phase temperature (UVUT), (b) unequal velocity equal temperature (UVET), (c) the drift flux versions of these two theories (UVUT-DF and UVET-DF), (d) a simple two-pressure version of UVUT, UVET, UVUT-DF, and UVET-DF, and (e) equal velocity equal temperature (EVET).

The following describe the foregoing activities in detail.

2.1 Core Code Development Work

(J. D. Ramshaw)

A number of problems of practical importance involve the low-speed flow of a single-component two-phase fluid (e.g., water-steam) with a large phase density ratio and a moving phase interface. In nuclear reactor safety analysis, such problems arise in connection with the reflooding of a steam-filled reactor core with liquid water from the emergency core cooling system following a loss-of-coolant accident (LOCA).

The numerical solution of such problems is hindered by the fact that the field variables do not vary slowly in comparison to practical finite-difference mesh spacings -- enormous gradients of density and internal energy occur at the phase interface. In general, it is known that even moderately large gradients lead to inaccurate results in Eulerian fluid flow calculations because of excessive numerical diffusion or smearing^[IV-1,-2]. In addition,

large density and energy gradients, in combination with slight numerical discrepancies between the mass and energy transport calculations, can cause unacceptable errors in pressure and velocity^[IV-3]. Under core reflood conditions, the gradients are so extreme that special care must be taken to avoid these problems.

A number of methods have been developed for the numerical treatment of fluid interfaces (see Reference IV-4 and references cited therein). Most of these methods are somewhat complicated and cumbersome to implement, and are not directly applicable to single-component two-phase interface problems, in which mass exchange between phases may spontaneously occur during the flow process.

A numerical technique was developed capable of calculating phase interphase movement under low Mach number two-phase flow. The use of donor-acceptor differencing to prevent interface smearing is an essential feature of the numerical technique. Another essential feature is the conversion of the energy equation into a form which does not involve a convective derivative. This approach has the advantage of eliminating any possibility of numerical inconsistencies between the mass and energy transport calculations. The pressure and velocity errors which might otherwise arise from this source are thus prohibited and overall accuracy is enhanced considerably, in spite of the loss of rigorous momentum conservation. This technique was incorporated into the SCORE-EVET Code (SCORLS). However, because of the manner in which the momentum and energy equations are treated, donor-accepting differencing is needed and used only in the continuity equation. The remainder of the numerical technique is more or less conventional and is closely related to the method presently used in the SCORE-EVET Code. The pressure gradient in the momentum equation, the velocity in the continuity equation, and the velocity divergence in the energy equation are treated implicitly. The remaining terms in the equations are treated explicitly. The implicit part of the scheme is solved by an iterative procedure very similar to that of SCORE-EVET.

The DA technique does not resolve the interface in detail within a computational cell, and therefore is not suitable for calculating physical effects (such as surface-wave instability) whose accurate representation requires a detailed knowledge of the interface configuration.

Two sample calculations were made with SCORLS: (a) calculation of the liquid-vapor interface in a vertical pipe subjected to a constant pressure drop in the absence of viscous forces and (b) water sloshing in a two-dimensional rectangular container initially divided into half liquid and half vapor. The SCORLS results for problem No. 1 are in excellent agreement with the results of an exact solution of this problem (see Figures IV-1 and IV-2). No analytical solution is available for problem No. 2, but the numerical results displayed in Figures IV-3a through IV-3f clearly show the qualitative sloshing behavior which one would intuitively expect.

The capability of the SCORLS code to model phase interface movement under adiabatic conditions has been adequately demonstrated. Future calculations with SCORLS will involve the actual quench front modeling under core reflood conditions.

Fig. IV-1 Velocity oscillations in vertical pipe under constant pressure drop. Solid line -- exact solution; circles = code results.

2.1.1 Numerical Method Description. The following is a detailed description of the numerical method, including two sample check-out calculations.

The basic equations for the transient flow of a single-component homogeneous two-phase (gas-liquid) fluid are

$$\rho \frac{D\mathbf{u}}{Dt} = -\nabla p + \nabla \cdot \underline{\underline{\tau}} + \rho \mathbf{g} \quad (1)$$

$$\frac{D\rho}{Dt} = -\rho \nabla \cdot \mathbf{u} \quad (2)$$

$$\rho \frac{DI}{Dt} = -p \nabla \cdot \mathbf{u} + \underline{\underline{\tau}} : \nabla \mathbf{u} - \nabla \cdot \mathbf{J} \quad (3)$$

$$\rho = f(p, I) \quad (4)$$

Fig. IV-2 Logarithmic density profiles at $t = 0.7, 1.5$, and 2.3 sec.

ANC-A-6190

Fig. IV-3a Sloshing problem at time = 0.0 sec.

Fig. IV-3b Sloshing problem at time = 0.200 sec.

Fig. IV-3c Sloshing problem at time = 0.400 sec.

Fig. IV-3d Slicing problem at time = 0.60 sec.

Fig. IV-3e Slicing problem at time = 0.800 sec.

Fig. IV-3f Sloshing problem at time = 1.00 sec.

where ρ is the mass density, $\underline{u}$ the velocity, p the pressure, $\underline{\tau}$ the viscous (deviatoric) stress tensor, $\underline{g}$ the acceleration of gravity, I the specific internal energy, and $\underline{J}$ the heat flux vector. The symbol D/Dt represents the convective derivative $\partial/\partial t + \underline{u} \cdot \nabla$. Equations (1) through (4) must, of course, be supplemented by constitutive equations for $\underline{\tau}$ and $\underline{J}$, normally the Newtonian Law for $\underline{\tau}$ and the Fourier Law for $\underline{J}$.

The state relation (4) applies in both the single-phase and the two-phase region. The derivatives $(\partial \rho / \partial I)_p$ and $(\partial \rho / \partial p)_I$ are discontinuous at the boundary between these regions. Several auxiliary state variables and relationships exist. For any pressure p less than the critical pressure there are well-defined liquid and gas saturation densities $\rho_l(p)$ and $\rho_g(p)$. The quality x and void fraction α are then defined in terms of these densities by the relations

$$\alpha = \begin{cases} 0 & \text{if } \rho \geq \rho_l \\ \frac{\rho - \rho_l}{\rho_g - \rho_l} & \text{if } \rho_g < \rho < \rho_l \\ 1 & \text{if } \rho \leq \rho_g \end{cases} \quad (5)$$

$$x = \left[1 + \frac{\rho_l}{\rho_g} \left(\frac{1}{\alpha} - 1 \right) \right]^{-1} \quad (6)$$

The local mass fraction of the gas phase is x , and the local volume fraction is α .

The energy Equation (3) is expressed below in an alternative form which will be convenient for numerical solution by finite-difference methods. This may be done by substituting Equation (4) into Equation (1) and eliminating DI/Dt from the result using Equation (3). The net result is

$$(\nabla \cdot \underline{u}) \left[p - \rho^2 \left(\frac{\partial \rho}{\partial I} \right)_p \right] = \left(\frac{\partial \rho}{\partial I} \right)_p (\underline{\tau} : \nabla \underline{u} - \nabla \cdot \underline{J}) + \rho \left(\frac{\partial \rho}{\partial p} \right)_I \frac{Dp}{Dt} \quad (7)$$

Equation (7) is really a combination of the energy, continuity, and state equations, but can be regarded as an equivalent form of the energy equation to replace Equation (3) while retaining Equations (1) and (4).

At this point it is convenient to restrict attention to the case of small Mach number. This case may be realized by assuming the isentropic sound speed $C = \sqrt{(\partial p / \partial \rho)_s}$ to be very large. This assumption, together with the thermodynamic relation

$$\rho \left(\frac{\partial \rho}{\partial p} \right)_I \left[p - \rho^2 \left(\frac{\partial \rho}{\partial I} \right)_p \right]^{-1} = - \frac{1}{\rho C^2} \quad (8)$$

implies that the last term in Equation (7) becomes negligibly small and can be omitted. Equation (7) then reduces to

$$\nabla \cdot \underline{u} = \left[p - \rho^2 \left(\frac{\partial I}{\partial \rho} \right)_p \right]^{-1} (\underline{\tau} : \nabla \underline{u} - \nabla \cdot \underline{J}) \quad (9)$$

which may be regarded as an incompressibility condition. Note that when ρ is independent of I (or, alternatively, in the absence of viscosity and heat conduction), Equation (9) reduces to $\nabla \cdot \underline{u} = 0$, the usual incompressibility condition for the case in which ρ is constant for a given fluid particle. Equation (9) is the generalization of this condition to allow for local thermal expansion or contraction. This interpretation becomes intuitively clear when it is noted that $\underline{\tau} : \nabla \underline{u} - \nabla \cdot \underline{J}$ is the local volumetric rate of energy buildup, and that

$$p - \rho^2 \left(\frac{\partial I}{\partial \rho} \right)_p = \left(\frac{\partial h}{\partial v} \right)_p \quad (10)$$

where h is the specific enthalpy and $v \equiv 1/\rho$ is the specific volume.

The basic equations for the transient flow of a single-component homogeneous two-phase fluid at small Mach number may now be taken to be Equations (1), (2), (4), and (9). The restriction to small Mach number implies that these equations do not describe pressure-wave propagation effects and cannot be used in problems where such effects are important.

2.1.2 The Numerical Scheme. The numerical solution of Equations (1), (2), (4), and (9) is affected by constructing finite-difference analogs of them. For clarity it is convenient to begin by exhibiting the temporal differencing of Equations (1), (2), (4), and (9) explicitly while suppressing the spatial differencing:

$$p^n \left(\frac{\underline{u}^{n+1} - \underline{u}^n}{\Delta t} + \underline{u}^n \cdot \nabla \underline{u}^n \right) = - \nabla p^{n+1} + \nabla \cdot \underline{\tau}^n + \rho^n \underline{g} \quad (11)$$

$$\nabla \cdot \underline{u}^{n+1} = \frac{\underline{\tau}^n : \nabla \underline{u}^n - \nabla \cdot \underline{J}^n}{p^n - (\rho^n)^2 \left(\frac{\partial I}{\partial \rho} \right)_p^n} \quad (12)$$

$$\frac{\rho^{n+1} - \rho^n}{\Delta t} + \nabla \cdot (\rho^n \underline{u}^{n+1}) = 0 \quad (13)$$

$$\rho^{n+1} = f(p^{n+1}, I^{n+1}) \quad (14)$$

where Δt is the time increment and A^n denotes the difference analog of the quantity A at time $n\Delta t$. It is understood that all spatial derivatives in Equations (11) through (13) are to be replaced by spatial differences in a manner yet to be described. The sequence of a calculational cycle is as follows. Equations (11) and (12) are first solved simultaneously, by an iterative procedure, to obtain p^{n+1} and $\underline{u}^{n+1}$. Equation (13) is then solved for ρ^{n+1} . Finally, I^{n+1} is obtained by inversion of Equation (14).

With the exception of the convective term $\nabla \cdot (\rho^n \underline{u}^{n+1})$ in Equation (13), which is differenced according to a modified donor-acceptor rule, the spatial differencing of Equations (11) through (13) is closely patterned after that of Hirt and Cook^[IV-5]. The region of computation is divided into small finite-difference cells of dimensions Δx , Δy , and Δz and labeled by integer indices (i, j, k) . The thermodynamic variables p , ρ , and I are defined at cell centers while the velocity components (u, v, w) are defined at cell faces.

Except for two minor differences, the spatial differencing of Equation (11) is identical to that of the corresponding equation in Reference IV-5, and hence will not be displayed in detail. The first difference is that the density is not constant in the present context; the densities in Equation (11) are evaluated at the cell face on which the velocity component in question is defined, by averaging the adjacent cell-centered densities. For example, in the calculation of $v_{i,j+1/2,k}^{n+1}$ the density ρ^n in Equation (11) is evaluated as

$$\frac{1}{2} (\rho_{i,j,k}^n + \rho_{i,j+1,k}^n) \equiv \rho_{i,j+1/2,k}^n$$

The second difference is that, unlike Hirt and Cook, there is no limitation to centered space differencing of the convective terms in Equation (11). Instead, a weighted average between centered and donor-cell differencing^[IV-3,-6] is allowed for. The weighting factor may be chosen to nullify a destabilizing truncation error^[IV-3,-7] so that viscosity is no longer required for numerical stability.

Equation (12) is evidently a cell-centered equation; all terms in it are evaluated at cell centers, the spatial derivatives being represented by simple centered differences. For example,

$$\begin{aligned} [\nabla \cdot \underline{u}^{n+1}]_{ijk} = & \frac{1}{\Delta x} (u_{i+1/2,j,k}^{n+1} - u_{i-1/2,j,k}^{n+1}) + \frac{1}{\Delta y} (v_{i,j+1/2,k}^{n+1} \\ & - v_{i,j-1/2,k}^{n+1}) + \frac{1}{\Delta z} (w_{i,j,k+1/2}^{n+1} - w_{i,j,k-1/2}^{n+1}) \end{aligned} \quad (15)$$

where the notation $[A]_{ijk}$ represents the spatial difference approximation to the quantity A at the center of cell (i,j,k) .

The iterative procedure by which the simultaneous solution of Equations (11) and (12) is affected is essentially identical to that of Hirt and Cook. At the beginning of the iteration an explicit advancement of the velocity components is performed, which serves to evaluate the terms at time level $\underline{n}$ in Equation (11). The iteration then proceeds by simultaneous adjustments of pressure and the velocity components to make the velocity divergence satisfy Equation (12). The pressure change in a given cell over one iteration is given by

$$\delta p_{ijk} = -\beta (D_{ijk} - S_{ijk}^n) \quad (16)$$

where D_{ijk} is the current value of the velocity divergence and S_{ijk}^n denotes the spatially differenced form of the right-hand side of Equation (12). As recommended by Viacelli^[IV-8], D_{ijk} is evaluated using all available new velocity iterates to accelerate convergence. The weighing factor β is given by

$$\beta = \frac{\beta_0}{\Delta t} \left[\frac{1}{\Delta x^2} \left(\frac{1}{\rho_{i+1/2,j,k}^n} + \frac{1}{\rho_{i-1/2,j,k}^n} \right) + \frac{1}{\Delta y^2} \left(\frac{1}{\rho_{i,j+1/2,k}^n} + \frac{1}{\rho_{i,j-1/2,k}^n} \right) + \frac{1}{\Delta z^2} \left(\frac{1}{\rho_{i,j,k+1/2}^n} + \frac{1}{\rho_{i,j,k-1/2}^n} \right) \right]^{-1} \quad (17)$$

where $0 < \beta_0 < 2$ for iteration convergence. With this weighting factor the iteration scheme becomes equivalent to the successive over-relaxation method as applied to the associated Poisson-like pressure equation. The velocity components are changed in correspondence to each pressure change, as required by the momentum equation. The iteration is terminated when the largest value of δp_{ijk} in the mesh falls below a specified cutoff value, typically 10^{-5} to 10^{-6} of the maximum pressure in the problem. Boundary conditions are reset after every iteration in essentially the same manner as described by Hirt and Cook.

After the iteration has converged u^{n+1} is known and Equation (13) can be solved explicitly for ρ_{ijk}^{n+1} . The term $\nabla \cdot (\rho^n u^{n+1})$ in Equation (13) is spatially differenced by writing it in terms of mass fluxes across cell faces:

$$\begin{aligned} [\nabla \cdot (\rho^n u^{n+1})]_{ijk} &= \frac{1}{\Delta x} \left(\rho_{i+1/2,j,k}^n u_{i+1/2,j,k}^{n+1} - \rho_{i-1/2,j,k}^n u_{i-1/2,j,k}^{n+1} \right) \\ &+ \frac{1}{\Delta y} \left(\rho_{i,j+1/2,k}^n v_{i,j+1/2,k}^{n+1} - \rho_{i,j-1/2,k}^n v_{i,j-1/2,k}^{n+1} \right) \\ &+ \frac{1}{\Delta z} \left(\rho_{i,j,k+1/2}^n w_{i,j,k+1/2}^{n+1} - \rho_{i,j,k-1/2}^n w_{i,j,k-1/2}^{n+1} \right) \end{aligned} \quad (18)$$

where the face-centered densities $\hat{\rho}$ represent the density of mass actually being transported across the cell face in question. This prescription for mass transport is conservative regardless of how the $\hat{\rho}$ are defined. The $\hat{\rho}$ are defined herein by a modified donor-acceptor rule. The guiding principle of donor-acceptor differencing may be generally stated as follows: in computing transport of a mixture quantity from a donor cell to an acceptor cell, the single-component or -phase values are determined by the donor cell while the composition or proportions are determined by the acceptor cell.

In a single-component two-phase mixture, the mixture density ρ is related to the phase densities ρ_ℓ and ρ_g by

$$\rho = \alpha \rho_g + (1 - \alpha) \rho_\ell$$

which follows from Equation (5). Thus, in dealing with density it is convenient to regard α as determining the composition of the two-phase mixture. Clearly α may be considered as a cell-centered quantity α_{ijk}^n , defined by Equation (5) with ρ replaced by ρ_{ijk}^n and with ρ_ℓ and ρ_g replaced by $\rho_\ell(p_{ijk}^n)$ and $\rho_g(p_{ijk}^n)$ respectively. With this background, a prescription for $\hat{\rho}$ can be mathematically expressed as follows:

$$\hat{\rho}_{i+1/2,j,k}^n = \begin{cases} \rho_D^n & \text{if } \alpha_D^n = 0 \text{ or } \alpha_D^n = 1 \\ & \text{or } |\alpha_D^n - \alpha_A^n| < 0.001 \\ \alpha_A^n \rho_{gD}^n + (1 - \alpha_A^n) \rho_{\ell D}^n & \text{otherwise} \end{cases} \quad (19)$$

where the subscripts D and A, which denote the donor and acceptor cells, are defined by

$$\begin{aligned} \left. \begin{aligned} D &= (i, j, k) \\ A &= (i+1, j, k) \end{aligned} \right\} & \text{if } u_{i+1/2,j,k}^{n+1} > 0 \\ \left. \begin{aligned} D &= (i+1, j, k) \\ A &= (i, j, k) \end{aligned} \right\} & \text{if } u_{i+1/2,j,k}^{n+1} < 0 \end{aligned} \quad (20)$$

The densities $\hat{\rho}_{ij+1/2,k}^n$ and $\hat{\rho}_{i,j,k+1/2}^n$ are, of course, defined by similar expressions.

The above prescription is a physically reasonable way to prevent interface smearing. For example, if D is a two-phase cell and A is a pure gas cell then $a_A = 1$ and $\hat{\rho}^n = \rho_{gD}^n$. That is, pure gas is transported into a pure gas cell from a two-phase cell until all the gas in the two-phase cell is exhausted.

The condition on $|a_D - a_A|$ in Equation (19) is a cutoff constraint which causes the transport between two adjacent cells of nearly the same composition to be computed by a pure donor-cell procedure even when the donor cell D is in the two-phase region. Failure to include such a cutoff can result in the spontaneous development of large density gradients in two-phase regions where the liquid and gas phases are well mixed and would be expected to remain so, but where persistent gradients in p (and hence in ρ_l and ρ_g) exist.

One further constraint must be added to the mass transport logic outlined above. Consider again the case in which D is a two-phase cell and A is a pure gas cell, but suppose that a_D^n is much smaller than $|u|\Delta t/\Delta x$. In this case the donor-acceptor prescription as described above would transport more gas out of D than D contained; the net effect would be to leave too much liquid and would be forced unphysically to move into the subcooled region ($\rho > \rho_l$). Similarly, if A is a pure liquid cell and $1 - a_D^n \ll |u|\Delta t/\Delta x$, the donor-acceptor prescription would transport too much liquid out of D, leaving it with an unphysically small (perhaps even negative) density. In order to avoid these problems, the density of a cell is not allowed to change from a two-phase value to a subcooled or superheated value on a single timestep. When such a change would otherwise occur, the density in question is set equal to the saturation value. The resulting change in mass is compensated by an equal but opposite change in the appropriate neighboring cell, so that overall mass conservation is maintained.

The spatial differencing of Equation (14) is trivial; all quantities are cell-centered and simply require the addition of subscripts ijk .

Numerical stability and accuracy restrictions are essentially the same as those reported by Hirt and Cook^[IV-5], except that the lower limits on viscosity are alleviated by the use of partial donor-cell differencing in the momentum flux terms, as discussed above.

The difference equations described have been programmed into the existing framework of the SCORE-EVET code (SCORLS). The functional relations $\rho = f(p, I)$, $\rho_l(p)$, and $\rho_g(p)$ are evaluated numerically by a table lookup and interpolation procedure, which represents the thermodynamic properties of water-steam as given by the 1967 IFC Formulation^[IV-9,-10]. The SCORLS code can be applied to problems in one, two, or three dimensions in Cartesian coordinates. The results of two sample calculations are presented in Section IV-2.1.3.

