**Quarterly Progress Report to
Nuclear Regulatory Commission
Division of Reactor Safety Research
Fuel Behavior Research Branch
Programs for Period Ending
March 1977**

April 1977

**Prepared for the
Nuclear Regulatory Commission**

NOTICE

This report was prepared as an account of work sponsored by the United States Government. Neither the United States nor the United States Nuclear Regulatory Commission, 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.

PACIFIC NORTHWEST LABORATORY
operated by
BATTELLE
for the
ENERGY RESEARCH AND DEVELOPMENT ADMINISTRATION
Under Contract EY-76-C-06-1830

Printed in the United States of America
Available from
National Technical Information Service
U.S. Department of Commerce
5285 Port Royal Road
Springfield, Virginia 22151
Price: Printed Copy \$____*; Microfiche \$3.00

*Pages	NTIS Selling Price
001-025	\$4.50
026-050	\$5.00
051-075	\$5.50
076-100	\$6.00
101-125	\$6.50
126-150	\$7.00
151-175	\$7.75
176-200	\$8.50
201-225	\$8.75
226-250	\$9.00
251-275	\$10.00
276-300	\$10.25

QUARTERLY PROGRESS REPORT
TO
NUCLEAR REGULATORY COMMISSION
DIVISION OF REACTOR SAFETY RESEARCH
FUEL BEHAVIOR RESEARCH BRANCH PROGRAMS

for

PERIOD ENDING MARCH 1977

Compiled and Edited by
W. J. Bailey

APRIL 1977

BATTELLE
Pacific Northwest Laboratories
Richland, WA 99352

LIST OF CONTRIBUTORS

C. R. HANN, PROJECT MANAGER:

EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM - TASK A

C. R. Hann
R. K. Marshall
R. E. Williford
D. D. Lanning
S. R. Wagoner
E. R. Bradley
C. E. Beyer
J. A. Christensen
D. W. Brite

EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM - TASK B

J. E. Garnier
J. L. Bates
S. Begej
D. W. Brite

FUEL SUBASSEMBLY PROCUREMENT AND IRRADIATION TEST PROGRAM

E. L. Courtright
R. K. Marshall
C. R. Hann
L. L. King
L. J. Parchen

PRESSURE TRANSDUCER DEVELOPMENT PROGRAM

E. L. Courtright
R. K. Marshall
L. L. King
C. R. Hann

SUMMARY

Three programs at Battelle, Pacific Northwest Laboratories are sponsored by the Fuel Behavior Research Branch of the Division of Reactor Safety Research (RSR) of the Nuclear Regulatory Commission (NRC). Progress on the three programs during the period up through March 1977 is summarized below.

EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM

The aim of this program is to obtain well-characterized data for verification of steady-state fuel codes and for development of improved models for stored energy calculations. The program includes in-reactor and ex-reactor tests which are designed to focus on the major uncertainties affecting nuclear fuel licensing in the U.S. Two, instrumented, six-rod fuel assemblies (IFA-431 and IFA-432) were designed in 1974 for irradiation testing in the Halden Boiling Water Reactor (HBWR). The ex-reactor tests involve extending the contact conductance work of Ross and Stoute to more relevant gas pressures, temperatures, and gas compositions and conducting experiments under conditions where no data are currently available.

In July 1976, a draft of a report containing detailed information on IFA-431 and IFA-432 was prepared. The report, BNWL-1988,⁽¹⁾ is to be a reference document for future analytical and experimental work conducted under this program as well as other Reactor Safety Research and industry safety programs. The test rationale, design, fabrication, and precharacterization data are included in extensive detail to ensure all pertinent information on the assemblies will be available for future mathematical modeling and data reduction. The information is necessary to quantify the uncertainty in thermal stored energy and thus reduce restrictions on commercial power plant operation.