2.1.3 Sample Calculations. A simple but quite stringent test problem for the numerical technique described is that of calculating one-dimensional oscillations in water level in a vertical pipe of length L subject to a constant pressure drop Δp , in the absence of viscosity and thermal conductivity. The velocity is uniform along the length of the pipe by virtue of Equation (9), and is hence a function of time alone. Initially the pipe is filled with

water of density ρ_l to a height H_0 , and with steam of density ρ_g above H_0 ; the initial velocity is u_0 . A force balance on the material within the pipe shows that the motion is governed by the following ordinary differential equation for the water height $H(t)$:

$$\frac{d^2 H}{dt^2} = \frac{\Delta p}{\rho_l H + \rho_g (L-H)} - g \quad (21)$$

subject to the initial conditions $H=H_0$ and $dH/dt = u_0$ at $t = 0$. This equation has been solved numerically, by a standard method, for the following values of the parameters:

L	$=$	3.6576 m	H_0	$=$	1.09728 m
u_0	$=$	0.0 m/s	Δp	$=$	$1.757 \times 10^4 \text{ N/m}^2$
ρ_l	$=$	920.048 kg/m^3	ρ_g	$=$	2.2325 kg/m^3
g	$=$	9.8 m/s^2			

(These density values are very close to the saturation values at $p = 4.137 \text{ bar}$.) The resulting time dependent of $u = dH/dt$ is shown as the solid curve in Figure IV-1, which may be regarded as the exact solution. The problem is seen to exhibit oscillatory behavior in velocity and hence in water level.

The same problem was run with SCIRKS at an ambient pressure of 4.137 bar , using 20 spatial cells ($\Delta x = 0.182188 \text{ m}$) and a timestep of $\Delta t = 0.005 \text{ sec}$. The resulting velocity curve is shown by the circular points on Figure IV-1; the agreement with the exact solution is seen to be excellent. The velocities were not quite uniform along the pipe because of the finite iteration convergence criterion (for this problem, the cutoff was $|\delta p|/p_{\max} = 5 \times 10^{-6}$), but were generally uniform to within 0.5% or better. Figure IV-2 is a logarithmic plot of the density profiles (unsmoothed) at three different times during the calculation. These plots show that the calculation preserved the sharp water-steam interface without appreciable smearing, even though the density ratio is almost 1000:1.

The second test calculation is that of liquid water sloshing in a two-dimensional rectangular tank of length L and height H . The tank is half filled with liquid water and half filled with steam. The initial conditions are: (a) all the liquid water is in the right-hand side of the tank, (b) the pressure is uniform at the value p_0 , and (c) the velocity is zero. The calculation was run with the following parameter values:

L	$=$	1.0 m	H	$=$	0.5 m
Δx	$=$	$L/20$	Δz	$=$	$H/10$
ρ_l	$=$	960.0 kg/m^3	ρ_g	$=$	0.585 kg/m^3
p_0	$=$	1.0 bar	Δt	$=$	0.005 s

Viscosity and heat conduction were neglected, and free slip boundary conditions were imposed. Thus, if there were no numerical damping, the sloshing would go on forever.

The development of the motion out to $t = 1.0$ sec is shown in Figures IV-3a through IV-3f, which are combined velocity vector plots and interface configuration plots. The interface configuration was plotted by hand from the computer output according to the following procedure. Any cell with $\rho_g < \rho < \rho_l$ is an interface cell. Within each interface cell the interface was represented by a straight line parallel to one of the two coordinate directions. Since the volume fractions of liquid and vapor are known [see Equation (5)], this line is constrained to four possible locations. Of these four possibilities the one was chosen which appeared visually to be the most realistic in its relation to neighboring cells. (For example, the liquid in an interface cell was taken to be contiguous with the liquid in neighboring cells whenever possible.) This procedure results in a "staircase" interface pattern, and was adopted here for two reasons: (a) it minimizes subjectivity in the plotting and (b) it emphasizes the fact that the DA technique does not resolve the interface in detail within a computational cell. Of course, the "staircase" interface plots could legitimately be smoothed further, if desired, by any procedure which preserves the volume fractions of liquid and gas in the interface cells.

No analytical solution is available for this problem, but the numerical results displayed in Figures IV-3a through IV-3f clearly show the qualitative sloshing behavior which one would intuitively expect.

The velocity vectors in Figures IV-3d through IV-3f appear to show an incipient instability near the interface. Fortunately, the irregularities do not grow appreciably following their initial appearance, and thus never become large enough to be troublesome. This behavior may reflect the fact that DA differencing is nearly equivalent to calculating mass fluxes by centered differencing at the interface and donor-cell differencing elsewhere, and thus would be expected to be locally unstable at the interface. To see this near equivalent, consider a one-dimensional problem in which velocity and pressure (and hence ρ_l and ρ_g as well) are constant in space and time. Let the interface be located in cell $J(n)$ at time level n . For this case, it is easily verified that the donor-acceptor differencing prescription reduces to

$$\frac{\rho_j^{n+1} + \rho_j^n}{\Delta t} + u \frac{\rho_j^n - \rho_{j-1}^n}{\Delta x} = 0 \quad [j \neq J(n)]$$

$$\frac{\rho_j^{n+1} - \rho_j^n}{\Delta t} + u \frac{\rho_{j+1}^n - \rho_{j-1}^n}{\Delta x} = 0 \quad [j = J(n)]$$

where we have assumed that $u > 0$. However, the local instability resulting from the use of centered differencing at the interface would be expected to be bounded by the coupling to neighboring cells, where local disturbances are strongly damped by the donor-cell differencing. Thus it is reasonable to expect global stability of the calculation as a whole, as is observed in practice.

The velocity vectors and interface configuration at $t = 2.0$ sec are shown in Figure IV-4. Here the noteworthy features are the presence of isolated "drops" and "bubbles" on the order of one cell width in diameter. Although the gross motion continues to be realistic,

Fig. IV-4 Sloshing problem at time = 2.00 sec.

the DA technique is inherently incapable of correctly representing the motion of single-cell drops or bubbles. The nature of this deficiency can again be seen by considering a one-dimensional problem in which velocity and pressure are constant in space and time. Let the initial condition be

$$\rho_j^0 = \begin{cases} \rho_g & j = J \\ \rho_\ell & j \neq J \end{cases} .$$

Now consider the value of ρ_J after the second timestep. According to the DA differencing procedure, this value is given by

$$\rho_J^2 = (1 - \mu - \mu^2) \rho_g + \mu (1 + \mu) \rho_\ell \quad (22)$$

where $\mu \equiv u\Delta t/\Delta x$ ($u > 0$). Physically, however, the bubble should simply translate with velocity u . The correct value for ρ_J^2 is therefore

$$\rho_J^2 = (1 - 2\mu) \rho_g + 2\mu \rho_\ell . \quad (23)$$

For small $\mu, \mu^2 \ll \mu$ and Equation (22) reduces to

$$\rho_J^2 \approx (1 - \mu) \rho_g + \mu \rho_\ell$$

which is just ρ_J^1 , the value of ρ_J after the first timestep. That is, the bubble has essentially remained stationary between cycles 1 and 2, instead of moving an additional distance $u\Delta t$ as

it should have. This difficulty may be regarded as a result of inadequate finite-difference resolution: one cannot expect to accurately represent the motion of a drop or bubble with a single cell. If the zoning were refined so that the bubble occupied many cells then the DA technique would again compute essentially correct mass fluxes and hence correct bubble motion.

2.2 Plenum Code Development Work (J. A. Trapp)

In the past few years there has been a considerable amount of activity in the area of turbulence modeling. This has been due largely to the increased capability of the digital computer. Several models of turbulences each requiring the solution of differential equations describing some aspect of the turbulent structure, have been developed.

Perhaps the first move toward a model of turbulence can be attributed to Boussinesq (1877). More than ninety years ago he suggested that the effective turbulent stress be replaced by an eddy viscosity times the mean velocity gradients. Prandtl in 1925 gave an algebraic model for this eddy viscosity. Many other algebraic formulas have been proposed since then.

The first differential model for turbulence was apparently proposed by Kolmogorov^[IV-11]. A few years later in 1945 another differential model was proposed by Prandtl^[IV-12]. Because of the inability at that time to solve the complicated differential equations that these models involved, no further work was done in the area until late in the 1960's.

Although some 40 years elapsed between Prandtl's mixing length proposal and the real beginning of differential turbulent modeling, little was done during that time to describe turbulent flows involving recirculation zones. The algebraic models have mainly been applied to quasi-parallel flows, i.e., plane channels, pipes, boundary layers, and jets.

With the development of the differential models it is possible to account for some of the dynamics of the turbulent flow field. Convection and diffusion effects can easily be incorporated in the differential models and hence recirculation type problems can be handled more realistically.

A two parameter differential model of turbulence is described in this section. A numerical scheme has been developed and coded for this model using a limiting form of the model equations to determine solid wall boundary conditions.

The basic turbulent fluid flow equations are described in Section IV-2.2.1. The development of the two-parameter model equations is presented in Section IV-2.2.2. The modeling required in a region near a solid wall for boundary conditions is developed in Section IV-2.2.3. Section IV-2.2.4 describes in detail the numerical scheme used to solve the equations and Section IV-2.2.5 presents the code results for two simple calculations: (a) prediction of Moody friction factors and (b) comparison with Laufer's channel flow data^[IV-13].

2.2.1 Basic Equations. The basic averaged equations for mass conservation, linear momentum, and internal energy can be obtained by time, space, or ensemble averaging. In each case the same equations are obtained, except that the variables are time, space, or ensemble averaged variables. These equations have been given by a number of authors [IV-14,-15,-16] and are recorded below:

$$\frac{\partial \bar{\rho}}{\partial t} + \frac{\partial \bar{\rho} \hat{v}_i}{\partial x_i} = 0 \quad (24)$$

$$\frac{\partial \bar{\rho} \hat{v}_i}{\partial t} + \frac{\partial \bar{\rho} \hat{v}_i \hat{v}_j}{\partial x_j} = \frac{\partial \bar{\rho}}{\partial x_i} + \frac{\partial (\bar{\sigma}_{ij} - \overline{\rho v_i^* v_j^*})}{\partial x_j} + \bar{\rho} g_i \quad (25)$$

$$\frac{\partial \bar{\rho} \hat{I}}{\partial t} + \frac{\partial \bar{\rho} \hat{v}_i \hat{I}}{\partial x_i} = - \frac{\partial (\bar{q}_i + \overline{\rho I^* v_i^*})}{\partial x_i} + \rho r - \bar{p} \frac{\partial \hat{v}_i}{\partial x_i} + p \frac{\partial \hat{v}_i^*}{\partial x_i} + \bar{\sigma}_{ij} \frac{\partial \hat{v}_i}{\partial x_j} + \sigma_{ij} \frac{\partial \hat{v}_i^*}{\partial x_j} \quad (26)$$

In Equations (24) to (26) ρ is the mass density, v_i is the velocity in the i th coordinate direction, p is the pressure, σ_{ij} is the viscous stress tensor, g_i is the constant body force, I is the internal energy per unit mass, q_i is the i th component of the heat flux vector, and r is the constant volume source of heat per unit mass. An over bar denotes an average (time, space, or ensemble); a circumflex denotes the mass averaged value of a variable, that is

$$\hat{\chi} = \frac{\overline{\rho \chi}}{\bar{\rho}} \quad (27)$$

An asterisk on any variable denotes the fluctuating part of the variable relative to the mass averaged value, that is

$$\chi^* = \chi - \hat{\chi} \quad (28)$$

A number of new terms referred to as correlation functions have been introduced into the mean equations by the averaging process. These new terms are

$$R_{ij} \triangleq \overline{-\rho v_i^* v_j^*} \quad (29a)$$

$$q_i^t \triangleq \overline{\rho v_i^* I^*} \quad (29b)$$

and the 4th and 6th terms on the right-hand side of Equation (26)^[a]. The terms in Equations (29a and 29b) are responsible for the turbulent transfer of momentum and energy respectively. R_{ij} is called the Reynolds stress. These terms are very important and will be considered first. The basic assumption made in the two-parameter turbulence model is that

$$R_{ij} = \mu_t \left(\frac{\partial \hat{v}_i}{\partial x_j} + \frac{\partial \hat{v}_j}{\partial x_i} - \frac{2}{3} \frac{\partial \hat{v}_k}{\partial x_k} \delta_{ij} \right) - 2 \bar{\rho} q \delta_{ij} \quad (30)$$

[a] The symbol $\triangle$ means "is defined as".

$$q_i^t = -k_t \frac{\partial \hat{I}}{\partial x_i} \quad (31)$$

where μ_t and k_t are obtained from differential equations accounting for convective and diffusive effects of turbulence.

The assumption of an eddy viscosity and turbulent conductivity has been criticized by some writers, because in some flows R_{ij} is not zero where the right-hand side of Equation (30) vanishes. Any model given for R_{ij} , even using a transport equation for it, will in some situations predict results that are not in exact agreement with the real flow. The eddy viscosity concept, when determined from differential transport equations, gives good results in a large number of flows and is equivalent to retaining the leading term in an expansion of the (unknown) functional relation between the Reynolds stress and the mean velocity distribution.

The remaining unknown correlations, the 4th and 6th terms on the right-hand side of Equation (26) of the energy equation, represent the heat energy supplied by turbulent dissipation in the smaller eddies and the fluctuating volume change work done by the turbulence fluctuations. As these terms appear in the equations used to describe the turbulence a discussion of their modeling is given in Section IV-2.2.2.

The average molecular transport of momentum and energy is given by $\bar{\sigma}_{ij}$ and $\bar{q}_i$. These are assumed to be small relative to the turbulent transport of momentum and energy. The model of turbulence developed here is for high Reynolds number flows where it can be assumed that the molecular transport effects are always small compared to the turbulent transport. Throughout the modeling molecular transport effects are neglected, that is

$$\bar{\sigma}_{ij} \approx 0, \quad \bar{q}_i \approx 0 \quad (32)$$

The state equation for the fluid is taken in the form

$$\rho = f(p, I) \quad (33)$$

This equation is also assumed to hold for the averaged variables $\bar{\rho}$, $\bar{p}$, $\bar{I}$.

2.2.2 Turbulence Model. Many different methods have been used to affect the closure of the mean Equations (24) through (26). These methods naturally divide themselves into two classes: (a) algebraic models and (b) differential models.

The algebraic models employ an algebraic equation for the eddy viscosity and assume that the turbulent time scale is much shorter than the mean flow time scale. Hence, convective and diffusive effects can be neglected in the turbulence modeling. The models involving differential equations try to account for some of the dynamics of the turbulent flow field.

The algebraic models have been successful in many applications. They have two basic drawbacks. One is the nonuniversality of the formula given for the eddy viscosity or mixing length. Each different flow field may require a different algebraic formula. The second drawback is the equilibrium assumption inherent in the algebraic approach. In a flow field involving recirculation and convection the turbulent structure at one point in the flow field will depend upon the dynamics of the turbulence in other parts of the flow field and an equilibrium model will not be applicable. This is seen in the lack of algebraic models for these types of flow.

The second basic approach to turbulence involving differential equations is the one pursued here. Many such models have appeared in the literature. A two-parameter differential model involving the turbulent kinetic energy q and its rate of dissipation ϵ has been chosen here. This model has been widely tested at Imperial College in a variety of different flow situations (see Reference IV-17 for summary and references).

Before developing this model a few remarks concerning compressibility and density fluctuations are in order. The working assumption used in almost all models of compressible turbulent flow is known as Morkovin's hypothesis^[IV-18]. Morkovin pointed out that one would not expect the turbulence structure to be affected by compressibility as long as the Mach number fluctuation is much less than unity (that is $\rho'/\bar{\rho} \ll 1$). This condition is satisfied if the Mach number, based on the mean velocity, is less than five and there are no shocks in the flow field^[IV-14,-19]. The above assumption implies that the density variation has a kinematic or volumetric rather than a dynamic effect on the velocity field. This is the reason for its inclusion in the mean equations. Since we are interested in flows where Morkovin's assumptions are valid, the turbulent model equations developed below are obtained from the local flow equations assuming ρ' equals zero and using the modeling developed for incompressible fluids.

The two-parameter model Equations (30) and (31) are still retained, but μ_t and k_t are given as functions of

$$q \triangleq 1/2 \cdot v_i^* v_i^* \quad (34)$$

and

$$\epsilon \triangleq -v \frac{\partial v_i^*}{\partial x_j} \frac{\partial v_i^*}{\partial x_j} \quad (35)$$

which are in turn obtained from dynamical equations governing the turbulence. In definition (34) ν is the kinematic viscosity, assumed constant. Since the turbulent equations are obtained assuming $\rho' = 0$ in this section there is no difference between the average $\bar{\chi}$ and mass average $\hat{\chi}$ and $\rho\chi^* = 0$. Assuming that μ_t and k_t depend only upon the turbulent parameters $\hat{q}, \hat{\epsilon}$, and $\bar{\rho}$ dimensional analysis requires that

$$\mu_t = C_\mu \bar{\rho} \frac{\hat{q}^2}{\hat{\epsilon}} \quad (36)$$

and

$$k_t = \frac{\mu_t}{Pr} = \frac{C_\mu \rho \hat{q}^2}{Pr \hat{\epsilon}} \quad (37)$$

where C_μ and Pr are constants and Pr has the interpretation of a turbulent Prandtl number.

The exact equation for $\hat{q}$ can be obtained from the local flow equations by simple manipulations. If the averaged equation (25) is subtracted from the unaveraged momentum equation and the resulting equation is multiplied by v^* and averaged one obtains^[a]

$$\begin{aligned} \frac{\partial \bar{\rho} \hat{q}}{\partial t} + \frac{\partial \bar{\rho} \hat{q} v_j}{\partial x_j} = R_{ij} \frac{\partial \hat{v}_i}{\partial x_j} - \frac{\partial}{\partial x_j} (\overline{\rho q v_j^*} + \overline{v_j^* \rho} + \overline{v_i^* \sigma_{ij}}) \\ + \overline{\rho \frac{\partial v_i^*}{\partial x_i}} - \overline{\sigma_{ij} \frac{\partial v_i^*}{\partial x_j}} \end{aligned} \quad (38)$$

The basic assumptions used to obtain a closed equation from Equation (38) are as follows:

- (1) $\overline{\rho q v_j^*}$ gives the diffusive transport of q by the fluctuating velocity and equals $C_q \mu_t \frac{\partial \hat{q}}{\partial x_j}$. The coefficient of $\frac{\partial \hat{q}}{\partial x_j}$ is required by dimensional arguments to have the form given above with C_q a dimensionless function.
- (2) $\overline{v_j^* p}$ is neglected or assumed to be similar to (1) and diffusive in character.
- (3) $\overline{v_i^* \sigma_{ij}}$ is the diffusion of the molecular stress due to turbulent fluctuations and in the range where $\mu_t \gg \mu$ is neglected.
- (4) $\overline{\rho \frac{\partial v_i^*}{\partial x_i}}$ equals zero if compressibility effects are ignored in the turbulence model as $\frac{\partial v_i^*}{\partial x_i}$ is identically zero.
- (5) Since $-\overline{\sigma_{ij} \frac{\partial v_i^*}{\partial x_j}}$ appears in the internal energy equation it can be interpreted as the turbulent energy dissipation (resulting in heating) primarily due to the viscous effects in the small eddies since the local shear gradients are largest in these eddies. If it is assumed that this is unaffected by the mean flow and hence equal to its value in a homogeneous flow one obtains $\rho \hat{\epsilon}$ as a model for the dissipation rate of $\hat{q}$.

[a] Equation (38) is exact even if Morkovin's assumption is not invoked.

Putting all these assumptions together the final transport equation for $\hat{q}$ becomes

$$\frac{\partial \bar{\rho} \hat{q}}{\partial t} + \frac{\partial \bar{\rho} \hat{q} v_j}{\partial x_j} = 1/2 \mu_t e_{ij} e_{ij} + \frac{\partial}{\partial x_j} (C_q \mu_t \frac{\partial \hat{q}}{\partial x_j} - \rho \hat{\epsilon}) \quad (39)$$

where $e_{ij} = \frac{\partial v_i}{\partial x_j} + \frac{\partial v_j}{\partial x_i}$. In obtaining the first term on the left from $R_{ij} \frac{\partial v_i}{\partial x_j}$ the incompressible assumption has been used implying that the production of turbulence is primarily due to mean shear gradients.

Examination of Equations (36) and (38) shows that they have two terms in common. These terms, the turbulent pressure volume work and the viscous dissipation rate of turbulence, have been modeled in connection with the $\hat{q}$ equation as zero and $\rho \hat{\epsilon}$ respectively. These terms will be modeled as zero and $\rho \hat{\epsilon}$ respectively in the mean internal energy Equation (26) also.

An exact equation for ϵ can be obtained from the local flow equations. It contains many more unknown correlations than did the turbulent kinetic energy equation. After much modeling it is finally written in a form similar to Equation (39), that is

$$\frac{\partial \bar{\rho} \hat{\epsilon}}{\partial t} + \frac{\partial \bar{\rho} \epsilon u_j}{\partial x_j} = C_p 1/2 \frac{\mu_t \hat{\epsilon}}{q} e_{ij} e_{ij} + \frac{\partial}{\partial x_j} (C_\epsilon \mu_t \frac{\partial \hat{\epsilon}}{\partial x_j}) - C_D \bar{\rho} \hat{\epsilon}^2 / q \quad (40)$$

Equations (39) and (40) are the differential equations used to model the dynamics of the turbulent flow field. They have terms accounting for convection of the turbulence by the mean flow, production of the turbulence by the mean flow shear strains, e_{ij} , diffusion of turbulence from regions of high intensity to regions of lower intensity, and finally dissipation of turbulence due to the viscous shears in the small scale eddies.

The basic turbulent model consisting of Equations (37), (39), and (40) contains a number of dimensionless functions. These are assumed constant for high Reynolds number flow and have been determined by comparing the predictions of the model with various simple problems, decay behind a grid, near wall constant stress layer, etc.

The high Reynolds number assumption means that laminar and transition type flows cannot be considered. For pipe flow this implies that the Reynolds number must be greater than 2,000.

A number of people have been responsible for the development of the q - ϵ model [IV-21-IV-23]. A number of other parameters have been used in different two parameter models, but almost all such models use q as one of these parameter. The q - ϵ model has been tested in a variety of flow situations at Imperial College by D. B. Spalding and his coworkers. The value of the constants recommended by them is as follows [IV-17]:

$$C_q = 1.0, C_\rho = 1.44, C_\epsilon = 0.77, C_D = 1.92, C_\mu = 0.09, \quad (41)$$

$$0.6 \leq Pr \leq 0.9.$$

2.2.3 Wall Boundary Conditions. The equations given in Sections IV-2.2.1 and 2.2.2 form a closed system for the calculation of turbulent flows with heat transfer when the viscous stress and molecular conductivity can be neglected relative to the turbulent transport of momentum and energy. This leads to a problem at a solid wall where we have a viscous sublayer and transition region in which molecular transport is larger than or equal to the turbulent transport. The preceding model cannot be applied in these regions (even if the model were extended to include molecular transport effects, the steep gradients in this region would in any finite difference scheme require a large number of cells and hence lead to costly calculations). For this reason the appropriate boundary conditions to apply in a region just outside the transition sublayer must be found. At present adiabatic boundary conditions have been assumed for the internal energy equation and hence only the boundary conditions on velocity, q and ϵ will be considered here. For convenience, the superposed bar and circumflex on all averaged quantities will be deleted from now on.

To develop the wall boundary conditions [IV-24] consider a quasi-steady parallel flow in the near wall region with negligible axial convection. The basic equations (25), (39), and (40) reduce to

$$\frac{\partial}{\partial y} (C_{\mu} \mu_t \frac{\partial u}{\partial y}) = -K \quad (42)$$

$$\mu_t (\frac{\partial u}{\partial y})^2 + \frac{\partial}{\partial y} (C_q \mu \frac{\partial q}{\partial y}) - \rho \epsilon = 0 \quad (43)$$

$$C_{\rho} \mu_t \frac{\epsilon}{q} (\frac{\partial u}{\partial y})^2 + \frac{\partial}{\partial y} (C_{\epsilon} \mu_t \frac{\partial \epsilon}{\partial y}) - C_D \frac{\rho \epsilon^2}{q} = 0 \quad (44)$$

where y is the normal distance from the wall, K is the pressure gradient in the principal flow direction assumed independent of y , and u is the axial velocity. The appropriate boundary conditions will now be obtained by finding the particular solution of these equations valid when y is small and requiring the solution in the bulk of the channel to approach this solution in the near wall region [IV-24].

$$C_{\mu} \rho \frac{q^2}{\epsilon} \frac{\partial u}{\partial y} = -Ky + \sigma_w \quad (45)$$

where σ_w is the wall shear stress. Since the transition layer in high Reynolds number flow is very thin (1/2% of pipe radius in pipe flow) Ky can be neglected relative to σ_w at the boundary of this region and

$$\frac{\partial u}{\partial y} = \frac{\sigma_w}{\rho} \frac{\epsilon}{C_{\mu} q^2} \quad (46)$$

Putting Equation (46) into Equations (43) and (44) and using Equation (45) gives

$$\frac{\rho \epsilon}{C_{\mu} q^2} \frac{\sigma_w^2}{\rho} + \frac{\partial}{\partial y} (C_q C_{\mu} \frac{q^2}{\epsilon} \frac{\partial q}{\partial y}) - \rho \epsilon = 0 \quad (47)$$

$$\frac{C_{\rho} \rho \epsilon^2}{C_{\mu} q^3} \frac{\sigma_w^2}{\rho} + \frac{\partial}{\partial y} (C_{\epsilon} C_{\mu} \frac{q^2}{\epsilon} \frac{\partial \epsilon}{\partial y}) - C_D \frac{\rho \epsilon^2}{q} = 0. \quad (48)$$

Assuming ρ is independent of y it is easily shown that $q = q_0$, $\epsilon = \epsilon_0/y$ are solutions of Equations (47) and (48) if the constants q_0 and ϵ_0 are such that

$$q = \left[\frac{1}{C_\mu} \frac{\sigma_w^2}{\rho} \right]^{1/2}, \quad (49)$$

and

$$\epsilon = \frac{1}{y} \left[\frac{C_\epsilon C_\mu q^3}{C_D - C_P \frac{\sigma_w^2}{\rho}} \right]^{1/2} \frac{1}{C_\mu q^2} \quad (50)$$

Equations (49) and (50) then give the boundary values that q and ϵ must assume in the region just outside the transition layer. With the values of q and ϵ in this region given, Equation (46) can be integrated to obtain

$$u = \frac{\sigma_w}{\rho} \kappa \frac{1}{C_\mu q^2} \ln y + C \quad (51)$$

where the value of q is given by Equation (49) and κ is the constant in the numerator of Equation (50). Equation (51) takes on a more familiar form if q is expressed in terms of the friction velocity

$$u_* \triangleq \left[\frac{\sigma_w}{\rho} \right]^{1/2} \quad (52)$$

Using the friction velocity, Equation (51) becomes

$$\frac{u}{u_*} = \frac{1}{\kappa} \ln y + C, \quad (53)$$

where

$$\kappa \triangleq \left[\frac{C_\mu^{1/2} (C_D - C_P)}{C_\epsilon} \right]^{1/2} = 0.432$$

is the value of the Karman constant implied by the values in Equation (41).

It is natural to make the argument of the log function dimensionless with the friction velocity u_* and the kinematic viscosity ν . In this case Equation (53) becomes

$$u/u_* = \frac{1}{\kappa} \ln \frac{u_* y}{\nu} + B. \quad (54)$$

The value of the constant B depends upon the wall roughness and for smooth walls can be taken as 5.7.

The boundary conditions imposed at a rigid wall are now obtained by forcing the solution to have the values given by Equations (49), (50), and (54) on the wall and neglecting the thin sublayers that exist there. The exact way in which this is done will be explained in Section IV-2.2.4.

The above analysis of the appropriate boundary conditions to apply near a solid wall has been carried out in a plane flow situation. In three dimensions there are two directions parallel to the wall. With the velocities in these directions denoted by u and w the above analysis remains valid except for the following changes: (a) σ_w in Equation (46) is replaced by the shear stress in the u direction, i.e., $\sigma_w \rightarrow \sigma_w^u$; (b) the momentum equation in the w direction is solved to obtain

$$\frac{\partial w}{\partial y} = \frac{\sigma_w^u}{\rho} - \frac{\epsilon}{C_\mu q^2} \quad (55)$$

where σ_w^2 is the wall shear stress in the w direction; and (c) Equations (43) and (44) have additional production terms due to $\frac{\partial w}{\partial y}$, which are obtained from Equation (55). The resulting solution of the $q-\epsilon$ equations is as given in Equation (49) except that σ_w is replaced by $(\sigma_w^u)^2 + (\sigma_w^w)^2$ and the solution of Equation (55) gives an equation similar to Equation (51), i.e.,

$$w = \frac{\sigma_w^w}{\rho} k \frac{1}{C_\mu q^2} \ln y + C^w. \quad (56)$$

2.2.4 Numerical Scheme. The numerical method used to solve Equations (24), (25), (26), (27), and (28) is an extension of the basic scheme in the SCORE code. SCORE solves Equations (42), (43), and (44) in an implicit manner.

The region in which computations are performed is divided into a set of small rectangular cells of size Δx_i , Δy_j , and Δz_k . The velocity components are located on the cell faces whereas density, internal energy, pressure, turbulent kinetic energy, and the dissipation are located at cell centers. A time-dependent solution is obtained by expressing the equations in finite difference form and advancing the variables through a sequence of small time steps, Δt .

The advancement for each time step can be described in two parts.

Let p^n , I^n , ρ^n , U^n , V^n , W^n , q^n , and ϵ^n be the values of the variables at the beginning of a time step^[a]. The first part of the calculation solves finite difference forms of Equations (24), (25), and (26) with the turbulence quantities evaluated at the beginning of the time step. This results in new time values for p , I , ρ , U , V , W , i.e., p^{n+1} , I^{n+1} , ρ^{n+1} , U^{n+1} , V^{n+1} , and W^{n+1} .

[a] U , V , W , are used in this section instead of v_1 , v_2 , and v_3 .

In the second stage q_{ijk}^{n+1} is calculated using

$$\frac{q_{ijk}^{n+1} - q_{ijk}^n}{\Delta t} = - (\text{CONV})_{ijk}^n + (\text{DIF})_{ijk}^n + (\text{SOURCE})_{ijk}^n - (\text{DECAY})_{ijk}^n \quad (57)$$

where

$$\begin{aligned} (\text{CONV})_{ijk}^n = \frac{1}{2\Delta x_i} & \left[-u_{i-1/2jk}^{n+1} (q_{i-ijk}^n + q_{ijk}^n) - \alpha_x \left| u_{i-1/2jk}^{n+1} \right| \right. \\ & (q_{i-ijk}^n - q_{ijk}^n) + u_{i+1/2jk}^{n+1} (q_{ijk}^n + q_{i+ijk}^n) + \alpha_x \left| u_{i+1/2jk}^{n+1} \right| \\ & \left. (q_{ijk}^n - q_{i+ijk}^n) \right] - q_{ijk}^n \left(\frac{u_{i+1/2jk} - u_{i-1/2jk}}{\Delta x_i} \right) \end{aligned} \quad (58)$$

+ SIMILAR TERMS FOR Y AND Z CONVECTION,

$$(\text{DIF})_{ijk}^n = \frac{1}{\Delta x_i} \left[v_{i+1/2jk}^n \left(\frac{q_{i+ijk}^n - q_{ijk}^n}{\Delta x_{i+1/2jk}} \right) - v_{i-1/2jk}^n \left(\frac{q_{ijk}^n - q_{i-ijk}^n}{\Delta x_{i-1/2jk}} \right) \right] \quad (59)$$

+ SIMILAR TERMS FOR Y AND Z DIFFUSION,

$$(\text{SOURCE})_{ijk}^n = 1/2 v_{ijk}^n \left[\left(\frac{u_{i+1/2jk} - u_{i-1/2jk}}{\Delta x_i} \right) \left(\frac{u_{ij+1/2k}^{n+1} - u_{ij-1/2k}^{n+1}}{\Delta x_i} \right) \right] \quad (60)$$

and

$$(\text{DECAY})_{ijk}^n = \epsilon_{ijk}^n \quad (61)$$

A similar finite difference form of Equation (40) is used to calculate ϵ_{ijk}^{n+1} . A number of comments concerning Equation (57) are in order.

- (1) $\nu_{ijk}^n \triangleq \frac{C_\mu (q_{ijk}^n)^2}{\epsilon_{ijk}^n}$ and is calculated as a simple average if needed at a point other than a cell center.
- (2) The convective term is a centered difference for $\alpha = 0$ and a donor cell difference for $\alpha = 1$.
- (3) As noted in Equation (60) the source term uses the new time velocities, as does the convection term.
- (4) The density has been assumed slowly varying and taken outside the gradient terms.

Equation (57) can be solved directly, without iteration, for q_{ijk}^{n+1} and hence the turbulence equations are evaluated only once during each time step. After q_{ijk}^{n+1} and ϵ_{ijk}^{n+1} are known the turbulent viscosity and conductivity are calculated using Equations (36) and (37) and used for the next time step in Equations (25) and (26).

The basic accuracy restriction requires that the fluid move less than one computational cell during a single time step that is

$$\Delta t < \min \left[\frac{\Delta x}{|u|}, \frac{\Delta y}{|v|}, \frac{\Delta z}{|w|} \right]. \quad (62)$$

The donor cell parameters α_x , α_y , and α_z and the time step must be such that

$$\alpha_x > \frac{|u| \Delta t}{\Delta x}, \quad \alpha_y > \frac{|v| \Delta t}{\Delta y}, \quad \alpha_z > \frac{|w| \Delta t}{\Delta z} \quad (63)$$

or else convective instability will result. Because the diffusion terms in the turbulence and momentum equations are treated explicitly, the following must be satisfied:

$$CC_\mu \frac{q^2}{\epsilon} \Delta t < \frac{1}{\left(\frac{1}{\Delta x^2} + \frac{1}{\Delta y^2} + \frac{1}{\Delta z^2} \right)} \quad (64)$$

($C = C_q$ or C_ϵ or one) or else diffusional instability will result. Other stability limits probably exist. No rigorous stability analysis has been conducted thus far but these have proven sufficient in most calculations to date.

The number of boundary conditions required on q and ϵ are similar to those used on other scalars. That is q and ϵ must be prescribed at an inflow boundary as required by the differential equations. Although not required by the differential equations, boundary conditions at an outlet boundary are required by the difference equations and they are usually obtained by extrapolation.

The boundary conditions at a solid wall require special consideration. A large number of cells in the near-wall region would be required to resolve the steep gradients that occur there. For this reason wall boundary conditions are applied just outside the transition region and the limiting form of the equations for this region derived in Section IV-3 are used.

For convenience, the numerical application of the results in Section IV-3 are described below for the plane flow case. At the end of a time step it is assumed that U^n , q^n , and ϵ^n are known in the first cell inside the wall and in the fictitious cell just outside the wall (see Figure IV-5).

With these known values Equation (54) is used to calculate the wall shear stress

$$U_*^n = \sqrt{\frac{\sigma_w^n}{\rho^n}} \quad (65)$$

With U_*^n known, the new time values, q_1^{n+1} and ϵ_1^{n+1} , are calculated from Equation (49). The new time velocity boundary conditions are such that Equation (46) is satisfied, that is

$$\frac{u_1^{n+1} - u_o^{n+1}}{L} = (u_*^n)^2 \frac{\epsilon^n}{C_\mu (q^n)^2} \quad (66)$$

and the components of the velocity normal to the wall are set to zero.

2.2.5 Test Problems. The code described in Section IV-2.2.4 will be referred to as SPLEN1. SPLEN1 was checked out with two sample problems. The first was to check the ability of SPLEN1 to calculate friction factors as represented by the Moody chart. The second problem was a comparison of the code results for mean velocity and turbulent energy profiles with the channel flow data of Laufer^[IV-13].

ANC-A-6199

Fig. IV-5 Cell configuration at solid wall.

Problem 1: SPLEN1 was run with all the constants in the model set to the values described in Section IV-2.2.2. Two plane channel problems were run at two different Reynolds numbers for smooth parallel plates. In both cases the fluid properties were for subcooled water. Table IV-I shows the code calculations of the friction factor f , defined as

$$f \triangleq \frac{\partial p}{\partial x} \left(\frac{H.D.}{1/2 \rho u^2} \right) \quad (67)$$

where H. D. is the hydraulic diameter. The concept of an equivalent H. D. is well founded for plane channel flows^[IV-25,-26].

There are two ways in which the pressure gradient can be obtained from SPLEN1: (a) directly from the pressure profile and (b) from the calculated wall shear stress in Equation (65). These two methods differed by about 1% in both runs and their average was used to obtain Table IV-I.

TABLE IV-I

COMPARISON OF CALCULATED AND EXPERIMENTAL FRICTION FACTORS

<u>Reynolds Number</u>	<u>SPLEN1</u>	<u>Moody Chart</u>
1.11×10^5	0.0178	0.0173
4.79×10^5	0.0135	0.0132

Table IV-I shows that SPEN1 calculates reasonable results for friction factors and hence gives the correct relationship between the average velocity and the wall shear stress or pressure drop.

Problem 2: A plane channel flow problem was run with the channel dimensions equal to the dimensions used in experiment conducted by Laufer^[IV-13]. The Reynolds number based upon the half channel width and maximum velocity, as used by Laufer, was 30,900. The results from SPEN1 are plotted in Figures IV-6 through IV-8 along with Laufer's data for the mean velocity and turbulent energy profiles.

As can be seen in Figure IV-6 the calculated velocity profile agrees well with Laufer's data. The calculated turbulent energy does not agree as well with the experimental results. SPEN1 gave the correct profile shape but the actual numerical agreement is not that close. From Equations (36), (45), and (49) it can be shown that in the near wall region the shear stress is given by

$$\tau = \kappa u y \frac{\partial u}{\partial y} \quad (68)$$

Laufer's data implies that Karman's constant has the value 0.33^[IV-26], whereas the most commonly accepted value of Karman's constant is 0.42. The model prediction based upon the constants recommended by Imperial College gives $\kappa=0.432$ [see Equation (53)]. As can be seen in Figure IV-8, SPEN1 calculates a shear stress larger than Laufer's data reflecting the value of κ used in the code model. The calculated value of the wall shear stress is more

Fig. IV-6 Mean velocity.

Fig. IV-7 Turbulent energy.

ANC - A - 6201

in line with the accepted value of $\kappa = 0.42$. This can be seen by using Laufer's mean velocity data to find the friction factor, based upon the H. D. of a plane channel and the Moody chart. This friction factor gives a corresponding pressure drop and wall shear stress. Since the shear stress in a fully developed channel flow has a linear variation across the channel and variables at the center, the dashed line in Figure IV-8 is obtained as the shear stress predicted by the Moody chart from Laufer's mean velocity profile. SPLEN1 follows this shear stress closely and hence would give a good shear stress profile when compared with data for which the Moody chart is applicable. From the preceding remarks the conclusion is drawn that Laufer's data for the turbulent shear stress is below that normally found. This conclusion has also been noted in Reference IV-27 where Laufer's mean flow data in the core was stated to be considerably above that of Nikuradse's^[IV-29] mean flow data when normalized by the wall shear stress. Since Laufer's data for μ_t is below that normally found and $\mu_t \propto \frac{q^2}{\epsilon}$, it follows that Laufer's data will give a q below that normally found. Figure IV-7 shows the q from Laufer's data to be below that calculated by SPLEN1 and hence it is consistent with the code calculations based upon $\kappa = 0.432$.

Although it appears that better agreement with the data of Laufer could be obtained by a readjustment of the model constants to give $\kappa = 0.33$ this was not done due to: (a) the large amount of data that supports a value of 0.42 for Karman's constant and (b) the large amount of data consistent with predictions based upon the Moody chart.

2.3 Loop Code Development Work (R. W. Lyczkowski)

The following defines how the UVUT code can be changed to incorporate the computations of additional two-phase flow theories. The specific theories discussed are: unequal velocity, unequal phase temperature (UVUT), unequal velocity, equal phase

ANC-A-6202

Fig. IV-8 Shear stress.

temperature (UVET), the Drift Flux versions of the previous two theories (UVUT-DF and UVET-DF), a simple two-pressure version of UVUT, UVET, and/or the Drift Flux version of these theories and equal velocity, equal phase temperature (EVET) calculations of a particular theory are preselected by the use of indicators or flags (by user) which are assumed fixed during the transient.

Adjacent volumes, in which differing theories are used, may introduce discontinuities into the code. For example, an EVET junction next to a UVET junction may introduce discontinuities in the friction force terms. This may occur especially if the UVET junction has computed slug or annular flow which has a large relative velocity between phases. Smooth spatial transitions of phase velocities and temperatures will require operating experience running the code.

2.3.1 UVUT Theory. The UVUT series of codes implements a full unequal velocity unequal temperature (UVUT) single pressure theory. A full UVUT theory has six field

equations in one dimension. All the codes have logic which properly couples the six field equations, when two phases coexist, to the three well known field equations when only one phase is present, either liquid or vapor. The logic is complex and treats phase appearance and complete disappearance. In this respect, the code already has two sets of hydrodynamic field equations, a single-phase EVET, and two-phase UVUT incorporated. Because of this coupling, it is not necessary to "seed" subcooled liquid for blowdown analyses. Because of the extra effort taken to affect smooth coupling of the single phase EVET field equations to the two-phase UVUT equations it will be fairly straightforward to replace the field equations at a volume or junction node through the use of user-supplied indicators or flags.

Because the code is modular, it will be a fairly straightforward task to add, as options, other field equations or correlations for velocity slip or temperature differential between phases. Those modifications may be effected as long as the basic solution scheme remains unaltered. The essence of the present scheme is solution of a pressure field equation which is formed from combinations of the phase continuity and momentum equations. The remainder of the field variables are solved from constituent field equations. Alternative formulations which may be more convenient for solving other two-phase flow theories and the remainder of the field variables will be described later.

2.3.2 Field Variables (Present). The present field variables in the UVUT codes are:

Two-Phase

- (1) Pressure, P
- (2) Liquid or vapor partial density, ρ^l or ρ^g
- (3,4) Liquid and vapor velocities, v^l and v^g
- (5,6) Liquid and vapor internal energies, U_l and U_g

Use of two equations of state gives enough information to solve for the volume fraction of vapor, α^g .

Single-Phase

- (1) Pressure, P
- (2) Liquid or vapor velocity, v^l or v^g
- (3) Liquid or vapor internal energy, U_l or U_g
- (4) Liquid or vapor partial density, ρ^l or ρ^g .

Field variable (4), under Single-Phase, assists in handling phase appearance and disappearance problems and the associated coupling to the UVUT field equations.