Papers^(2,3) presented in March and September 1976 included preliminary evaluations of data from IFA-431. The evaluations were extremely encouraging and could have a significant, favorable impact on fuel licensing in the U.S. Most important was the behavior of very unstable fuel that should, as characterized by preirradiation measurements, have a propensity to undergo considerable densification. The effects of fuel densification on fuel temperatures

and gap conductance were quite benign. Comparisons of computer code predictions with the data indicated good simulations of startup conditions and that the NRC densification model is conservative. Controlled power ramps yielded valuable insight into transient gap conductances. The effects of fuel eccentricity were also shown to be significant in low conductance rods. Preliminary results of uncertainty analyses that describe the reliability of the results were also presented. Conclusions drawn from these preliminary evaluations of IFA-431 data were:

- IFA-431 operated within design limits and with good instrumentation.
- Dynamic temperature measurements indicate transient gap conductance calculations are tractable and can be related to steady-state values.
- Gap conductances derived from IFA-431 data are somewhat higher than code predictions.
- Fuel temperatures in unstable fuel are several hundred degrees below predicted values. The results should have a significant, beneficial impact on fuel licensing.
- Xenon has a very strong effect on fuel temperatures.
- Fuel column eccentricity may have a significant effect in xenon-filled gaps.
- Extensive fuel rod component precharacterization is required to reduce uncertainties in both the experimental data and code comparisons.
- Correction factors are required for power distribution in the assembly before adequate gap conductance calculation can be made or before rod-to-rod comparisons can be completed.
- The GAPCON-THERMAL-2⁽⁴⁾ code predicted startup data quite well.
- The NRC densification model is conservative relative to the results from these tests.
- Relocation models used in the GAPCON-THERMAL-2⁽⁴⁾ code need revision.
- Uncertainty analyses are necessary to describe reliability of results.

- Uncertainties in the variables must often be estimated with engineering judgment.
- Proper methods must be used for propagation of uncertainty into the results.
- Additional work is required to quantify the uncertainties in gap conductance values derived from this experiment because all of the uncertainties in the test eventually accumulate in the gap conductance.

A paper, BNWL-SA-6161,⁽⁵⁾ was completed in February 1977 that describes the method which was developed to estimate the reliability of gap conductances deduced from measured fuel centerline temperatures of NRC-RSR/BPNL fuel rods irradiated in the HBWR. In arriving at reliability estimates for experimental data, the ideal situation is to repeat all measurements of the input parameters required in the calculations enough times using enough observers and enough diverse instruments so that the reliability of the results can be assured by the use of statistics. Unfortunately, it is not practical to estimate all of the uncertainties of the input parameters by repetition. Since statistics could not be applied to the errors, the uncertainty intervals selected are based on past experience and engineering judgment. The uncertainty, which is the maximum deviation expected, is interpreted to be equivalent to $\pm 3\sigma$ on a Gaussian distribution and hence includes 99.75% of the distribution.

The $\int k(t)dt$ method⁽⁶⁾ is used in BNWL-SA-6161 to calculate the fuel surface temperature from the fuel centerline temperature and the fuel thermal conductivity. The gap conductances for the fuel rods in this program were deduced from measured centerline fuel temperatures so the fuel surface temperatures could be calculated. The gap conductance is calculated from the fuel surface temperature, the calculated cladding inside surface temperature, and the measured fuel assembly power. The uncertainties in the input parameters for calculating the gap conductance were established and the uncertainty in the gap conductance was calculated using the method of propagation of uncertainties with a first order Taylor series approximation to the nonlinear functions. An example of the calculational method is included in BNWL-SA-6161.

In April-March 1977, a report⁽⁷⁾ and a paper⁽⁸⁾ were prepared that describe activities in this program on the verification of fuel centerline thermocouple readings through response to linear power decreases. As the report and paper point out, an independent estimate of the fuel centerline temperature can be obtained from the thermocouple response to a linear power change. This estimate is independent of the absolute initial values of temperature or power. It is simultaneously independent of any prior knowledge of gap conductance or fuel thermal conductivity. Thus it provides a truly independent cross-check on the thermocouple reading. This independent cross-check is, however, subject to possible bias from at least two sources:

- (1) The assumption that the fuel rod responds as a linear system, with a constant transfer function determined by the effective values of gap conductance, fuel thermal conductivity, etc., applicable to the beginning of the transient.
- (2) A bias in the estimate of the power slope.