Figure IV-9 presents an outline of the solution scheme for the UVUT codes. The present code-set switches between single-phase EVET and two-phase UVUT equation sets as well as the areas of heat transfer, friction, and material property calculations are indicated in the figure. It will be necessary to modify some of these areas for other two-phase flow theories. These changes will be indicated when and where necessary.

Fig. IV-9 Simplified UVUT code flow diagram.

2.3.3 Field Variables (Alternative). Two alternative sets of field variables are presented herein. Solving for mixture combinations of phase velocities or energies may be more convenient for other two-phase flow theories, like Drift Flux (DF).

(1) Alternative UVUT Set I

- (a) Pressure, P
- (b) Mixture internal energy, U_m
- (c) Liquid or vapor internal energy, U_l or U_g
- (d) Mixture velocity, v_m
- (e) Liquid or vapor velocity, v^l or v^g
- (f) Liquid or vapor partial density, ρ^l or ρ^g .

(2) Alternative UVUT Set II

- (a) Pressure, P
- (b) Mixture internal energy, U_m
- (c) Mixture density, ρ_m
- (d) Mixture velocity, v_m
- (e) Liquid or vapor velocity, v^l or v^g
- (f) Liquid or vapor internal energy, U_l or U_g .

Alternative Set I or II could easily be incorporated into the present solution scheme shown in Figure IV-9 as follows.

Subroutine NERGY presently computes U_l and/or U_g . If one phase is absent, its internal energy is presently set to its saturation value at the pressure in the volume. There are two ways the user-supplied flags could be applied to calculate U_m and one of the other phase energies in subroutine NERGY. The first way would be to compute U_l and U_g as at present and form U_m ; when needed, or replace one of the phase energies field equations by a mixture energy field equation.

Subroutine MOMNTA presently computes v^l and v^g . When one phase is absent, the velocity of the missing phase is set equal to the value of the velocity of the phase which is present at a junction rather than set equal to zero. User-supplied flags in subroutine MOMNTA can compute v_m analogous to U_m described in the previous paragraph.

2.3.4 UVET Theory. A simple five-field equation unequal velocity equal temperature (UVET) single-pressure theory code was written in the latter half of 1972. The scheme is described herein and indications are made as to how one could incorporate this solution scheme into UVUT. The four-field equation UVET theory is a subset of the five equation UVET theory and is arrived at by assuming that the terms associated with momentum exchange between phases during phase change, $\dot{m} \hat{v}$, can be neglected in the phase momentum equations. UVET theory completely eliminates the need for energy partition formation.

2.3.5 Field Variables (Present). UVUT can be degenerated into UVET in a somewhat different manner than in the UVET code. The fraction of each phase at the saturation state is set equal to unity and the phase internal energies are set equal to their saturation values. This procedure is adequate for adiabatic systems only. The partial density of one of the phases is computed from a mass transfer rate due to wall heat transfer and pressure change. The pressure and velocity field equations remain unchanged from UVUT as are the transitions to and from single phase. The field variables which presently compute a UVET theory in the UVUT code are therefore:

- (1) Pressure, P
- (2,3) Liquid and vapor velocities, v^l and v^g
- (4) Combination of the mixture energy equation and phase continuity equations for pressure change (equilibrium) mass transfer rate, $\dot{m}_{\Delta P}$
- (5) Liquid or vapor partial density, ρ^l or ρ^g .

The single-phase field variables are the same as in UVUT.

2.3.6 Field Variables (Alternative). As previously mentioned, the UVET code solves a five-equation UVET theory somewhat differently from the way UVUT is presently degenerated to UVET. The flow diagram is shown in Figure IV-10. The mixture energy equation is solved for U_m and the total mass transfer rate is computed from one of the two-phase continuity equations. In a four-equation UVET theory, the mass transfer rate is not needed and this latter computation is eliminated. Therefore, the alternative UVET field variables are:

- (1) Pressure, P
- (2) Mixture internal energy, U_m
- (3,4) Liquid and vapor velocities, v^l and v^g
- (5) Mass transfer rate, $\dot{m}$, from either of the phase continuity equations (5 equation UVET only).

ANC - A - 6197

Fig. IV-10 Simplified UVET solution scheme.

A flag in the subroutine NERGY would affect the computation of U_m by continuing to use the single-phase energy equation in the two-phase region. A flag in subroutine SHMP would affect computation of the UVET mass transfer rate, $\dot{m}$, from one of the two continuity equations. Similar flags would bypass most of the subroutines called for in QMRVB.

A minor perturbation of the above scheme would be to solve for the average velocity, v_m , and either the liquid or vapor phase velocities, v^l or v^g .

2.3.7 Drift Flux (DF) Theory. The essence of the drift flux (DF) two-phase theory is that the phase velocity fields have been computed from a combination of a mixture momentum equation and some steady state correlation for slip ratio or velocity differential^[IV-29]. The concept of the two-phase drift velocity is based on an analogy with the diffusion theory of gases^[IV-30]. This approach is to be distinguished from the mechanical approach which the constituent momentum equations describe. The functional forms of the correlations for the drift flux theory can be derived from these mechanical equations, as will be shown in this section.

Two methods may be employed to include a drift flux theory. The first involves solving the mixture momentum equation and a drift flux correlation. There are various formulations of the mixture momentum equations depending on whether the flux term is expressed in terms of the drift flux, the relative velocity or the difference between the phase velocity and the average velocity, v_m ^[IV-29]. The second method involves solving for one of two-phase velocities from a constituent mechanical momentum equation and the other velocity from a drift flux correlation. This latter approach would be easier to incorporate into the present UVUT codes. Both methods require modification of the pressure field equation. If the derivatives of the terms involving the drift velocity are neglected, then the pressure equation would remain unchanged in the former method.

DF can be applied to an unequal or equal phase temperature theory. Hence, we have UVUT-DF and UVET-DF theories to consider.

2.3.7.1 Derivation of the Drift Velocity from Separated Flow Theory. This section derives the drift velocity for the churn turbulent bubbly flow regime from the two constituent momentum equations of the mechanical theory and compares the resulting equation to experiment.

The phase momentum equations from unequal velocity mechanical theory can be written

$$\begin{aligned} \rho^a \frac{\partial v^a}{\partial t} + \rho^a v^a \frac{\partial v^a}{\partial x} + \alpha^a \frac{\partial \rho}{\partial x} + \bar{A}_{ab} D_{ab} (v^a - v^a) \\ + \bar{A}_w^a B_w^a v^a + \rho^a g \cos \theta = \dot{m}(\hat{v} - v^a) \quad a=l, g \end{aligned} \quad (69)$$

where

ρ^a = the partial density

- a^a = the volume fraction
 A_{ab} = the interphase area per unit volume
 D_{ab} = the interphase drag function
 A_w^a = the area per unit volume of each phase in contact with the wall
 B_w^a = the wall drag function
 g = the acceleration due to gravity
 θ = the angle of inclination of the pipe measured from the vertical
 $\dot{m}$ = the mass transfer rate
 v = the intrinsic velocity associated with mass transfer
 x = the spatial dimension
 t = the time.

The direction of positive flow is assumed *upward*.

The velocity difference equation is arrived at by dividing Equation (69) by a^a , subtracting the two momentum equations from each other, and noting that $A_{lg}D_{lg} = A_{gl}D_{gl}$.

The result is

$$\begin{aligned}
 v^g - v^l = & \frac{\alpha^l \alpha^g (\rho_l - \rho_g) g \cos \theta}{\bar{A}_{lg} D_{lg}} + \frac{\bar{A}_w^l D_w^l v^l \alpha^l - \bar{A}_w^g B_w^g v^g \alpha^g}{\bar{A}_{lg} D_{lg}} \\
 & + \frac{\alpha^l \alpha^g (\rho_l \frac{dv^l}{dt} - \rho_g \frac{dv^g}{dt})}{\bar{A}_{lg} D_{lg}} - \frac{\dot{m}(v^g + v^l) \alpha^l \alpha^g}{\bar{A}_{lg} D_{lg}}
 \end{aligned}$$

where

$$\frac{d}{dt}^a = \frac{\partial}{\partial t} + v^a \frac{\partial}{\partial x} \quad (70)$$

An expression similar to Equation (70) was derived [IV-31] which is called the "slip equation".

In order to arrive at the drift velocity, all terms on the right side of Equation (70) are dropped except the first one. These terms can be dropped only if the following conditions are met for purposes of obtaining the relative velocity differential:

- (1) Wall friction is negligible
- (2) Inertial effects are negligible
- (3) Steady state is approximated
- (4) Momentum exchange between phases caused by phase change is negligible.

These terms may not all be negligible. For example, Yang^[IV-32] retained the effect of wall friction in deriving a correlation for slip which accounts for the fact that slip is observed to increase with increasing liquid velocity. This experimental fact cannot be deduced if the wall friction is dropped. The resultant expression fits the data much better with less scatter.

The expression which balances the interphase friction force with the force due to gravity is

$$v^g - v^l = \frac{\alpha^l \alpha^g (\rho_l - \rho_g) g \cos \theta}{\bar{A}_{lg} D_{lg}} \quad (71)$$

The general form of D_{lg} is

$$D_{lg} = \frac{f_i \rho_c |v^g - v^l|}{8} \quad (72)$$

where

f_i = the interphase friction factor

ρ_c = the continuous phase density.

The form of A_{lg} for bubbly flow is

$$\bar{A}_{lg} = 3\alpha^g / r_p \quad (73)$$

where r_p is the average bubble radius. Combination of Equations (71), (72), and (73) results in

$$v^g - v^l = \left[\frac{8\alpha^l (\rho_l - \rho_g) g \cos \theta r_p}{3f_i \rho_c} \right]^{1/2} \quad (74)$$

The critical Weber number for drop or bubble breakup is defined here as

$$We_{crit} = 2r_{max} \rho_c (v^g - v^l)^2 / \sigma \quad (75)$$

where

$r_{\max}$ = the maximum drop or bubble radius

σ = the surface tension.

The average particle radius may be related to the maximum particle radius by making the postulation that

$$r_p = C \cdot r_{\max} \quad (76)$$

where C is a distribution-dependent coefficient which relates the average to the maximum bubble diameter. Combination of Equations (74), (75), and (76) yields the desired relation for the velocity difference

$$v_g - v_l = \left(\frac{\alpha_l^4 C We_{\text{crit}}}{3f_i} \right)^{1/4} \left[\frac{(\rho_l - \rho_g) g \cos \theta \sigma}{\rho_c^2} \right]^{1/4} \quad (77)$$

Experimental and theoretical investigations have shown that

$$2.1 \leq We_{\text{crit}} \leq 2.48 \quad (78)$$

for air bubbles rising in water [IV-33,34]. The friction factor can be estimated from data obtained for fully developed turbulent flow over a nondeforming sphere which indicates that

$$f_i = C_D \approx 0.42 \quad (79)$$

The definition of drift flux, V_{gj} , is [IV-35]

$$V_{gj} = \alpha_l \cdot (v_g - v_l) \quad (80)$$

Combination of Equations (77), (78), (79), and (80) results in

$$V_{gj} = C_1 C^{1/4} \left[\frac{(\rho_l - \rho_g) g \cos \theta \sigma}{\rho_c^2} \right]^{1/4} \alpha_l^{5/4} \quad (81)$$

where $1.61 < C_1 < 1.67$.

Over the range $0.8 \leq \alpha_l \leq 1$, which is typical for the churn turbulent regime [IV-36] $(\alpha_l)^{1/4}$ varies from 1 to 0.76. If it is assumed that all the bubbles are near their maximum size, $C \cong 1$. This assumption is reasonable for the churn turbulent flow regime. Hence

$$1.22 \left[\frac{(\rho_l - \rho_g) g \cos \theta \sigma}{\rho_c^2} \right]^{1/4} \leq V_{gj} \leq 1.67 \left[\frac{(\rho_l - \rho_g) g \cos \theta \sigma}{\rho_c^2} \right]^{1/4} \quad (82)$$

Experimental data for churn turbulent bubbly flow have produced the correlation

$$v_{gj} = K \left[\frac{(\rho_l - \rho_g) g \cos \theta \sigma}{\rho_c^2} \right]^{1/4} \quad (83)$$

where K has been found to be 1.41[IV-35] and 1.53[IV-30]. These lie in the theoretically derived range of 1.22 to 1.67. Not only is the functional group in the one-fourth power the same as that for the correlation, but the constant multiplier is remarkably close. The major uncertainty lies in the value of the distribution function, C. Since this factor is raised to the one-fourth power, its value does not strongly influence the final result given by Equation (81). A similar analysis can be performed for the slug flow regime.

The foregoing derivation shows that the drift velocity can be derived from the separated flow mechanical theory and that at this point there is no apparent advantage of using some steady state correlation for relative velocity over using the more basic steady state correlations for interphase friction forces and bubble breakup. There are, however, some obvious disadvantages. First, acceptable drift flux velocities for other than gravity dominated flow regimes and horizontal flow do not exist[IV-37]. Secondly, discontinuities in the drift velocity may produce numerical instabilities. Discontinuities in the derivative of the drift velocity may also cause numerical difficulties in the mixture momentum and pressure field equations.

At steady state, UVUT will generate relative velocities which are consistent with the drift velocity expressions which can be generated analytically as done in this section. When the drift velocity is zero, the mixture momentum equation is identical to the single phase EVET momentum equation already coded in UVUT.

2.3.8 UVUT-DF Theory. The most straightforward way to specify UVUT-DF in the UVUT codes would be to replace the call to one of the two momentum equations in subroutine MOMNTA, say the liquid momentum equation, and replace it with a relation for the drift velocity similar to Equation (83). The relative velocity can be retrieved by using Equation (80). This approach constitutes a five-field equation UVUT-DF theory. The field variables are:

(1) UVUT-DF Field Variables

- (a) Pressure, p
- (b) Liquid or vapor partial density, ρ^l or ρ^g
- (c) Vapor velocity, v^g
- (d,e) Liquid and vapor internal energies, U_e and U_g .

Equations (80) and (83) would yield the liquid velocity, v^l .

Alternative UVUT Sets I and II, Section IV-2.3.3(1) and (2) could also be used with item (e) in each of those two sections replaced by a correlation. The pressure equations in subroutine PRESSR will need to be modified to be consistent with the relative velocity correlation.

Other embodiments of an unequal temperature theory have been proposed wherein the mixture energy, U_m is computed, the vapor energy set to its saturation value and the liquid energy computed from the definition of mixture energy [IV-29]. In this manner, the energy partition functions are inherently assumed to be equal to one for the vapor and zero for the liquid. The liquid could be subcooled, saturated, or superheated. The last condition would be a metastable state. This theory is a four-field equation UVUT-DF theory.

2.3.9 UVUT-DF Theory. The easiest way to approximate UVET-DF in the present code for adiabatic flow would be to set the phase energies to saturation and the fraction of each phase at saturation equal to unity, compute a constituent momentum equation for the vapor velocity, and use a correlation for the velocity difference, $v^g - v^l$, to obtain the liquid velocity. A four-field equation EVET-DF theory results. Alternatively the schemes may be employed from Section IV-2.3.3. Either a single constituent momentum equation or a mixture momentum equation could be used. If the mixture momentum equation is used, it would not be necessary to solve for the mass transfer rate, $\dot{m}$, since this quantity is presently neglected in the drift velocity computation. A three-field equation theory results. However, these alternate three-field equation formulations are not necessarily equivalent.

2.3.10 Two-Pressure Theory. A rudimentary two-pressure theory might possibly be incorporated into UVUT by utilizing expressions which relate the two-phase pressures. It is felt that expressions which involve spatial gradients are the only type which might be incorporated into the structure of the present numerics. Expressions which involve time derivatives, such as found in Noordzij and Van Wijngaarden [IV-38], are therefore ruled out. The two-pressure model being investigated by ANC would require *two* pressure equations. Incorporation of this model would require further research. It does not appear at this time that a two-pressure model which is based on additional field equations can be easily inserted into a single-pressure code.

The two-pressure model is presently being evaluated. It is clear that most two-pressure theories can be expressed as equivalent single-pressure theories. The error incurred in using this single pressure to evaluate thermodynamic quantities must be estimated.

2.3.11 EVET Theory. As mentioned in Section IV-2.3.1, the UVUT codes already contain single-phase EVET theory coding in them. User-supplied flags will allow bypassing of the two-phase UVUT field equations in favor of the EVET field equations in the two-phase region. Calling arguments will have to be changed in the heat transfer and friction correlation subroutines.

EVET two-phase is accomplished by incorporating the following changes: When the liquid energy falls into the two-phase region and the code logic senses vapor is about to

appear, instead of switching energy field equations at this point, the flags in subroutine NERGY would cause the EVET energy equation to continue to be used. In subroutine STATE, the user-supplied flag would call subroutines MIXPRO and RUDMIX rather than NONSAT and RUDNON to compute the two-phase thermodynamic properties. The void fraction will be computed by these routines also. A mixture energy will need to be defined in the main program. Flags in subroutine MOMNTA will override the switch in momentum field equations completely analogous to override of the switch in the energy field equation in subroutine NERGY. A mixture velocity will need to be defined in the main program. v^L and v^G will be set equal to the mixture velocity. The pressure equation remains unchanged. The calls to compute the liquid or vapor continuity equation will be bypassed by use of the same user-supplied flags.

The field variables for EVET are then

- (1) Pressure, P
- (2) Mixture velocity, v_m
- (3) Mixture energy, U_m .

One of the continuity equations can be used to compute the mass transfer rate, $\dot{m}$, in subroutine SHMP if it is needed in the pressure equation formulation.

The friction factor calculation in subroutine SBW need not be changed if the argument list is expanded or modified to use the mixture density and velocity for the Reynolds number. Some average velocity will need to be computed. The wall area per unit volume for single-phase flow is to be computed for two-phase flow by use of the flags as are skips to calls to SDGL and SAGL which compute the interphase friction forces.

The heat transfer correlations would be modified such that mixture properties are of the Dittus-Boelter type relation used for single-phase convective heat transfer. Bypasses are made to all the energy partition functions and all the interphase heat transfer correlations.

An alternative technique to obtain EVET would be to specify a zero drift velocity in UVET-DF. In this manner, the mixture momentum and energy equations would become identical to those already in single-phase portion of UVUT. In addition, the heat transfer and friction correlations would not have to be changed if individual calls are made to them with $v^L = v^G$.

2.3.12 Flow Regime Map Selection. Several sets of flow regime maps are presently in UVUT. These include the Baker and Govier maps for horizontal flow and the Baker-Bennet map for vertical flow. These maps have been extended in order to account for the fact that the wall temperature may be above CHF. They have also been extended in order to handle countercurrent flow. The Govier maps appear to be superior to the Baker plot.

If desired, additional flow regime maps may be added for UVUT-DF and UVET-DF to select the proper drift velocity correlation. Such a flow regime map has been suggested by Wulff^[IV-37]. However, the present maps could be used to select the proper drift velocity correlations.

3. REACTOR FUEL MODEL DEVELOPMENT

J. A. Dearien, P. E. MacDonald

Development of the FRAP-T1 and FRAP-S computer codes continued through the last quarter of FY 1975. Major activities concentrated on the development of analytical models for the two codes and modification of the codes to accept the new models. A significant amount of time was spent in the modification and adaptation of each code to the special requirements of the experimental planning function at ANC. Sections 3.1 through 3.4 describe in detail the individual code development activities and two of the major models or subcodes which were developed during this report period.

A major component of the FRAP computer code is the subcode MATPRO. This subcode is comprised of modular function subroutines which define the material properties required by the computational subcodes of FRAP. Each function subprogram or subroutine defines one material property. Sections 3.5 and 3.6 describe recent advances in MATPRO.

3.1 FRAP-T1 Development (L. J. Siefken)

During this report period, the FRAP-T1 code has been continually improved by the addition of models reported in this and the preceding quarterly. Additional developments related to FRAP-T1 are:

- (1) FRAP-T1 was transmitted to the verification branch of ANC for testing and evaluation.
- (2) Modifications were made to the code to:
 - (a) Include pellet dish volume in the internal pressure calculation
 - (b) Calculate cladding strain concentration
 - (c) Include the Tong-Young transition and film boiling correlation as a heat transfer option
 - (d) Consider mixed oxide material properties
- (3) The iteration procedure on the temperature calculation was changed to skip the analysis of thermal nodes after convergence.

This modification results in a reduction of computation time of four for certain types of accident analyses.

3.2 FRAP-S Development (G. A. Berna)

The FRAP-S code was updated to include the FRACAS routine reported in the last quarterly. Additional developments related to FRAP-S are:

- (1) The preliminary version of FRAP-S Mod 002 was released to the verification branch of ANC for initial testing and debugging.
- (2) A fuel densification model developed by Rolstad^[IV-39] was included in FRAP-S and checked.

3.3 Transient Plenum Temperature Model For FRAP-T1 (R. L. Benedetti)

The mechanical response of a reactor fuel rod during many of the postulated accidents such as loss-of-coolant (LOCA) or power coolant mismatch (PCM) depend on the internal pressure of the fuel rod. To calculate internal pressure, the temperature for all gas volumes in the fuel rod must be calculated. With the fuel rod gas plenum representing, in some cases, over 50% of the free internal volume, an accurate determination of the plenum temperature is desirable.

A new steady state and transient plenum thermal model was developed and added to FRAP-T1 Mod-2 to accurately calculate the plenum gas temperature. The plenum model has been added to FRAP-T1 by the addition of subroutine PLNT. This model, as accurately as possible, models all thermal interactions between the plenum gas and the end pellet surface, holddown spring, and cladding wall. The basic equations comprising the model sensitivity results and LOCA analysis results are as follows.

3.3.1 Plenum Energy Equations. Figure IV-11 is a schematic of the fuel rod plenum geometry and the various energy exchange mechanisms that are modeled. Three assumptions were made in order to simplify the formulation of the energy equations for the cladding, spring, and gas.

- (1) The end or insulator pellet and coolant temperatures are assumed to be independent of the plenum temperature.
- (2) The plenum gas is assumed to be well mixed by natural convection.
- (3) Temperature gradients in the spring and cladding are assumed to be small.

Fig. IV-11 Plenum energy flow model.

The first assumption allows the end pellet and coolant temperatures to be treated as independent variables. The second permits the gas to be modeled by one lumped mass with average properties. Using the third assumption, the temperature response of the cladding and spring can be represented by a small number of lumped masses. Accordingly, the spring was modeled by two nodes of equal mass, center and surface (Figure IV-12). The cladding was modeled by three nodes, two surface nodes, and one center node. The center node has twice the mass of the surface nodes (Figure IV-13). This nodalization scheme results in a set of six energy equations from which the plenum thermal response can be calculated. The transient energy equations for the gas, spring, and cladding are as follows (nomenclature for the plenum energy equations is given in Table IV-II):

Fig. IV-12 Spring noding.

Fig. IV-13 Cladding noding.

Plenum Gas:

$$\begin{aligned}
 \rho_g V_g C_g \frac{\partial T_g}{\partial t} &= A_{ep} h_{ep} (T_{ep} - T_g) + A_{cl} h_{cl} (T_{cli} - T_g) \\
 &+ A_{ss} h_s (T_{ss} - T_g)
 \end{aligned} \tag{84}$$

Spring Center Node:

$$V_{sc} C_s \rho_s \frac{\partial T_{sc}}{\partial t} = \ddot{q} V_{sc} + \frac{A_{sc} K_s (T_{ss} - T_{sc})}{R_{ss}} \quad (85)$$

Spring Surface Node:

$$\begin{aligned} V_{ss} C_s \rho_s \frac{\partial T_{ss}}{\partial t} = & \ddot{q} V_{ss} + A_{sc} K_s (T_{sc} - T_{ss}) \\ & + A_{ss} h_{rads} (T_{cli} - T_{ss}) + A_{ss} h_s (T_g - T_{ss}) \\ & + A_{ss} h_{cons} (T_{cli} - T_{ss}) \end{aligned} \quad (86)$$

where h_{cons} is the conductance between the spring and cladding. The conductance h_{cons} is only used when a stagnant gas condition exists, i.e., when the natural convection heat transfer coefficient for the spring (h_g) is zero.

Cladding Interior Node:

$$\begin{aligned} \rho_{cl} C_{cl} V_{cli} \frac{\partial T_{cli}}{\partial t} = & A_{cl} h_{radc} (T_{ss} - T_{cli}) \\ & + A_{cl} h_{cl} (T_g - T_{cli}) + A_{cl} h_{conc} (T_{ss} - T_{cli}) \\ & + \frac{A_{cl} K_{cl}}{\Delta r/2} (T_{clc} - T_{cli}) + \ddot{q} V_{cli} \end{aligned} \quad (87)$$

Cladding Central Node:

$$\begin{aligned} \rho_{cl} C_{cl} V_{clc} \frac{\partial T_{clc}}{\partial t} = & \ddot{q} V_{clc} + \frac{A_{cl} K_{cl}}{\Delta r/2} (T_{cli} - T_{clc}) \\ & + \frac{A_{cl} K_{cl}}{\Delta r/2} (T_{clo} - T_{clc}) \end{aligned} \quad (88)$$

Cladding Exterior Node:

$$\rho_{cl} C_{cl} V_{clo} \frac{\partial T_{clo}}{\partial t} = \ddot{q} V_{clo} + \frac{A_{cl} K_{cl}}{\Delta r/2} (T_{clc} - T_{clo}) - A_{cl} \ddot{q}_{cl} \quad (89)$$

where $\ddot{q}_{cl}$ is the cladding surface heat flux to the coolant, which includes both convection and radiation, and is given by:

$$\ddot{q}_{cl} = h_{conv} (T_{clo} - T_{cool}) + \epsilon \sigma (T_{clo}^4 - T_{cool}^4) \quad (90)$$

TABLE IV-II

NOMENCLATURE

I. Quantities

A	=	surface area
C	=	heat capacitance
DIAC	=	diameter of the spring coil
DIAS	=	diameter of the spring wire
$\bar{F}_{1-2}$	=	gray-body shape factor from body 1 to body 2
F_{1-2}	=	view factor from body 1 to body 2
Gr	=	Grashof number
h	=	surface heat transfer coefficient
I	=	radiation flux
ID	=	inside diameter of the cladding
K	=	thermal conductivity
L	=	length
OD	=	outside diameter of the cladding
Pr	=	Prandtl number
q	=	energy
$\ddot{q}$	=	surface heat flux
$\ddot{\ddot{q}}$	=	volumetric heat generation
R	=	radius
Δr	=	thickness of the cladding $(OD-ID)/2.0$
T	=	temperature
V	=	volume
σ	=	Stefan-Boltzmann constant
ρ	=	density
Σ	=	absorption coefficient

TABLE IV-II (contd.)

ϵ = emissivity

δ = spring-to-cladding spacing (ID-DIAC)/2.0

II. Subscripts Meaning

cl = cladding

clc = cladding center node

cli = cladding interior node

clo = cladding outside node

cool = coolant

conc,
cons = conduction between the spring and cladding

ep = end pellet

g = gas

p = plenum

sc = spring center node

ss = spring surface node

s = spring

rads,
radc = radiation heat transfer between the spring and cladding

γ = gamma radiation

III. Superscripts Meaning

$m,$
 $m+1$ = old and new time step

For steady state, the time derivatives of temperature on the left-hand side of Equations (84) through (89) were set to zero, and the temperature distribution in the spring and cladding was assumed uniform.

To solve Equations (84) through (89), they were rewritten in the Crank-Nicolson^[IV-40] implicit finite difference form. This formulation resulted in a set of six equations and six unknowns. The six finite difference equations were solved by the method of successive substitution. This method of solution results in the same cladding exterior surface temperature formulation as is used in the Heat 1 subcode (HT1TDP) in FRAP-T1.

$$\bar{A}T_{clo}^{m+1} + \bar{B} = \dot{q}_{cl}^{m+1} \quad (91)$$

where $\bar{A}$ and $\bar{B}$ are constants, $m+1$ indicates quantities at the new time and

$$\dot{q}_{cl}^{m+1} = h_{conv} (T_{cool}^{m+1}) + \epsilon \sigma [(T_{clo}^{m+1})^4 - (T_{cool}^{m+1})^4] \quad (92)$$

The PLNT subroutine first calculates the constants $\bar{A}$ and $\bar{B}$ and then solves for the cladding exterior surface temperature. The remaining cladding, spring, and gas temperatures are calculated by direct substitution into the finite difference equations.

3.3.2 Natural Convection Heat Transfer Coefficient for the Plenum Model. The natural convection film coefficients for the end pellet, spring, and cladding, (h_{ep} , h_s , and h_{cl} respectively) are calculated in subroutine PLNT. The correlations used for these coefficients are those given by Kreith^[IV-41] and McAdams^[IV-42] for laminar and turbulent natural convection from flat plates, horizontal cylinders, and vertical surfaces.

The flat plate natural convection coefficients used for the end pellet surface heat transfer are:

- (a) For laminar conditions on a heated surface

$$h_{ep} = 0.54 K_g (Gr \times Pr)^{0.25}/ID \quad (93)$$

- (b) For turbulent conditions, Grashof number (Gr) greater than 2.0×10^7 , on a heated surface

$$h_{ep} = 0.14 K_g (Gr \times Pr)^{0.33}/ID \quad (94)$$

- (c) For laminar conditions on a cooled surface

$$h_{ep} = 0.27 K_g (Gr \times Pr)^{0.25}/ID \quad (95)$$

The following natural convection coefficients for horizontal cylinders are used for the film coefficient for the spring:

- (a) For laminar condition

$$h_s = 0.53 K_g (Gr \times Pr)^{0.25}/DIAS \quad (96)$$

- (b) For turbulent conditions, Gr from 10^9 to 10^{12} ,

$$h_s = 0.18 (T_g - T_{ss})^{0.33} \quad (97)$$

The vertical surface natural convection coefficients used for the cladding interior surface are given by:

- (a) For laminar conditions

$$h_{cl} = 0.55 K_g (Gr Pr)^{0.25} / L_p \quad (98)$$

- (b) For turbulent conditions, Gr greater than 10^9 ,

$$h_{cl} = 0.021 K_g (Gr Pr)^{0.4} / L_p \quad (99)$$

The validity of using these coefficients in an enclosed volume such as a fuel rod plenum is questionable. The natural convection correlations described above were derived for flat plates, horizontal cylinders, and vertical surfaces in an infinite gas volume. Heat transfer coefficients calculated using these correlations are expected to be higher than those actually existing within the confined space of the plenum. However, until plenum temperature experimental data are available these coefficients are believed to provide a best estimate for the true values.

3.3.3. Conduction Heat Transfer Between the Spring and Cladding. Conduction of energy between the spring and cladding is represented by the heat transfer coefficients h_{cons} and h_{conc} in Equations (86) and (87). These coefficients are calculated in subroutine PLNT when stagnant gas conditions exist. The conduction coefficients are calculated based on the spring and cladding geometries shown in Figure IV-14, and the assumptions that:

- (1) The cladding and spring surface temperatures are uniform
- (2) Energy is conducted only in the direction perpendicular to the cladding wall (heat flow is one dimensional).

Using the above assumptions and the geometry given in Figure IV-14, the energy (q) conducted from an elemental surface area of the spring ($R_s \cdot d\theta \cdot L_s$) to the cladding is

$$dq = \frac{K_g (T_{ss} - T_{cli}) [L_s \cdot R_s \sin(\theta) d\theta]}{(\delta + R_s - R_s \sin \theta)} \quad (100)$$

Integrating Equation (100) over the surface area of the spring facing the cladding, the total flow of energy is:

$$q = \frac{K_g A_{ss}}{\pi} (T_{ss} - T_{cli}) \left[\frac{-\pi}{2 R_s} + \frac{2}{R_s} \left\{ \sqrt{\frac{1}{1-R_s^2}} + \tan^{-1} \frac{s}{(\delta+2R_s)^2} \right\} \right]$$

$$\left(\tan(\theta/2) - \frac{R_s}{(\delta + 2R_s)} \sqrt{1 - \frac{R_s^2}{(\delta + 2R_s)^2}} \right) \left. \vphantom{\frac{R_s}{(\delta + 2R_s)}} \right|_{\theta = 0.0}^{\theta = \pi/2} \quad (101)$$

The two conduction heat transfer coefficients are given by:

$$h_{\text{cons}} = q/A_{\text{ss}} (T_{\text{ss}} - T_{\text{cli}}) \quad (102)$$

and

$$h_{\text{conc}} = h_{\text{cons}} A_{\text{ss}}/A_{\text{cl}} \quad (103)$$

When natural convection heat transfer exists, h_{cl} or $h_{\text{g}} > 0.0$, it is assumed that energy flows to the gas from the spring and then from the gas to the cladding wall, or vice versa. Under these conditions h_{cons} and h_{conc} are set to zero. Therefore, in the current version of PLNT, h_{cons} and h_{conc} are used only when the temperature is uniform throughout the plenum. Future plenum data or analytical analysis may indicate that natural convection flow between spring and cladding does not exist. If this is true, the conduction coefficient will be used at all times.

3.3.4 Radiation Heat Transfer Between the Spring and the Cladding. Transport of energy by radiation between the spring and cladding is included in the plenum model by use of the heat transfer coefficients h_{rads} and h_{radc} in Equations (86) and (87). These coefficients are calculated in subroutine PLNT. They are derived from the radiant energy exchange equation for two grey bodies in thermal equilibrium^[IV-41] as follows:

$$q_{1-2} = A_1 \bar{F}_{1-2} \sigma (T_1^4 - T_2^4) \quad (104)$$

where q_{1-2} is the net rate of heat flow by radiation between bodies 1 and 2.

Fig. IV-14 Geometrical relationship between the cladding and spring.

The gray-body shape factor (F_{1-2}) is related to the geometrical view factor (F_{1-2}) from body 1 to body 2 by:

$$A_1 \bar{F}_{1-2} = \frac{1}{(1-\epsilon_1)/A_1 \epsilon_1 + 1/A_1 F_{1-2} + (1-\epsilon_2)/A_2 \epsilon_2} \quad (105)$$

Using Equations (104) and (105) and approximating the geometrical view factor from the cladding to the spring (F_{cl-s}) by:

$$F_{cl-s} = \frac{A_{ss}}{2 A_{cl}} \frac{(2A_{cl} - A_{ss}) A_{ss}}{4 A_{cl}^2} \quad (106)$$

The net radiant energy exchange between the cladding and spring is written as:

$$q_{cl-s} = A_{cl} \bar{F}_{cl-s} \sigma (T_{cli}^4 - T_{ss}^4) \quad (107)$$

The radiation heat transfer coefficients h_{radc} and h_{rads} are calculated by:

$$h_{radc} = q_{cl-s} / A_{cl} * (T_{cli} - T_{ss}) \quad (108)$$

$$h_{rads} = (h_{radc} * A_{cl}) / A_{ss} \quad (109)$$

3.3.5 Gamma Heating of the Spring and Cladding. The volumetric power generation term $\dot{q}$, shown in Equations (85) to (89), represents the gamma radiation heating of the spring and cladding. A simple relationship is used to calculate $\dot{q}$ in subroutine PLNT. The relationship used was derived from the gamma flux attenuation equation:

$$-dI(x) = \Sigma_{\gamma} I(x) dx \quad (110)$$

where $I(x)$ is the gamma flux, Σ_{γ} is the gamma ray absorption coefficient, and x is the spatial dimension of the solid on which the gamma radiation is incident. Since the cladding and spring are thin in cross section, it can be assumed that the gamma ray flux is constant (I) throughout the volume. The portion of the gamma flux (I), incident on the spring and cladding, absorbed (ΔI) therefore can be described by:

$$-\Delta I = \Sigma_{\gamma} I \bar{x} \quad (111)$$

where $\bar{x}$ is the thickness of the spring or cladding. Therefore, the volumetric gamma ray absorption rate is given by:

$$-\frac{\Delta I}{\bar{x}} = \Sigma_{\gamma} I \quad (112)$$

Equation (112) can also represent gamma volumetric energy deposition by letting I represent the energy flux associated with the gamma radiation. Approximately 10% of the energy released in the fissioning of uranium is in the form of high energy gamma radiation. Therefore, the gamma energy flux leaving the fuel rod would be approximately equal to 10% of the thermal flux. The gamma energy flux throughout the reactor can then be estimated by:

$$I = 0.10 \dot{q}_{\text{rod}} \quad (113)$$

where P_{rod} is the average fuel rod surface heat flux. For zirconium, Σ_{γ} is approximately 11.0 ft^{-1} . Therefore, the gamma energy deposition rate is given by:

$$\left| -\frac{\Delta I}{x} \right| = \dot{q} = 1.1 \dot{q}_{\text{rod}} \quad (114)$$

Equation (114) is an estimate of the gamma heating rate for the spring and cladding. Reactor data should be collected to determine the magnitude of the gamma flux expected at the location of the plenum. The development of a more sophisticated analytical model is currently in progress.

3.3.6 Sensitivity Analysis of the New Plenum Model. A driver program was used to perform a sensitivity analysis for the plenum gas temperature predicted by the new plenum model. The analysis was made using typical pressurized water reactor (PWR) plenum component dimensions and material properties (Table IV-III). Gamma heating of the spring and cladding was set to zero, and the end pellet temperature was set at 1500°F . Hypothetical LOCA coolant temperatures and heat transfer conditions were used to drive the plenum thermal transient (Figure IV-15). The response of the gas, spring, and cladding temperatures to the hypothetical LOCA coolant conditions is shown in Figure IV-16. Note that during the early stages of the transient, the plenum gas temperature is significantly below the coolant and cladding temperatures, but above the spring temperature. The cladding acts as a source of energy for the gas, while the spring acts as a sink. Therefore, the presence of the spring in the model adds thermal capacitance to the gas, slowing its response to varying boundary conditions occurring at the cladding wall.

The effects on the plenum gas temperature of variations in end pellet temperature, natural convection heat transfer from the spring and cladding (h_s and h_{cl} respectively), and the spring volume were analyzed. A reduction of end pellet temperature from 1500°F to 750°F produced a 10°F change in peak gas temperature (Figure IV-17). The insensitivity of the gas temperature to end pellet temperature is due to the small heat transfer surface area of the end pellet compared to the cladding and spring surface areas. The maximum gas temperature was changed approximately 50°F (a 12.5% change) by a 50% change in both h_s and h_{cl} (Figures IV-18 and IV-19). However, a decrease in h_s results in an increase in the gas temperature, while a decrease in h_{cl} resulted in a decreased gas temperature and vice versa for an increase in h_s and h_{cl} . These opposite effects are due to the fact that during the transient the spring acts as an energy sink while the cladding acts as an energy source.

The sensitivity of the gas temperature to the presence of the spring was determined by multiplying the spring volume by a factor of 10^{-8} . This reduction in spring volume increased the maximum gas temperature by 300°F (Figure IV-20). With no spring, the gas temperature responds quickly to cladding temperature changes resulting in a gas temperature nearly equal to that of the coolant.

TABLE IV-III

PHYSICAL CONSTANT USED FOR SENSITIVITY ANALYSIS

1. Dimensional Quantities

cladding ID = 0.378 inch

cladding OD = 0.422 inch

plenum length = 8.7 inches

spring coil diameter = 0.35 inch

spring wire diameter = 0.076 inch

spring pitch = 0.25 inch

2. Material Properties

cladding density = 4.10.0 lbm/ft³

cladding specific heat = 0.081 Btu/lbm-°F

cladding thermal conductivity = 6.98 Btu/hr-ft-°F

emissivity of the clad = 0.6

spring density (stainless steel) = 585.0 lbm/ft³

spring specific heat = 0.12 Btu/lbm-°F

spring thermal conductivity = 9.72 Btu/hr-ft-°F

emissivity of the spring = 0.6

plenum gas density (helium) = 0.52 lb/ft³

plenum gas specific heat = 1.24 Btu/lbm-°F

plenum gas thermal conductivity = 0.10 Btu/hr-ft-°F

plenum kinematic viscosity = 0.1395 ft²/hr

Only small temperature variations are found in the spring and cladding (Figure IV-16). If desired, it is possible to represent the spring and cladding each by one lumped node.

3.3.7 FRAP-T1 Plenum Gas Temperature Calculations Under LOCA Conditions. Prior to the inclusion of the new plenum model in FRAP-T1, the plenum gas temperature was prescribed as the coolant temperature at the top of the coolant channel. A comparison of

Fig. IV-15 Coolant temperature and heat transfer coefficient for hypothetical LOCA.

Fig. IV-16 Plenum gas, spring, and cladding temperature responses to hypothetical LOCA conditions with an insulator pellet temperature (T_{ep}) of 1,700°F.

Fig. IV-17 Plenum gas temperature response as a function of end pellet temperature (T_{ep}).

Fig. IV-18 Plenum gas temperature response for a 50% change in the spring natural convection heat transfer coefficient (h_s).

TEMPERATURE °F

Fig. IV-19 Plenum gas temperature response for a 50% change in the cladding natural convection heat transfer coefficient (h_{cl}).

Fig. IV-20 Temperature response with and without a spring.

the plenum gas temperature produced by this procedure and those calculated by the new plenum model are shown in Figure IV-21 for a standard FRAP-T1 LOCA calculation^[IV-43]. As can be seen, the new plenum model predicts the initial gas temperature higher than the coolant temperature. This higher temperature is due to the inclusion of gamma heating of the spring and cladding in the new plenum model. Under steady state conditions, the spring, cladding, and therefore the gas temperatures must be higher than the coolant temperature in order to dissipate energy to it. Using the new plenum model, the plenum gas temperature is only slightly affected by the large and rapid superheat coolant temperature variations and the response of the plenum gas temperature being damped by the transfer of heat from gas to the spring.

3.4 Reactivity Initiated Accident Analysis (R. L. Casperson)

A subroutine has been developed as part of the FRAP-T1 code to calculate the loads generated in reactor fuel rods during a reactivity initiated accident (RIA). The FRIDA (Fuel Reactivity Insertion Dynamic Analysis) subroutine utilizes a one-dimensional, discrete analysis to solve for the longitudinal response of a rod subjected to thermal transients. These thermal transients can be general in form and are input at arbitrary axial rod nodes as temperature-time histories formulated by the user or generated by the FRAP-T1 code.

Fig. IV-21 LOCA plenum gas temperature response calculated by FRAP-T1.

The rod to be analyzed is modeled discretely so that the mass, damping, and stiffness properties are described at nodal or collocation points and take matrix forms. Since, in this case, only one axial coordinate (degree-of-freedom) is allowed at each node, the property matrices will have the same order as the number of nodes. The model nodes can either coincide with the input temperature nodes, or, if they differ in number, will be equally spaced along the rod length.