The method is so straightforward, and leads to such a simple error analysis, as to warrant further experimental work to reduce the bias from source (2) and quantify the bias from source (1).

Part of the IFA-431 irradiation test involved power decreases from approximately 328 W/cm (10 kW/ft) to 262 W/cm (8 kW/ft) [peak power] in 20-30 sec. During these transients, output was recorded from all 12 fuel thermocouples and from a cobalt neutron detector centered in the assembly. A special data-logging system was used to do this, and a full set of data was recorded once every 3 sec. Several of the power decreases were found to be sufficiently linear to permit straightforward analysis of the time-dependent fuel centerline thermocouple response. This response can be used to predict the true value of the initial fuel centerline temperature, if it is assumed that the power decrease is truly linear and the transfer function between rod power and centerline temperature is independent of the power change. The bias introduced by these assumptions is not insignificant, but is smaller than the scatter in the present data, and is predictable for each

case. When the estimated bias is taken into account, we find the fuel thermocouple readings to be in agreement with their predicted values within 75°C.

The report, BNWL-2189,⁽⁷⁾ is soon to be published and provides a more complete exposition of the IFA-431 data and its error analysis than shown in the paper.⁽⁸⁾ BNWL-2189 also points out the following kinds of experimental work that could be done to improve the method. There are several ways the results of this method could be improved or extended in future experiments, two of which are:

1) Narrowing the Confidence Limits on Fuel Centerline Temperature:

From the experience so far it is apparent that the overall rate of 0.67%/sec is nearly ideal for a power decrease; steeper rates produce too few data points in the linear region, and shallower rates produce power histories that are decidedly nonlinear. It is also apparent that increasing the number of data points taken per run will not decrease the residual standard deviation, which determines the confidence limits. Thus the only avenue open for narrowing the confidence limits is to make the power decreases more truly linear, which means altering the method by which they are achieved. This could probably be done in the helium-3 rig in the Halden reactor. It could also be done by surrounding the assembly by a rotatable shroud (similar to the shroud on IFA-429). The shroud could be tapered or graded in composition around its circumference, such that as it rotates it casts a linearly increasing shadow to neutron radiation across a rod site. Juxtaposition of a cobalt detector near the rod site would adequately determine the variation of the flux at the rod site since it is the slope of the power that is important, not its instantaneous absolute value.

2) Deriving Estimates of the Gap Conductance Values:

The data in BNWL-2189 indicate that the limiting value of the power-to-centerline-temperature transfer function does not vary typically more than ~5% over a power range of 20% and its variation is typically less than 3% over a power range of 10%. This is not a new or startling result. But it does point to a way to independently estimate the gap conductance. If a rapid but accurately calibrated change in power

were made (say 10% in less than 5 sec) then the centerline temperature behavior about 20 sec after that change would be exponential with time constant directly related to the ratio of the fuel-to-coolant conductance divided by the fuel conductivity. This independent check on both conductivity and conductance could be achieved by the same rotatable shroud described above, by simply making a rapid twist. Again, the fact that the neutron detector recording the power change is not exactly at the rod site will not affect the results, since it is the ultimate relative value of the power change that is important, not the instantaneous absolute value of the power. Note that the shroud could be graded axially rather than circumferentially, and lowered past a thermocouple elevation at constant speed to produce linear and rapid power variations.

A paper⁽⁹⁾ was presented in March 1977 that covered two aspects of the IFA-431 experiment: the effects of fill gas composition and pellet eccentricity on fuel rod gap conductances. Fuel Rod 4 of IFA-431 was backfilled with xenon at 1 atm at room temperature during fabrication. The other five fuel rods in IFA-431 were backfilled with helium under the same conditions. Instrumented portions of the fuel column were mechanically constrained with oversize pellets and molybdenum rods to ensure a concentric geometry in the upper portion of the rod and an eccentric geometry in the lower portion. Solutions for the hot gap size were found using the GAPCON-3⁽¹⁰⁾ and TRUMP⁽¹¹⁾ codes. Extensive pellet-cladding interaction (PCI) during startup reduced the radial hot gap in Rod 4 to a value comparable to that of Rod 3, which was helium-filled. The following conclusions can be drawn from this study:

- An order-of-magnitude difference between the gap conductances of the xenon- and helium-filled rods was found, with respective uncertainties of $\pm 11\%$ and $\pm 50\%$.
- Quantification of hot gap size is necessary to assess the effects of fill gas composition and eccentricity on gap conductance.
- The helium-filled concentric versus xenon-filled concentric cases show that the ratio of their respective gap conductances

is proportional to the ratio of the thermal conductivities of the associated gases, indicating that the gas properties presently used in the GAPCON-THERMAL codes are correct.

- Eccentricity in the xenon-filled rod increases the gap conductance by 25 to 50% at a linear heat rating of 200 W/cm when radial hot gap sizes are less than 0.001 in. (0.025 mm). This is thermally equivalent to a helium-filled rod with a radial hot gap of approximately 0.305 mm (0.012 in.).

A preliminary report⁽¹²⁾ has been completed that describes fuel rod elongation measurements at low burnup from IFA-431 and IFA-432. The two instrumented assemblies, irradiated in different channels in HBWR, each contain six fuel rods all loaded with flat-ended 10% enriched UO₂ pellets. The two assemblies are essentially identical in design; IFA-431 operated at a linear heat rating (LHR) of 328 W/cm (10 kW/ft) and IFA-432 is operating at 492 W/cm (15 kW/ft). IFA-431 began operation in June 1975 and was discharged in February 1976 after reaching an average burnup of 372 GJ/kgM (4300 MWd/MTM)* [goal burnup was 345 GJ/kgM (4000 MWd/MTM)]. IFA-432 was charged into HBWR in December 1975 and will operate until late in 1977; goal burnup is 1730 GJ/kgM (20,000 MWd/MTM) for three rods and is less for the other three rods which can be removed and replaced.

The cladding elongation behavior over the 0-173 GJ/kgM (2000 MWd/MTM) burnup range of five rods in each assembly, all of which were backfilled with helium at 1-atm pressure, is described in that report.⁽¹²⁾ A sixth rod in each assembly which was designed to study the effect of xenon fill gas and gap eccentricity on irradiation behavior is discussed in two papers.^(9,13) Parameters in the tests include pellet-cladding gap size and eccentricity, fill gas composition, fuel density and stability, linear heat rating, and burnup.

All of the five helium filled rods in IFA-432 and two in IFA-431 showed pellet-cladding interaction (PCI). One rod in each assembly was designed with a small gap so that strong PCI would occur to minimize the temperature drop across the gap. This rod acted as an internal power standard. With

* kgM = kilogram of heavy metal, MTM = metric ton of heavy metal.

the exception of this rod, three rods during the initial ramp to power and one during a later ramp showed PCI in IFA-432. In IFA-431, with the exception of the internal power standard, only one rod showed PCI and this occurred during the initial ramp to power. Thus, except for the small gap rods, three times as many rods in IFA-432 experienced PCI during the first ramp to power as did those in IFA-431.

This difference in PCI behavior cannot be attributed to differences in assembly design, linear heat ratings, or to different locations in the reactor. The most likely cause is the initial ramp rate which in IFA-432 was three times that in IFA-431. It is postulated that the greater the ramp rate and thus the greater the pellet thermal strain rate, the greater the likelihood of PCI.