The output of the subroutine is axial values of displacement, velocity, acceleration, load, stress, strain and strain rate, and cladding hoop stress, all as functions of time. Also the output contains the minimum and maximum values of these variables and the times at which they occur.

Mass properties are assumed constant in time, but stiffness and damping properties vary with temperature (consequently, time). The values for fuel elastic modulus and linear coefficient of thermal expansion are taken from MATPRO [IV-44].

3.4.1 Basic Formulation. The governing equations for an N degree of freedom (d.o.f.) discrete structural system are second-order, linear differential equations. In matrix notation these equations can be written as

$$[M]\{\ddot{q}\} + [C]\{\dot{q}\} + [K]\{q\} = \{P\} \quad (115)$$

where

- $[M]$ = (NxN) matrix of mass coefficients
- $[C]$ = (NxN) matrix of damping coefficients
- $[K]$ = (NxN) matrix of stiffness coefficients
- $\{q\}$ = (Nx1) matrix of generalized displacement coordinates
- $\{\dot{q}\}$ = (Nx1) matrix of generalized velocity coordinates
- $\{\ddot{q}\}$ = (Nx1) matrix of generalized acceleration coordinates
- $\{P\}$ = (Nx1) matrix of forces applied at the discrete system node points.

The general form for $\{P\}$ is

$$\{P\} = \{F\} - \{Q\} \quad (116)$$

where $\{F\}$ is the vector of externally applied, mechanical forces and $\{Q\}$ is the vector of nodal thermal forces.

Equation (115) is a standard form that can be found in any text on structural dynamics (e.g., Reference IV-45). Also, this equation can be written in indicial form as

$$\sum_j M_{ij} \ddot{q}_j + \sum_j C_{ij} \dot{q}_j + \sum_j k_{ij} q_j = P_i \quad (117)$$

for $i = 1, 2, \dots, N$ where N is the number of d.o.f., or the number of nodes in the case of an axial rod system. The displacement, velocity, acceleration, and applied force at the i^{th} node are given, respectively, by q_i , $\dot{q}_i$, $\ddot{q}_i$ and P_i . Figure IV-22 depicts the displacement coordinates and applied forces for a typical axial structural system.

In the present formulation, the FRIDA subroutine calculates the mass and stiffness properties for a free-free rod as depicted in Figure IV-22. However, in the response calculations, this free-free system is restrained at the rod bottom node by eliminating the appropriate rows and columns in the mass and stiffness matrices.

The Equations (115) and (117) are applied, then, to an N d.o.f. fixed system. The free-free mass and stiffness matrices are of the order $N+1$.

3.4.2 Thermal Forces. The forces P in Equation (115) can be written in a more explicit form if only thermal forces are assumed to be acting. For this case, Equation (116) becomes

$$\{P\} = -\{Q\} \quad (118)$$

From Reference IV-45, and considering only a single rod element as shown in Figure IV-23, Equation (118) becomes for the Δ^{th} element,

$$\{\bar{P}\}^{\Delta} \equiv -\{Q\}^{\Delta} = - (AE\alpha T)^{\Delta} \begin{Bmatrix} 1 \\ -1 \end{Bmatrix} \quad (119)$$

where

$$\{\bar{P}\}^{\Delta} = \begin{Bmatrix} \bar{P}_1 \\ \bar{P}_2 \end{Bmatrix}^{\Delta}$$

and

$$\{Q\}^{\Delta} = \begin{Bmatrix} \bar{Q}_1 \\ \bar{Q}_2 \end{Bmatrix}^{\Delta} = \begin{Bmatrix} AE\alpha T \\ -AE\alpha T \end{Bmatrix}^{\Delta}$$

The superimposed bar notation denotes the fact that the quantities are referenced to an elemental or local coordinate system. Also in the above equations,

A = element cross-sectional area

E = element elastic modulus

α = element linear coefficient of thermal expansion

T = element temperature change from some reference temperature.

Fig. IV-22 Global coordinate and element numbering system for an N-d.o.f. axial system (restrained).

The thermal forces for all N of the elements expressed in local coordinates can be written as one vector by

$$\{\bar{Q}\} = \begin{bmatrix} \{\bar{Q}\}^{(1)} \\ \{\bar{Q}\}^{(2)} \\ \vdots \\ \{\bar{Q}\}^{(\Delta)} \\ \vdots \\ \{\bar{Q}\}^{(N)} \end{bmatrix} = \begin{bmatrix} \{AE\alpha T\}^{(1)} \\ \{-AE\alpha T\} \\ \{AE\alpha T\}^{(2)} \\ \{-AE\alpha T\} \\ \vdots \\ \{AE\alpha T\}^{(\Delta)} \\ \{-AE\alpha T\} \\ \vdots \\ \{AE\alpha T\}^{(N)} \\ \{-AE\alpha T\} \end{bmatrix} \quad (120)$$

Also, the local displacement coordinates for all N elements of the rod can be written as one vector by

$$\{\bar{q}\} = \begin{bmatrix} \{\bar{q}\}^{(1)} \\ \{\bar{q}\}^{(2)} \\ \vdots \\ \{\bar{q}\}^{(\Delta)} \\ \vdots \\ \{\bar{q}\}^{(N)} \end{bmatrix} = \begin{bmatrix} \{\bar{q}_1\}^{(1)} \\ \{\bar{q}_2\} \\ \{\bar{q}_1\}^{(2)} \\ \{\bar{q}_2\} \\ \vdots \\ \{\bar{q}_1\}^{(\Delta)} \\ \{\bar{q}_2\} \\ \vdots \\ \{\bar{q}_1\}^{(N)} \\ \{\bar{q}_2\} \end{bmatrix} \quad (121)$$

ANC - A - 6204

Fig. IV-23 Local force and displacement coordinates.

These local displacements can be related to the global displacements of the total system node points $\{q\}$ by a displacement compatibility matrix $[A]$, as

$$\{\bar{q}\} = [A] \{q\} \quad (122)$$

For the axial case under consideration, $[A]$ will consist primarily of zero elements with a lesser number of ones in the appropriate locations. Equation (122) expanded appears, then, as

$$\begin{bmatrix} \begin{Bmatrix} \bar{q}_1 \\ \bar{q}_2 \end{Bmatrix}^{(1)} \\ \begin{Bmatrix} \bar{q}_1 \\ \bar{q}_2 \end{Bmatrix}^{(2)} \\ \vdots \\ \begin{Bmatrix} \bar{q}_1 \\ \bar{q}_2 \end{Bmatrix}^{(N-1)} \\ \begin{Bmatrix} \bar{q}_1 \\ \bar{q}_2 \end{Bmatrix}^{(N)} \end{bmatrix} = \begin{bmatrix} \begin{array}{cc|cc} 0 & 0 & 0 & 0 \\ 1 & 0 & 0 & 0 \end{array} & & \\ \begin{array}{cc|cc} 1 & 0 & 0 & 0 \\ 0 & 1 & 0 & 0 \end{array} & \text{(Zeros)} & \\ & \text{(Zeros)} & \begin{array}{cc|cc} 0 & 1 & 0 & 0 \\ 0 & 0 & 1 & 0 \\ 0 & 0 & 1 & 0 \\ 0 & 0 & 0 & 1 \end{array} \end{bmatrix} \cdot \begin{bmatrix} q_1 \\ q_2 \\ q_3 \\ \vdots \\ q_{N-2} \\ q_{N-1} \\ q_N \end{bmatrix} \quad (123)$$

where the matrix $[A]$ is of dimensions $2N \times N$.

Note that the local coordinate $\bar{q}_1$ of rod element 1 is equal to zero since this node is considered physically and analytically restrained. This is accounted for in $[A]$ by all elements of the first row being zeroes.

Using Equation (122) and the principle of virtual work, it can be shown that the thermal forces in global coordinates are given by

$$\{Q\} = [A]^T \{\bar{Q}\} \quad (124)$$

or, expanded, Equation (124) becomes

$$\{Q\} = \begin{bmatrix} -(AE\alpha T)^1 + (AE\alpha T)^2 \\ -(AE\alpha T)^2 + (AE\alpha T)^3 \\ \vdots \\ -(AE\alpha T)^{N-1} + (AE\alpha T)^N \\ -(AE\alpha T)^N \end{bmatrix} \quad (125)$$

3.4.3 Initial Conditions. The present version of FRIDA is programmed to accept the initial displacements and velocities of the rod axial nodes as input through the subroutine variable list. However, for compatibility with the desired form for the equivalent thermal forces, FRIDA assumes for internal transient analysis that the initial displacements are all zero. For this datum, the transient analysis is performed and the resulting displacements (relative to the datum) are then added to the input set of initial displacements.

The thermal forces at the starting time of the analysis are also zero to correspond with the zero initial relative displacements. For the Δ^{th} rod element, the thermal force vector for the temperature T takes the form

$$\{\bar{Q}^\Delta(T)\} = A^\Delta E^\Delta(T) [(\alpha T) - (\alpha T_o)]^\Delta \cdot \begin{Bmatrix} 1 \\ -1 \end{Bmatrix} \quad (126)$$

where

T = current temperature at time of interest

T_o = reference or initial temperature at start of transient

$E(T)$ = modulus of elasticity at temperature T .

3.4.4 Numerical Solution Technique. Once the matrices $[M]$, $[C]$, $[K]$, and $\{P\}$ of Equation (115) have been determined, the equation is integrated using a fourth order Runge-Kutta numerical procedure^[IV-46]. This direct integration approach is considered preferable to the modal analysis method for this problem for several reasons. For systems with relatively few d.o.f. and a moderate number of time steps, the direct method is competitive in calculation time with the modal method and is less involved conceptually.

This is especially true here since it cannot be presupposed that only the few lowest rod modes will adequately describe rod response for all possible reactivity insertion phenomena. More importantly, since the rod stiffness properties will change dramatically with changes in fuel temperature, not one but many modal analyses would be required for updating rod vibratory modes and frequencies as the temperature changed.

Equation (115), in conjunction with Equation (118), is first modified to obtain the accelerations as

$$\{\ddot{q}\} = -[M]^{-1}\{Q\} - [M]^{-1}[c]\{\dot{q}\} - [M]^{-1}[K]\{q\} \quad (127)$$

where initial values of displacement and velocity, $\{q(0)\}$ and $\{\dot{q}(0)\}$, respectively, are input. The Runge-Kutta numerical integration is then used to integrate $\{\ddot{q}\}$ to obtain $\{\dot{q}\}$. The subroutine TR1 performs the numerical integration.

The fuel rod and its environment and boundaries are modeled as shown in Figure IV-24. The nodes at which response calculations are made may coincide with the input temperature nodes or they may be of a different number and equally spaced as depicted in the figure.

ANC-A-6205

Fig. IV-24 Fuel rod idealization.

The stiffness matrix in Equation (127) is modified to account for fixity at the rod bottom, cladding stiffness, and the plenum spring.

Due to axial temperature variation, the springs that are indicated in Figure IV-24 will, in general, be dissimilar. If the response nodes coincide with the temperature nodes, then the masses shown in the figure may also be dissimilar if the temperature nodes are not equally spaced.

The FRIDA output quantities that are calculated at each longitudinal node of the rod as functions of time are:

- (1) displacement, $\{q(t)\}$
- (2) velocity, $\{\dot{q}(t)\}$
- (3) acceleration, $\{\ddot{q}(t)\}$
- (4) axial load, $\{L(t)\}$
- (5) axial stress $\{\sigma_A(t)\}$
- (6) axial strain, $\{\epsilon_A(t)\}$
- (7) axial strain rate, $\{\dot{\epsilon}_A(t)\}$
- (8) cladding hoop stress, $\{\sigma_H^C(t)\}$.

Each of the above vectors is of size $(N, 1)$.

The axial displacements, velocities, and accelerations of the rod nodes are calculated directly from the Runge-Kutta procedure.

The nodal loads defined in global system coordinates are derived strictly from rod inertial loads which result from nodal mass and accelerations. The thermal forces do not enter into the load computation since they are just equivalent mechanical forces (or, pseudo-forces) used to produce the appropriate rod displacement-time history.

The rod nodal loads are calculated as

$$\{L\}_j = - [M] \{\ddot{q}\} \quad (128)$$

with the axial stress at each node obtained by dividing the internal loads by the rod cross-sectional area which is considered constant. Thus,

$$\{\sigma_A\} = \frac{1}{A} \{L\} \quad (129)$$

An estimate of cladding hoop stress is made utilizing the following assumptions:

- (1) A central portion of the fuel is molten (see Figure IV-25)
- (2) The solid annulus of fuel is radially cracked
- (3) Hoop stress and crack shear forces in the fuel are negligible
- (4) The hydrostatic pressure in the fuel molten core is equal to the average axial stress in the fuel
- (5) The fuel-cladding gap is closed
- (6) The fuel-clad contact surface is frictionless.

From force equilibrium considerations on a segment of cracked fuel (Figure IV-26), the normal surface traction on the fuel-cladding contact surface is given by

$$\sigma_{rr}^O = \frac{r_m}{r_f} \sigma_{rr}^I \quad (130)$$

where

σ_{rr}^O = radial surface stress (traction) on outer fuel boundary

σ_{rr}^I = radial surface stress on inner melt boundary of fuel

r_m = radius of molten fuel

r_f = outside fuel radius/inside cladding radius.

Considering force equilibrium on a segment of cladding (Figure IV-27), the thin-wall shell theory yields the cladding hoop stress ($\sigma_H^C \equiv \sigma_{\theta\theta}^C$) as

$$\sigma_H^C = \frac{\sigma_{rr}^O r_f}{t} \quad (131)$$

This is further simplified by using assumption (4) and Equation (130). Thus, Equation (131) becomes

$$\sigma_H^C = \frac{r_m}{r_f} \sigma_{rr}^I \frac{r_f}{t} = \frac{r_m}{r_f} \sigma_A \frac{r_f}{t} \quad (132)$$

where

σ_A = the average value of axial stress in the fuel, given by Equation (129).

Using Equation (129) the value for the cladding hoop stress output in FRIDA is the vector

$$\{\sigma_H^c\} = \left\{ \frac{r_m}{t} \right\}^T \{\sigma_A\} \quad (133)$$

so that any rod axial node, the i^{th} node hoop stress in the cladding is given in indicial form, from Equation (133), as

$$(\sigma_H^c)_i = \left(\frac{r_m}{t} \right)_i (\alpha_A)_i \quad (134)$$

3.4.5 Results. The FRIDA subroutine was used to analyze the response of a BWR-6 type fuel rod to the thermal transient shown in Figure IV-28. The temperature history depicted in Figure IV-28 was applied to each node of the fuel rod. Other pertinent details of the fuel rod and analytical modeling of the rod are shown in Table IV-IV.

Figures IV-29 through IV-40 show the transient results of the analysis. In viewing the magnitudes of the response results several factors should be kept in mind:

- (1) Cladding hoop stress is highly dependent upon both cladding wall thickness and fuel melt radius. For example, the melt radius used in the case presented was 1/3 of the fuel outside radius. If the fuel were completely molten at some axial node, the hoop stress would be three times the value plotted at that axial position.

ANC - A - 6206

Fig. IV-25 Assumed fuel-clad conditions.

Fig. IV-26 Cracked segment of fuel pellet.

Fig. IV-27 Clad stress equilibrium.

- (2) Certain portions of the output are questionable and should be interpreted with care. Tensile axial load and compressive cladding hoop stress, for example, are questionable within the framework of the analytical model, and/or the physical situation.
- (3) In regards to comment (2), it should be noted that the axial load output from FRIDA is dynamic load only and does not include static dead weight, plenum spring load, nor loads imposed from axial physical constraints of the clad, fuel canisters, etc. These load effects may be included at a later date.

TABLE IV-IV

FRIDA INPUT PARAMETERS

Number of d.o.f. = 5

Fuel stack length = 148 inches

Fuel stack OD = 0.416 inch

Fuel density = 9.73×10^{-4} lb/sec²-in.⁴

Fuel melt radius = 0.069 inch

Clad thickness = 0.034 inch

Integration time step = 1.0×10^{-4} seconds

Δ stiffness factor = 0.05

Fig. IV-28 Temperature history with representative ramp.

Fig. IV-29 Fuel rod response to RIA thermal transient.

Fig. IV-30 Fuel rod response to RIA thermal transient.

Fig. IV-31 Fuel rod response to RIA thermal transient.

Fig. IV-32 Fuel rod response to RIA thermal transient.

Fig. IV-33 Fuel rod response to RIA thermal transient.

Fig. IV-34 Fuel rod response to RIA thermal transient.

Fig. IV-35 Fuel rod response to RIA thermal transient.

Fig. IV-36 Fuel rod response to RIA thermal transient.

Fig. IV-37 Fuel rod response to RIA thermal transient.

Fig. IV-38 Fuel rod response to RIA thermal transient.

Fig. IV-39 Fuel rod response to RIA thermal transient.

Fig. IV-40 Fuel rod response to RIA thermal transient.

- (4) In interpreting the results, it should be remembered that with rapidly increasing temperature the fuel elastic modulus and coefficient of thermal expansion vary dramatically. The fuel can become orders of magnitude softer (less stiff) over the temperature range of an RIA.

3.4.6 Calculation Time. The computer output has been generated using the Berkeley CDC-7600 system. Some representative computation (CPU) times are presented here for comparison purposes. The variable SFACT in the tabulation is the allowable fractional change in rod stiffness before the stiffness and damping matrices are updated in the analysis.

<u>Number of d.o.f.</u>	<u>Number of Integration Time Steps</u>	<u>Value of SFACT</u>	<u>CPU Time (seconds)</u>
5	650	0.05	6
10	500	0.25	4
20	2,400	0.05	27
20	6,500	0.05	66
20	10,000	0.05	101
20	6,000	0.25	41
20	8,400	0.25	55
20	12,200	0.25	65
30	30,500	0.25	294

It should be noted in the previous tabulation that CPU time is practically insensitive to the number of d.o.f. used in the rod model.

Reducing the input variable SCFACT by a factor of 5 causes a 50 to 100% increase in CPU time. However, although not shown here, the decrease in SFACT from 0.25 to 0.05 produced a significant improvement in rod response maximum values. This is not too surprising because of the large changes in stiffness properties that can occur between widely spaced temperature-time points.

3.4.7 Conclusions. Subroutine FRIDA provides a straightforward and efficient method of calculating axial response of a fuel rod subjected to an RIA transient. It also provides a reasonable approximation to the cladding hoop stress under RIA conditions.

Input to the subroutine is simple, entailing only basic rod properties and temperature-time histories at axial positions of the rod. The subroutine is capable of independently studying an entire RIA time history or it can be used coupled with other phenomena models over short increments of time histories.

The FRIDA subroutine is, of course, not limited to fuel rod analysis, but rods in general. Also, it is not limited to RIA analysis, but thermal transients in rods in general.

3.5 Fuel Elastic Modulus (C. S. Olsen)

The elastic modulus for UO_2 and $\text{UO}_2\text{-PuO}_2$ fuels has an important bearing on fuel pellet-to-cladding interactions and as such a model for this property is included in the general LWR fuel properties subroutine MATPRO. The elastic modulus model is coded under the name FELMOD.

3.5.1 FELMOD. Fuel ELastic MODulus (FELMOD) calculates the elastic modulus for UO_2 and $\text{UO}_2\text{-PuO}_2$ mixed oxides as a function of porosity and temperature. The available data supports the assumption of a linear decrease in elastic modulus with both increasing temperature and increasing porosity. The relationships described here and programmed in FELMOD are:

$$E = 2.26 \times 10^{11} (1 - 1.131 \times 10^{-4} T) [1 - 2.62 (1 - D)] \quad (135)$$

$$E = 2.52 \times 10^{11} (1 - 7.843 \times 10^{-4} T) [1 - 2.03 (1 - D)] \quad (136)$$

for UO_2 and $\text{UO}_2\text{-PuO}_2$ respectively, where

E = Young's Modulus (Pascals)

T = Temperature ($^{\circ}\text{C}$) (range 0 C - 1300 $^{\circ}\text{C}$)

D = Fraction of theoretical density.

The UO_2 data have a standard deviation with respect to Equation (135) of 0.037×10^{11} Pascals. The mixed oxide equation is less certain and the limited data do not allow an accurate quantitative estimate of error.

3.5.2 UO_2 Elastic Modulus. The elastic modulus for UO_2 is a function of porosity, temperature, oxygen/metal ratio, and irradiation and potentially a function of other parameters such as grain size and impurity level. The variables considered to be the most significant, and those for which data are available, are temperature and porosity. The following paragraphs describe the effect of these two variables on the UO_2 elastic modulus and a model is developed and compared with experimental data.

3.5.2.1 Porosity Dependency of UO_2 Elastic Modulus at Room Temperature. Elastic modulus for stoichiometric UO_2 has been determined at room temperature (20 $^{\circ}\text{C}$) as a function of porosity by the resonant frequency [IV-47-IV-53] and bending [IV-54,-55] techniques. Since deformation measurements lack the accuracy needed to determine elastic modulus, only the more reliable resonant frequency measurements were used in a least-squares regression analysis to derive the coefficients E_0 and β in Equation (137).

$$E = E_0 [1 - \beta (1 - D)] \quad (137)$$

where

$$E_0 = \text{Young's modulus for 100\% TD UO}_2 \text{ at } 20^\circ\text{C} = 2.256 \times 10^{11} \text{ Pascals}$$

$$\beta = \text{porosity coefficient} = 2.62 \text{ (unitless)}$$

and E and D have been defined previously.