On the first ramp to power, the three rods in IFA-432 which were designed to investigate the effect of fuel density and stability showed PCI. All of these rods had a cold diametral gap width of 0.229 mm (9 mils) which is typical of current BWR design. The loading in these rods consisted of 95% TD stable and 92% TD stable and unstable fuels. The initial cladding elongations were inversely related to the fuel type thermal conductivities. As the irradiation progressed from 0 to 173 GJ/kgM (0 to 2000 MWd/MTM) burnup, the cladding elongations of these rods decreased; however, the rod with the unstable fuel decreased less than the rods with stable fuel. Of the same type of rods in IFA-431, only the rod with the lowest thermal conductivity fuel showed PCI and this occurred during the first ramp to power.

One rod in each assembly had a large diametral gap (0.381 mm or 15 mils) to simulate instantaneous fuel densification. In IFA-432, this rod did not show PCI during the first ramp to power but did so at low burnup, whereas the small gap rod, which showed strong PCI on the first ramp to power, at low burnup showed no cladding elongation beyond that of free axial thermal expansion. No PCI was observed in the large gap rod in IFA-431.

The conclusions derived from the fuel rod elongation study associated with these experiments that are relevant to the fuel rods operating under the same conditions are:

- Power ramp rates strongly influence the initiation of PCI. Power ramps induce thermal strain rates in fuel pellets which cause radial circumferential pellet cracking. The pellet fragments can relocate into the pellet-cladding gap during the power ramp and cause PCI. The higher ramp rates in IFA-432 compared to those in IFA-431 caused more extensive pellet cracking and fragment relocation and a greater reduction in the effective thermal conductivity of the fuel pellets which resulted in significantly more PCI in IFA-432 than in IFA-431.
- The magnitude of cladding elongation is inversely related to fuel thermal conductivity. Fuel rods containing fuel pellets with higher thermal conductivities elongate less than those containing fuel pellets with lower thermal conductivities.
- The magnitude of cladding elongation decreases at a slower rate in fuel rods containing unstable fuel than in rods containing stable fuels.
- Small gap rods are more susceptible to PCI than large gap rods.
- Cladding elongations resulting from PCI in small and large gap rods both decreased to low values by the time a burnup of 173 GJ/kgM (2000 MWd/MTM) was reached.
- Increased pellet-cladding contact force in the rod with the small diametral gap (0.076 mm or 3 mils), evidenced by an abrupt increase in cladding up-ramp slope, produced an increase in fuel centerline temperature. Apparently, a decrease in effective pellet thermal conductivity was more effective in influencing the fuel centerline temperature than the increase in gap conductance caused by the increase in pellet-cladding contact pressure.
- The boiling power, which is changed over a wide range during assembly calibration, causes a change in cladding surface heat transfer coefficient and a less nonuniform radial temperature distribution in the channel. This causes a change in the output of the elongation detectors but does not cause PCI.

The objective of the ex-reactor testing program is to determine the gap contact conductance of UO_2 and Zircaloy interfaces as a function of the following seven variables: temperature, gas pressure, gas composition, interfacial contact pressure, gap separation, surface characteristics (roughness, wavelength, and oxidation), and time.

Although the principles which govern heat transfer across the fuel-cladding interface are fairly well understood, the process within an actual fuel element is complex and difficult to predict. The degree of thermal resistance offered by the interface is a function of the actual contact area, the thermal and physical properties of the mating materials, and the interstitial fluid. The contact area, in turn, is strongly dependent upon the applied load and the mechanical properties of the mating materials. The heat transfer can occur by fluid convection, conduction and radiation. These modes are interdependent and difficult to predict precisely because of their dependence upon a number of variables which are not easily defined in a fuel element. For example, solid-solid conduction is strongly dependent upon the number and size of surface contacts. Eccentricity or nonuniform surface contact adds a very unpredictable dimension. Ex-reactor experiments allow a well-defined and controlled study of those variables which strongly influence the heat transfer across an interface.