Equation (137) results in a value of zero elastic modulus at 0.38 volume fraction porosity (0.62) of theoretical density. Exponential functions that relate Young's modulus to porosity have been used empirically [IV-56], but these relationships do not yield zero elastic modulus with increasing porosity. Theoretical considerations for a material composed of uniform spherical particles predict zero elastic modulus for porosities less than 0.4764 volume fraction [IV-57]. Alao Gatto [IV-58] predicted zero elastic modulus at 0.424 volume fraction porosity using the theory of sound propagation. The value 2.62 for β is consistent with these theoretical considerations.

The 2.256×10^{11} value in Equation (137) for E_0 for theoretically dense (10.96 g/cc) UO_2 at 20°C is between 2.170×10^{11} Pascals (Reuss Average) and 2.440×10^{11} Pascals (Voigt average) [IV-59]. These average values are respectively lower and upper bounds for Young's modulus of isotropic theoretically dense UO_2 [IV-60]. Thus 2.256×10^{11} Pascals appears to be a good estimate for E_0 . Equation (137) is compared with experimental data taken at 20°C in Figure IV-41.

3.5.2.2 Temperature Dependency of UO_2 Elastic Modulus. Elastic modulus data were obtained as a function of temperature by Padel and Novion [IV-49], Wachtman et al [IV-52], and Belle and Lustman [IV-53] and were used in a least-squares regression analysis using Equation (138) to determine the temperature dependency of Young's modulus.

$$E = E_0 (1 - \alpha T) \quad (138)$$

where

$$E_0 = \text{Young's modulus at } 0^\circ\text{C}$$

$$\alpha = \text{temperature coefficient} = 1.131 \times 10^{-4} \text{ }^\circ\text{C}^{-1}$$

and E and T have been previously defined.

Equation (137) was used to normalize the modulus data from UO_2 of different densities to values for theoretically dense UO_2 . The low density data (91%TD) from Padel and Novion [IV-49] and all of the data (93%TD) from Belle and Lustman [IV-53] lie on a line that is nearly parallel to that of the remaining normalized data, but which results in a lower modulus at a given temperature (Figure IV-42). The room temperature modulus obtained from this data from the low density fuel and extrapolated to theoretically dense UO_2 is

Fig. IV-41 Comparison of Young's modulus data for UO_2 at 25°C and the fraction of porosity, from a least squares fit to Equation (135).

Fig. IV-42 Comparison of Young's modulus for UO_2 normalized to 100% TD as a function of temperature, with least square fits to Equation (136).

2.143×10^{11} Pascals, which is outside the range expected for isotropic, theoretically dense UO_2 [IV-59]. Young's modulus determined from the remaining data was 2.2×10^{11} Pascals which is in the expected range. However, both sets of data resulted in nearly the same slope given by a in Equation (138), thus indicating that the temperature coefficient is independent of porosity. The average value of $1.131 \times 10^{-4} \text{ C}^{-1}$ is used for the temperature dependency, a .

Equation (138) predicts a linear decrease in Young's modulus as the temperature increases. For temperatures between room temperature and about $1,000^\circ\text{C}$, this linear decrease has been observed experimentally on other oxide systems [IV-61]. However, with other ceramic materials [IV-61, 47], grain boundary sliding occurs at high temperatures. When sliding occurs, the modulus decreases more rapidly with temperature than at lower temperatures. This phenomena has been observed for UO_2 [IV-60] and also contributes to creep deformation. The abrupt change in elastic modulus with temperature expected at temperatures $>1,300^\circ\text{C}$ has not been modeled because of the lack of data as a function of temperature and porosity for this phenomenon. The extrapolation of Equation (138) to temperatures higher than $1,300^\circ\text{C}$ is therefore dubious.

3.5.2.3 UO_2 Elastic Modulus as a Function of Both Porosity and Temperature. The resultant expression for Young's modulus as a function of temperature and porosity, assuming that the temperature and porosity coefficient are independent, [Equation (139)] was obtained by combining Equations (137) and (138). The best-estimate value for E_0 derived in Equation (137) for theoretically dense (10.96 g/cc) UO_2 was normalized to 0°C using Equation (138) and used in Equation (139). Then for stoichiometric UO_2 , with the standard deviation shown,

$$E = 2.26 \times 10^{11} (1 - 1.131 \times 10^{-4} T) [1 - 2.62 (1 - D)] \pm 0.037 \times 10^{11} \quad (139)$$

where

E = Young's modulus (Pascals)

T = temperature ($^\circ\text{C}$)

D = fraction of theoretical density.

3.5.2.4 Effect of Irradiation on UO_2 Elastic Modulus. Equation (139) can be used as an approximation for irradiated fuel although it has been derived solely from measurements on unirradiated fuel. The porosity term can be used to account for the changes in porosity induced by irradiation. While other structural changes such as cracking would not be accounted for, Marlowe and Kaznoff [IV-47] have indicated that fission products in small concentration (10% or less) would have a negligible effect upon the elastic modulus. The minor effect of solute additions is due to the fact that Young's modulus is primarily determined by the interatomic bonding of the solvent.

3.5.3 UO₂-PuO₂ Elastic Modulus. As with UO₂, temperature and porosity are considered to be the most significant variables affecting the elastic modulus of U_xPu_{1-x}O₂. However, in the case of mixed oxides, the oxygen/metal ratio at room temperature has also been found to be important.

3.5.3.1 O/M Ratio Dependency of Mixed Oxide Elastic Modulus. Padel and Novion [IV-49] determined the Young's modulus for 95% theoretical density U_{0.8}Pu_{0.2}O₂ ± x between 25 and 1300°C. A Young's modulus of 1.808 x 10¹¹ Pascals was measured at 25°C for an oxygen/metal ratio of 1.962; for an oxygen/metal ratio of 2.00, a Young's modulus of 2.265 x 10¹¹ Pascals was measured. However, while Young's modulus is observed to vary strongly with the change in oxygen/metal ratio in mixed oxides, the data are not extensive enough to support an empirical modeling effort. FELMOD will be restricted to the stoichiometric case.

3.5.3.2 Temperature Dependency of (U, Pu)O₂ Elastic Modulus. Young's modulus for stoichiometric UO₂-PuO₂ was found to decrease linearly with temperature as follows [IV-49]:

$$E = 2.265 \times 10^{11} (1 - 7.843 \times 10^{-4} T) \quad (140)$$

where

E = Young's modulus (Pascals)

T = temperature (°C).

3.5.3.3 Porosity Dependency of (U, Pu)O₂ Elastic Modulus. Nutt et al [IV-62] determined Young's modulus for U_{0.8}Pu_{0.2}O_{1.984} at room temperature as a function of porosity as follows:

$$E = 2.103 \times 10^{11} [1 - 2.03 (1 - D)] \quad (141)$$

where D is the fraction of theoretical density.

3.5.3.4 UO₂-PuO₂ Elastic Modulus as a Function of Both Porosity and Temperature. Equations (140) and (141) were combined to obtain an overall effect of temperature and porosity on Young's modulus for stoichiometric mixed oxides. A value of 2.52 x 10¹¹ Pascals was obtained from the value of 2.265 x 10¹¹ Pascals in Equation (140) after normalizing to theoretically dense UO₂ at 0°C. The resultant expression given by Equation (142) is shown in Figure IV-43 as a function of temperature and porosity and used in FELMOD to calculate Young's modulus for mixed oxides.

$$E = 2.52 \times 10^{11} (1 - 7.843 \times 10^{-4} T) [1 - 2.03 (1 - D)] \quad (142)$$

where D is the fraction of theoretical density and T is the temperature (°C).

Fig. IV-43 Young's modulus as a function of temperature and porosity for $\text{UO}_2\text{-PuO}_2$ mixed oxides as calculated from Equation (140).

As in the case of UO_2 , the effects of irradiation upon the elastic modulus of (U, Pu) O_2 are theoretically expected to be small. There is no data presently available to contradict this expectation and Equation (142) may therefore be used for irradiated as well as unirradiated (U, Pu) O_2 .

3.6 A Fuel Thermal Expansion Model (C. S. Olsen)

Fuel thermal expansion has an important bearing on fuel-cladding gap conductance and on the stresses in the fuel. Literature regarding the thermal expansion of UO_2 and $\text{UO}_2\text{-PuO}_2$ has been reviewed and a model has been developed from the experimental data. This model is now coded under the name FTHEXP in the materials properties code MATPRO^[IV-63] where it is used in the study of fuel rod behavior during steady state and transient situations.

3.6.1 UO_2 Thermal Expansion. Linear thermal expansion data for unirradiated UO_2 has been published by Burdick and Parker^[IV-64], Conway et al^[IV-65,-66], and Christensen^[IV-67] with additional data presented by Belle^[IV-68] that was obtained from Lambertson and Handwerk^[IV-69], Bell and Makin^[IV-70], and Murray and Thackray^[IV-71]. These data are generally consistent although Christensen reported slightly lower values than the other studies. The effect of irradiation on UO_2 thermal expansion is usually assumed to be negligible even though no experimental data are available to support this assumption. Fuel thermal expansion in-pile is uncertain because accurate descriptions of fuel cracking (resulting from thermal stresses) and healing are not available.

The linear thermal expansion model for UO_2 is based on the following correlations:

- (1) The data between 1,000 and 2,250° presented by Conway et al^[IV-65] and the low temperature data of Burdick and Parker^[IV-64], Lambertson and Handwerk^[IV-69], Bell and Makin^[IV-70], and Murray and Thackray^[IV-71] for solid phase, was fit by regression analysis to a third order polynomial. This correlation is given by:

$$\frac{\Delta L}{L_0} = -4.972 \times 10^{-4} + 7.107 \times 10^{-6}T + 2.581 \times 10^{-9}T^2 + 1.140 \times 10^{-13}T^3 \quad (143)$$

where

$$\frac{\Delta L}{L_0} = \text{linear thermal expansion}$$

$$T = \text{temperature (}^\circ\text{C)}.$$

- (2) The thermal expansion data of Christensen^[IV-67] is used for the phase change at the melting point. A uniform-expansion is assumed and the results in the correlation:

$$\frac{\Delta L}{L} = (1 + 0.096)^{1/3} - 1 \quad (144)$$

- (3) The coefficient of expansion presented by Christensen^[IV-67] is used for the liquid phase (above the melting point), resulting in Equation (145):

$$\frac{\Delta L}{L} = 3.5 \times 10^{-5} (T_l - T_m) \quad (145)$$

where

T_m = fuel melting temperature ($^{\circ}\text{C}$).

T_l = fuel temperature above melting ($^{\circ}\text{C}$).

These three correlations along with the data between 0 and $2,500^{\circ}\text{C}$ are shown in Figure IV-44 for UO_2 .

3.6.2 PuO_2 Thermal Expansion. Tokar et al^[IV-72] measured the linear thermal expansion of stoichiometric PuO_2 in the temperature range 25 to $1,420^{\circ}\text{C}$. A polynomial least squares fit by Tokar et al^[IV-72] for the expansion data yielded the expression:

$$\begin{aligned} \Delta L/L_o = & -1.2232 \times 10^{-4} + 7.5866 \times 10^{-6} T + 5.6948 \times 10^{-9} T^2 \\ & -5.9768 \times 10^{-12} T^3 + 4.4092 \times 10^{-15} T^4 - 1.2897 \times 10^{-18} T^5 \end{aligned} \quad (146)$$

where T is the temperature in degrees Centigrade.

This equations is shown graphically in Figure IV-45. Although Equation (146) represents the data well at low temperatures, a fifth order polynomial is not the correct form for extrapolation of the PuO_2 thermal expansion to the melting point.

The PuO_2 thermal expansion data of Tokar et al^[IV-72] and Brett and Russel^[IV-73] were therefore fit to a third order polynomial and an equation for the thermal expansion of the solid phase compatible with the UO_2 expression was determined as follows:

$$\begin{aligned} \Delta L/L_o = & -3.9735 \times 10^{-4} + 8.4955 \times 10^{-6} T + 2.1513 \times 10^{-9} T^2 \\ & + 3.7143 \times 10^{-16} T^3 \end{aligned} \quad (147)$$

where T is again temperature in degrees Centigrade.

3.6.3 Conclusion. Figure IV-46 shows the linear expansion of PuO_2 as a function of temperature as based on the above correlations. The thermal expansion of solid mixed oxides is calculated from the weighted average of the thermal expansion of UO_2 [Equation (143)] and PuO_2 [Equation (147)]. The thermal expansion for the phase change and for the liquid phase of mixed oxides is assumed to be the same as that for UO_2 [Equations (144) and (145)] because of the similarity in structure of UO_2 and $\text{UO}_2\text{-PuO}_2$ mixed oxides.

Fig. IV-44 Comparison of UO_2 thermal expansion data with that calculated from FHTEXP subcode.

Fig. IV-45 Temperature dependent of PuO_2 thermal expansion calculated from Tokar et al equation.

Fig. IV-46 Comparison of PuO_2 thermal expansion data with that calculated from FHTEXP subcode.

4. 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 and/or experimental effort.

The following sections present recent accomplishments and results in the area of code verification. Section 4.1 discusses comparison of SCORE-EVET calculations with LOFT flow distribution data, part of an effort to prepare LOFT Fuel Experiment Requirements. Section 4.2 summarizes ANC's RELAP4-BE verification activities under the program entitled ANC Technical Support to NRC for Industry Cooperative Programs.

4.1 Comparison of SCORE-EVET Calculations with LOFT Flow Distribution Data (K. R. Perkins)

The thermal hydraulic computer code SCORE-EVET is being used to analyze three-dimensional hydrodynamic effects during LOFT loss-of-coolant experiments in conjunction with preparation of a LOFT Fuel Experiment Requirements Document. In order to lend confidence to the analysis, SCORE-EVET calculations have been compared to LOFT velocity distribution data^[IV-74] and previous core calculations presented by Bjornard^[IV-75]. The velocity data are taken under isothermal conditions at typical operating flow rates in a model of a LOFT 15 x 15 fuel rod bundle. These data show extremely nonuniform velocities at the inlet (apparently due to jetting effects behind holes in the core support plate) but at a distance of 24 inches from the inlet, the flow is uniform within $\pm 15\%$ in all but the side channels.

4.1.1 Test Assembly and Measurement Techniques. The test assembly was a full-size model of the 15 x 15 LOFT center fuel module designed and fabricated by Exxon Nuclear Company. The test assembly included tie plates, spacers, and thermocouples which are representative of the current LOFT fuel module design. The test assembly was surrounded by a shroud so the flow distribution in the outer channels will not simulate the LOFT behavior.

The velocity distribution measurements were made with a Laser Doppler Velocimeter in one quadrant of the 15 x 15 array. Since the measurement process takes a considerable amount of time, there was a significant variation ($\pm 4\%$) in the flow rate during the test. The results were adjusted to account for this transient, but it is doubtful whether the measurements can be expected to be better than the variation of $\pm 4\%$ (the repeatability of the measurements is said to be $\pm 3\%$).

4.1.2 SCORE-EVET Model of the Exxon Assembly. The 8 x 8 array used in SCORE-EVET for comparison to the Exxon data, is shown in Figure IV-47. The symmetry planes were chosen so that the calculations are performed for one quadrant of a 15 x 15 rod array surrounded by a shroud. Details of the model are outlined below.

- (1) The subchannels are located between rods as shown in Figure IV-47.
- (2) The presence of the shroud is accounted for as a decrease in the hydraulic diameter of the outer subchannels.

Fig. IV-47 Schematic of the LOFT core model used in SCORE-EVET

- (3) The guide tubes are slightly larger than the fuel rods and decrease the hydraulic diameter of adjacent subchannels (the guide tube diameter is 0.537 in. and the rod diameter is 0.422 in.).
- (4) The thermocouples are modeled as a 0.9% decrease in the cross-sectional flow area.

- (5) The distance from grid spacer to measurement station (2.6 in.) was used as the mesh size with one volume of 3.6 in. to terminate the control volume exactly at each measuring station (the axial velocities calculated by SCORE are exiting velocities).
- (6) The grid spacers were accounted for by loss coefficients ($K = 0.7$ for typical channels, 0.5 for side channels, 0.3 for corner channels, and 0.85 for guide tube channels).
- (7) The distance between rods (pitch) is 0.563 in. while the shroud is 0.421 in. from the centerline of the outer row of rods.

4.1.3 Adiabatic, Three-Dimensional Results. The measured LOFT inlet velocity distribution is given in Table IV-V and is reduced from the Exxon data^[IV-74] taken after the first grid spacer. The downstream velocities calculated with SCORE-EVET using the inlet velocity distribution are listed with the downstream Exxon data in Table IV-VI. Although there are some substantial differences between the SCORE-EVET calculations and the experimental data, the overall agreement (the rms of the difference is about 5%) is reasonable considering the accuracy of the measurements. (The accuracy is reported to be repeatable to within $\pm 3\%$ and the dimension between the outer rod and shroud is questioned by the experimenters.)

TABLE IV-V
INLET VELOCITY DISTRIBUTION^[a]
FOR SCORE-EVET ISOTHERMAL MIXING CALCULATIONS

								Channel Designation
8.83	10.01	10.71	11.47	14.03	14.16	12.80	12.18	8
6.75	12.15	13.68	17.65	20.13	18.02	14.64	11.45	7
6.61	9.77	9.43	13.46	21.19	13.06	9.64	8.78	6
8.97	16.25	15.83	17.31	15.82	17.52	15.41	11.52	5
9.32	15.86	16.62	19.94	15.07	14.13	19.94	16.09	4
9.40	16.72	16.74	15.86	17.20	16.70	20.58	15.58	3
7.21	10.92	12.32	15.35	21.81	21.42	15.66	13.58	2
7.52	13.74	13.78	13.34	16.03	19.96	14.83	12.15	1
8	7	6	5	4	3	2	1	

[a] Velocities in feet per second are reduced from the data of Reference IV-74 at a location 7.85 in. from the lower support plate.

TABLE IV-VI

SCORE-EVET VELOCITY CALCULATIONS COMPARED
TO EXXON DATA^[a] 24.45 IN. ABOVE THE LOWER SUPPORT PLATE

								Channel Designation
13.6	13.6	13.5	13.4	13.8	13.6	14.0	15.2	8
13.7	13.8	14.2	14.6	15.1	14.9	14.4	14.1	
14.5	13.6	14.2	16.2	15.6	14.8	14.4	14.2	7
13.5	13.8	14.3	15.2	15.9	15.1	14.2	13.7	
13.9	14.0	12.1	13.4	15.0	12.8	12.3	13.9	6
13.5	13.6	13.2	14.1	15.9	13.7	13.1	13.2	
14.7	14.2	14.5	14.6	14.6	14.4	14.2	14.2	5
14.1	14.7	14.1	14.5	14.8	14.2	14.2	13.3	
14.5	14.5	15.5	15.9	13.3	13.3	16.7	13.6	4
14.1	14.8	15.0	15.5	13.9	13.9	15.8	14.2	
15.0	14.3	13.2	13.3	14.0	14.9	16.2	14.6	3
14.1	14.7	14.1	14.1	14.5	14.5	15.9	14.8	
13.5	13.3	12.0	13.8	16.4	15.4	13.6	13.4	2
13.5	13.7	13.5	14.4	16.4	16.3	15.0	14.2	
14.0	14.4	14.4	13.1	14.0	15.1	13.8	12.8	1
13.7	14.1	14.1	13.6	14.4	15.9	14.6	13.2	
8	7	6	5	4	3	2	1	

0 - Guide tube location

[a] The calculated values in feet per second are below the Exxon^[IV-74] data at each position. The rms value of the difference between the calculation and data is about 5.0%.

A data comparison has also been made at a further downstream location where the effects of inlet conditions are minimal (Table IV-VII). COBRA calculations are also shown. The agreement of SCORE-EVET calculations with the data is again about 5% which is not appreciably different than the 4% agreement for the COBRA calculations. Surprisingly, the agreement of one prediction with the other does not appear any better than with the data. (This observation may not be as surprising as it first appears since the mixing coefficients in COBRA have been determined empirically from similar rod bundle data, whereas the eddy diffusivity correlations used in SCORE-EVET have a relatively broad semi-analytical foundation.)

TABLE IV-VII

SCORE-EVET VELOCITY CALCULATIONS COMPARED TO
 EXXON DATA AND COBRA^[a] CALCULATIONS 35.85 IN. ABOVE THE
 LOWER SUPPORT PLATE

								Channel Designation
14.3	14.6	14.9	15.3	15.6	15.3	15.2	14.9	8
9.8	14.2	14.1	14.3	13.8	12.9	14.5	14.2	
12.1	13.6	13.8	14.2	14.4	14.2	13.9	13.6	
14.3	14.5	14.8	15.3	15.6	14.2	14.6	14.5	7
14.5	15.2	15.1	15.6	16.1	15.8	15.1	15.5	
13.4	14.5	14.8	15.3	15.8	15.2	14.7	14.4	
14.5	14.3	13.5	13.9	15.5	13.8	13.3	14.1	6
14.3	15.1	14.3	14.2	15.6	14.1	12.9	14.6	
13.4	14.4	13.6	14.1	15.6	13.8	13.4	14.0	
14.9	14.8	13.9	14.1	14.9	13.5	13.6	13.3	5
14.5	15.1	14.9	15.1	14.8	13.9	13.9	13.9	
13.8	15.0	14.0	14.2	15.0	13.9	13.9	13.4	
14.9	15.2	15.2	15.2	13.9	13.9	15.1	13.5	4
14.2	15.1	14.6	15.1	14.1	13.8	15.3	14.2	
13.8	15.1	15.2	15.3	13.9	13.8	15.4	14.0	
14.8	14.9	13.8	13.5	13.8	14.1	15.1	13.3	3
14.2	14.8	14.2	13.6	14.1	14.5	15.2	14.2	
13.7	14.9	13.9	13.9	14.1	14.2	15.6	15.1	
14.5	14.3	13.6	13.6	15.3	15.5	13.8	12.9	2
13.2	14.5	13.9	14.9	15.3	14.9	13.6	14.3	
13.4	14.4	13.6	14.1	15.8	15.9	15.2	14.7	
14.5	14.6	14.3	13.6	13.6	13.9	13.1	12.3	1
12.9	13.9	14.3	13.8	14.5	14.5	14.2	13.6	
13.4	14.6	14.5	13.6	14.2	15.8	15.0	13.3	
8	7	6	5	4	3	2	1	

[a] Velocities are in feet per second. The velocities from COBRA are reduced from the calculations presented in Reference IV-74. The COBRA velocities are above the Exxon^[IV-74] data and the SCORE-EVET velocities are below the data.

4.2 ANC Technical Support to NRC for Industry Cooperative Programs (R. E. Rice)

Success of the cooperative programs requires that the experimental data provided by the reactor vendors in cooperation with the NRC fully meet the needs of other organizations concerned with verification of the codes. To assure that these needs are met in the industry cooperative programs, Aerojet Nuclear Company has been placed in a technical advisory role.

This section summarizes ANC's contributions to date as advisor on the BWR Blowdown Heat Transfer Program being conducted by General Electric in San Jose. These experiments are conducted in the Two Loop Test Apparatus (TLTA) which is a scaled simulator of a Boiling Water Reactor (BWR). The system has been designed to provide blowdown conditions in the bundle as close as possible to a BWR so that investigations can be made of time to critical heat flux (CHF), post-CHF heat transfer, and the effect of lower plenum swell on bundle thermal performance. The TLTA is built around a full-length bundle of 49 electrically heated rods representing the BWR core region. This scaled facility features all major BWR components including two external recirculation loops with a single jet pump in each loop.

The ANC support on the GE program has fallen into five categories: (a) Program plan review, (b) Operations and measurements review, (c) Analytical support, (d) Review of TLTA data, and (e) RELAP4 code evaluation. Significant results and findings of these tasks are as follows.

4.2.1 Program Plan Review. The code verification needs were reviewed for the BWR LOCA. This review has indicated that additional experiments are necessary for verification of thermal-hydraulic codes used for analysis of the emergency core cooling (ECC) period. These experiments should be conducted on a facility which allows interaction between core spray water, bottom reflood water, and the core as affected by flow paths and elevations of the complete system.

It has been proposed that TLTA be modified to allow conduct of these experiments. ANC has recommended that this potential extension to the GE program be assigned a high priority in the future NRC planning.

4.2.2 Operations and Measurements. An analysis of the accuracies of selected TLTA measurements were completed. Findings of this study were that most of the measurements were adequate for purposes of model verification. Exceptions to this are the poor resolutions of all flow rates at low values. This problem, which arises after lower plenum flashing, greatly limits the diagnosis of code problem areas. It was recommended that GE strive to improve the flow measurement resolution.

4.2.3 Analytical Support. ANC has investigated pertinent TLTA areas of interest using the RELAP4-BE computer code. Significant results of these studies included the following:

- (1) Wall heat transfer will be of significance in the TLTA and must be factored into evaluation of the test results.
- (2) The alternate lower plenum geometry proposed for Test 18 should not have a significant effect on TLTA performance.
- (3) Elevation and momentum differences between the TLTA and BWR will result in a nontypical TLTA core flow coastdown. This difference, which will affect core temperature response, must be accounted for in relating TLTA performance to a BWR.

4.2.4 Review of TLTA Data. A review of data from all Bundle 1 tests has been completed. Emphasis has been placed on evaluating the impact of the various Bundle 1 test parameters and in identifying new or unexpected phenomena.

- (1) Variations in bundle power have little effect on system performance, but substantially affect rod temperatures. A threshold for an early (less than 1 second) CHF exists at power levels between 4.55 and 6.05 MW.
- (2) Initial downcomer mass, which was not repeatable from test to test has a significant effect on system performance.
- (3) Test to test repeatability appears to be good for tests run close together in time.
- (4) A test using an alternate power decay curve was inconclusive.
- (5) Changes in break area affect system response and event timing.
- (6) Liquid fall-back from the upper plenum may play an important role in holding rod temperatures down in the postflashing period.

Based on these observations, several recommendations have been made. GE should establish a constraint on allowable initial downcomer masses. The CHF threshold should be pinpointed by additional testing. A new alternate power decay profile test should be run. An additional long-term repeatability test should be scheduled. Additional model development and evaluation work is indicated to account for observed experimental trends in bundle performance.

4.2.5 RELAP4-BE Code Evaluation. Results of three experiments (TLTA Tests 1, 3, and 6) have been compared to RELAP4-BE calculations to assess adequacy of the code and to suggest areas of code improvement.

The overall accuracy of the RELAP4-BE calculation can be expressed in terms of heater rod temperature. A typical rod temperature comparison is shown in Figure IV-48. The model is shown to overpredict peak rod temperature by several hundred degrees. The peak occurs shortly after bundle dryout in the prediction, but is usually observed at the end of blowdown in the experiment. The model is shown to underpredict the temperature of some rods at the end of blowdown. The temperature level at this time is especially important as it influences subsequent bundle behavior during the core spray, reflood, and refill periods of the LOCA.

Fig. IV-48 Typical RELAP4 data comparison; TLTA heater rod temperatures.

There is apparently no primary cause of the data-prediction mismatch on bundle temperature. Rather, this result is believed to follow from an accumulation of relatively small anomalies, each having an important impact on system and bundle performance. Many of these problem areas are readily identified, however, some are masked by complex thermal-hydraulic interactions between components in the TLTA, and will require additional studies to isolate causes and determine corrective actions.

Recommendations have been made for additional RELAP4-BE code development and model verification in the following areas: (a) bundle flow and heat transfer, (b) separation effects in vertically oriented volumes, (c) jet pump performance, (d) horizontal slip, (e) critical flow, and (f) wall heat transfer.

5. REFERENCES

- IV-1. J. E. Welch et al, *The MAC Method: A Computing Technique for Solving Viscous, Incompressible, Transient Fluid Flow Problems Involving Free Surfaces*, Los Alamos Scientific Laboratory Report LA-3425 (1966).
- IV-2. A. A. Amsden *The Particle-in-Cell Method for the Calculation of the Dynamics of Compressible Fluids*, Los Alamos Scientific Laboratory Report LA-3466 (1966).
- IV-3. F. H. Harlow and A. A. Amsden, *J. Computational Phys.* 17, 19 (1975).
- IV-4. F. H. Harlow and A. A. Amsden, *J. Computational Phys.* 16, 1 (1974).
- IV-5. C. W. Hirt and J. L. Cook, *J. Computational Phys.* 10, 324 (1972).
- IV-6. F. H. Harlow and A. A. Amsden *J. Computational Phys.* 8, 197 (1971).
- IV-7. C. W. Hirt, *J. Computational Phys.* 2, 339 (1968).
- IV-8. J. A. Viecegli, *J. Computational Phys.* 8, 119 (1971).
- IV-9. C. A. Mayer et al, "Thermodynamic and Transport Properties of Steam", *American Society of Mechanical Engineers* (1967).
- IV-10. E. Schmidt, "Properties of Water and Steam in SI Units", Springer-Verlag, New York (1969).
- IV-11. A. N. Kolmogorov, "Equations of Turbulence Motion of an Incompressible Turbulent Fluid", *Izv. Akad. Nauk. SSSR Ser Phys.* VI, No. 1-2 (1942) p 56.
- IV-12. L. Prandtl, "Über ein neues Formelsystem für die ausgebildete Turbulenz", *Nachrichten von der Akad. der Wissenschaft in Göttingen* (1945).
- IV-13. J. Laufer, *Investigation of Turbulent Flow, Two-Dimensional Channel*, NACA-TR-1053 (1951).
- IV-14. K. Laufer, "Thoughts on Compressible Turbulent Boundary Layers", NASA SP-216, Sym. on Compressible Turbulent Boundary Layers, *Langley Research Center*, Hampton, Virginia (1968) pp.1-13.
- IV-15. J. O. Hinze, *Turbulence: An Introduction to its Mechanism and Theory*, McGraw-Hill Book Company (1959).

- IV-16. D. C. Wilcox and I. E. Alber, "A Turbulence Model for High Speed Flows", *Proceedings of the 1972 Heat Transfer and Fluid Mechanics Institute*, Stanford University Press, (1972) p 231-252.
- IV-17. B. E. Launder and D. B. Spalding, *Mathematical Models of Turbulence*, Academic Press, New York (1972).
- IV-18. A. Tavre (ed.), *The Mechanics of Turbulence*, New York: Gordon and Breach (1964).
- IV-19. P. Bradshaw and D. H. Terriss, "Calculation of Boundary-Layer Development Using the Turbulent Energy Equation: Compressible Flow on Adiabatic Walls", *J. Fluid Mech.* 46, (1971) pp 83-110.
- IV-20. W. P. Jones and B. E. Launder, *Int. J. Heat Mass Transfer*, Vol. 15, (1972) pp 301-314.
- IV-21. K. Hanjalic, *Two-Dimensional Asymmetric Turbulent Flow in Ducts*, Ph.D. Thesis, Imperial College, University of London (1970).
- IV-22. F. H. Harlow and C. W. Hirt, LASL Report LA-4086 (1969).
- IV-23. F. H. Harlow and C. W. Hirt, LASL Report LA-3854 (1968).
- IV-24. P. G. Saffman, "A Model for Inhomogeneous Turbulent Flow", *Proc. Roy. Soc., Lond. A.* 317 (1970) pp 417-433.
- IV-25. H. Schlichting, *Boundary Layer Theory*, McGraw-Hill, 4th edition (1960).
- IV-26. J. G. Knudsen and D. L. Katz, *Fluid Dynamics and Heat Transfer*, McGraw-Hill (1958).
- IV-27. J. Nikuradse, *VDI-Forschungsheft* 289, (1929).
- IV-28. A. A. Amsden and F. H. Harlow, *Kachina: An Eulerian Computer Program for Multifield Fluid Flows*, Los Alamos Scientific Laboratory Report LA-5680 (December 1974).
- IV-29. N. Zuber, "Flow Excursions and Oscillations in Boiling Two-Phase Flow Systems with Heat Addition", in *Symposium on Two Phase Flow Dynamics*, Euratom, Brussels, EUR-4288e (1969) pp 1,071-1,089.
- IV-30. N. Zuber and F. W. Staub, "An Analytical Investigation of the Transient Response of the Volumetric Concentration in a Boiling Forced-Flow System", *Nucl. Sci. and Eng.*, 30, (1967) pp 268-278.

- IV-31. J. A. Boure, A. E. Bergles, L. S. Tong, "Review of Two-Phase Flow Instability", *Nuclear Engineering and Design*, 25, (1973) pp 165-192.
- IV-32. Wen-ching Yang, *Ind. Eng. Chem. Fund.*, 12, (1973) pp 349-352.
- IV-33. M. Sevik and S. H. Park, "The Splitting of Drops and Bubbles by Turbulent Fluid Flow", *J. Fluids Eng.*, 95, (1973) p 53.
- IV-34. J. Berghmans, "Stability of Gas Bubbles Rising in Inviscid Fluids", *Chem. Eng. Sci.*, 28, (1973) pp 2,005-2,011.
- IV-35. N. Zuber and J. A. Findlay, "Average Volumetric Concentration in Two-Phase Flow Systems", *J. Heat Transfer*, 87, (1965) pp 453-468.
- IV-36. N. Zuber, F. W. Staub, G. Bijivaard, P. G. Kroeger, *Steady State and Transient Void Fraction in Two-Phase Flow Systems - Final Report Vol. 1*, General Electric Report GEAP-5417 (January 1967).
- IV-37. W. Wulff, *Development of a Computer Code for Thermal Hydraulics of Reactor (THOR)*, First Quarterly Report, Brookhaven National Laboratory, BNL-19978, RP-1038 (January 1975).
- IV-38. L. Noordzij and L. Van Wijngaarden, "Relaxation Effects, Caused by Relative Motion, or Shock Waves in Gas Bubble/Liquid Mixtures", *J. Fluid Mech.*, 66, Pt. 1 (1974) pp 115-143.
- IV-39. E. Rolstad, A. Hanevick, K. K. Knudsen, "Measurement of the Length Changes of UO_2 Fuel Pellets During Irradiation", *Presented at the England HPG Meeting on Computer Control and Fuel Research* (June 4-7, 1974).
- IV-40. J. Crank and P. Nicolson, "A Practical Method for Numerical Evaluation of Solutions of Partial Differential Equations of the Heat Conduction Type", *Proceedings, Cambridge Philosophical Society*, 43, (1947) pp 50-64.
- IV-41. F. Kreith, *Principles of Heat Transfer*, 8th edition, International Text Book Company, Scranton, Pennsylvania (1964).
- IV-42. W. H. McAdams, *Heat Transmission*, 3rd edition, McGraw-Hill Book Company, Inc., New York (1954).
- IV-43. J. A. Dearien and L. J. Siefken, "FRAP-T: A Computer Code for the Transient Analysis of Oxide Fuel Rods - Vol 1 - ANALYTICAL MODELS AND INPUT MANUAL", I-243-3-51.1, Aerojet Nuclear Company (July 1974).

- IV-44. R. L. Miller, R. R. Hobbins, V. F. Baston, W. A. Yuill, "FRAP-T - A Computer Code for the Transient Analysis of Oxide Fuel Rods - Volume 2 - MATPRO Materials Properties Subcode", I-243-2, Aerojet Nuclear Company (January 15, 1975).
- IV-45. J. S. Prezemieniecki, *Theory of Matrix Structural Analysis*, McGraw-Hill Book Company, New York (1968).
- IV-46. S. Gill, "A Process for the Step-by-Step Integration of Differential Equations in an Automatic Digital Computing Machine", *Proceedings Cambridge Philosophical Society*, 47, (1951) pp 96-108.
- IV-47. M. O. Marlowe and A. I. Kaznoff, "Elastic Behavior of Uranium Dioxide", *J. Amer. Cer. Soc.* 50 (1970) pp 90-99.
- IV-48. A. R. Hall, "Elastic Moduli and Internal Friction of Some Uranium Ceramics", *J. Nucl. Mat.* 37 (1970) pp 314-323. Also AERE-R-5650 Atomic Energy Research Establishment (1967).
- IV-49. A. Padel and C. de Novion, "Constants Elastiques des Carbures, Nitrures et Oxydes d'Uranium et de Plutonium", *J. Nucl. Mat.* 33 (1969) pp 40-51.
- IV-50. R. J. Forlano et al, "Elasticity and Anelasticity of Uranium Oxides at Room Temperature: I Stoichiometric Oxide", *J. Amer. Cer. Soc.* 50 (1967) pp 92-96.
- IV-51. R. Scott et al, "The Plastic Deformation of Uranium Oxides above 800°C", *J. Nucl. Mat. Vol 1* (1959) pp 39-48.
- IV-52. J. B. Wachtman et al, *Factors Controlling Resistance to Deformation and Mechanical Failure in Polycrystalline (Glass-Free) Ceramics. Period Covered July 1, 1975*, WADC-TR-59-278 (1959).
- IV-53. J. Belle and B. Lustman, "Properties of Uranium Dioxide", in *Fuel Elements Conference*, Paris, France (November 18-23, 1974; March 1958).
- IV-54. W. A. Lambertson and J. H. Handwerk, *The Fabrication and Physical Properties of Urania Bodies*, ANL-5053 Argonne National Laboratory (February 1956).
- IV-55. J. T. A. Roberts and Y. Ueda, "Influence of Porosity and Deformation and Fracture of UO_2 ", *J. Amer. Cer. Soc.* 55 (1972) pp 117-124.
- IV-56. N. Igata and K. Domoto, "Fracture Stress and Elastic Modulus of Uranium Dioxide Including Excess Oxygen", *J. Nucl. Mat.* 45 (1972/73) pp 317-322.
- IV-57. M. O. Marlowe and D. R. Wilder, "Elasticity and Internal Friction of Polycrystalline Yttrium Oxide", *J. Amer. Ceram. Soc.* 48 (1965) pp 227-233.

- IV-58. F. Gatto, "Influence of Small Cavities on Velocity of Sound in Metals", *Alluminio* 19 (1) (1950) pp 19-26.
- IV-59. J. B. Wachtman et al, "Elastic Constants of Single Crystal UO_2 at 25°C ", *J. Nucl. Mat.* 16 (1965) pp 39-41.
- IV-60. R. Hill, "The Elastic Behavior of a Crystalline Aggregate", *Proc. Phys. Soc. Lond* A65 (1952) pp 349-354.
- IV-61. J. B. Wachtman et al, "Exponential Temperature Dependence of Young's Modulus for Several Oxides", *Phys. Rev.* 122 (6) (1961) pp 1,754-1,759.
- IV-62. A. W. Nutt, Jr., A. W. Allen, J. H. Handwerk, "Elastic and Anelastic Response of Polycrystalline $\text{UO}_2\text{-PuO}_2$ ", *J. Amer. Cer. Soc.* 53 (1970) p 205.
- IV-63. R. L. Miller et al, "FRAP-T: A Computer Code for the Transient Analysis of Oxide Fuel Rods, Vol 2 - MATPRO Materials Properties Subcode", Aerojet Nuclear Company, I-243-2 (January 15, 1975).
- IV-64. M. D. Burdick and H. S. Parker, "Effect of Particle Size on Bulk Density and Strength Properties of Uranium Dioxide Specimens", *J. Amer. Cer. Soc.* 39 (1956) p 181.
- IV-65. J. B. Conway, R. M. Fincel, Jr., R. A. Hein, "The Thermal Expansion and Heat Capacity of UO_2 to $2,200^\circ\text{C}$ ", *Trans. Amer. Nuc. Soc.* 6 (1) (June 1963).
- IV-66. J. B. Conway, R. M. Fincel, Jr., R. A. Hein, *The Thermal Expansion and Heat Capacity of UO_2 to $2,200^\circ\text{C}$* , TM-63-6-6, General Electric Company (1963).
- IV-67. J. A. Christensen, "Thermal Expansion and Change in Volume of Uranium Dioxide on Melting", *J. Amer. Cer. Soc.* 46 (12) (1963) p 607.
- IV-68. J. Belle (ed.), *Uranium Dioxide: Properties and Nuclear Applications*, USAEC (1961).
- IV-69. W. A. Lambertson and J. H. Handwerk, *The Fabrication and Physical Properties of Urania Bodies*, ANL-5053, Argonne National Laboratory (1956).
- IV-70. I. P. Bell and S. M. Makin, "Fast Reactor - Physical Properties of Materials of Construction", *Review of Progress from September 1, 1953 to April 1, 1954*, RDB(C)/TN-70 (1954).
- IV-71. P. Murray and R. W. Thackray, *The Thermal Expansion of Sintered UO_2* , HERE M/M 22 (no date).

- IV-72. M. Tokar, A. W. Nutt, T. K. Keenan, "Linear Thermal Expansion of Plutonium Dioxide", *Nucl. Tech. Vol 17* (February 1973) p 147.
- IV-73. N. H. Brett and L. E. Russell, "The Thermal Expansion of PuO_2 and Some Other Actinide Oxides Between Room Temperature and $1,000^\circ\text{C}$ ", in *Plutonium* (1960). E. Grison, W. B. H. Lord, R. D. Fowler (eds.), *Proc. 2nd International Conf. Plutonium Metallurgy*, Grenoble, France, April 19-22, 1960, p 397.
- IV-74. *Experimental Studies of Flow Distribution in a LOFT Reactor Fuel Assembly*, XN-74-3, Exxon Nuclear Company (February 1974).
- IV-75. T. A. Bjornard, *Analysis of the LOFT Instrumented Fuel Assembly Flow Distribution Experiments*, XN-74-53, Exxon Nuclear Company (October 1974).

DISTRIBUTION RECORD FOR ANCR-1262

External

- 245 - NRC-1, Basic Distribution
- 13 - NRC-2, Water Reactor Safety Research Systems Engineering
- 40 - NRC-3, Water Reactor Safety Research Fuel Behavior
- 52 - NRC-4, Water Reactor Safety Research Analysis Development
- 2 - H. J. C. Kouts, NRC

Internal

- 1 - Chicago Patent Group - ERDA
9800 South Cass Avenue
Argonne, Illinois 60439
- 3 - A. T. Morphey, Classification and Technical Information Officer
ERDA-ID
Idaho Falls, Idaho 83401
- 1 - R. J. Beers, ID
- 1 - P. E. Litteneker, ID
- 1 - R. E. Swanson, ID
- 1 - V. A. Walker, ID
- 1 - R. E. Wood, ID
- 31 - INEL Technical Library
- 20 - Author

Total Copies Printed - 412