Two experimental techniques have been developed for measuring thermal interface conductance. These are the MPD (modified pulse design: a non-steady-state, time-dependent technique employing a laser heat source and a temperature detection system) and the MLD (modified longitudinal technique: a variation of the classical steady-state column apparatus whereby the sample is self-guarding).

Measurements are being made using the MPD technique. The results from the MPD measurements will be compared from a limited number of similar experiments employing the MLD technique. The information obtained will be coupled with in-reactor testing and be used to further refine the GAPCON-THERMAL and FRAP computer programs for oxide fuel elements.

FUEL SUBASSEMBLY PROCUREMENT AND IRRADIATION TEST PROGRAM

A conceptual design for the 4X test train has been proposed and the key features identified. Arrangements have been made to ship irradiated test rods from the IFA 431 Test Series in Halden to INEL for subsequent testing in PBF under selected transient conditions. New fuel for a proposed dual purpose test assembly involving testing in Halden and follow up transient tests in PBF has been fabricated. No progress in the design of 16-rod LOCA test assembly is reported for this period.

PRESSURE TRANSDUCER DEVELOPMENT PROGRAM

A review of pressure transducer concepts suitable for dynamic measurement of fuel rod pressure under reactor operating conditions was conducted and an LVDT-bellows concept selected for further study and development. Only one commercial supplier has indicated an interest in building such a device and problems with obtaining prototypes for further evaluation on a timely basis have been experienced.

CONVERSION TABLE FOR U.S. CUSTOMARY TO SI UNITS^{(a)(b)}

	To convert from	to	Use this equation
Temperature	degree Fahrenheit (°F)	degree Celsius (°C)	$^{\circ}\text{C} = (^{\circ}\text{F} - 32)/1.8$
	degree Fahrenheit (°F)	kelvin (K)	$\text{K} = (^{\circ}\text{F} + 459.67)/1.8$
	degree Celsius (°C)	kelvin (K)	$\text{K} = ^{\circ}\text{C} + 273.15$
	To convert from	to	Multiply by
Time	hour (hr)	second (s)	3.600×10^3
	day (d or day)	second (s)	8.640×10^4
Length	inch (in.)	meter (m)	2.540×10^{-2}
Area	inch ² (in. ²)	meter ² (m ²)	6.452×10^{-4}
Volume	inch ³ (in. ³)	meter ³ (m ³)	1.639×10^{-5}
Pound-Mass	lbm	kilogram (kg)	4.536×10^{-1}
Pound-Force	lbf	newton (N)	4.448
Pressure or Stress	psi (lbf/in. ²)	pascal (Pa)	6.895×10^3
Energy	British thermal unit (Btu)	joule (J)	1.055×10^3
	calorie (cal)	joule (J)	4.187
	watt-second (W-s)	joule (J)	1.000
Linear Heat Rating	kilowatts/ft (kW/ft)	watts/centimeter (W/cm)	30.81
Burnup	megawatt-day/metric ton of heavy metal ^(e) (Mwd/MTM)	gigajoule/kg of heavy metal ^(e) (GJ/kgM)	8.64×10^{-2}

(a) Conversions are based on ASTM Metric Practice Guide E380-74. The SI prefixes, SI symbols, and associated multiplication factors are:

Prefix	SI Symbol	Multiplication Factors
tera	T	1 000 000 000 000 = 10^{12}
giga	G	1 000 000 000 = 10^9
mega	M	1 000 000 = 10^6
kilo	k	1 000 = 10^3
hecto	h	100 = 10^2
deka	da	10 = 10^1
deci	d	0.1 = 10^{-1}
centi	c	0.01 = 10^{-2}
milli	m	0.001 = 10^{-3}
micro	μ	0.000 001 = 10^{-6}
nano	n	0.000 000 001 = 10^{-9}
pico	p	0.000 000 000 001 = 10^{-12}
femto	f	0.000 000 000 000 001 = 10^{-15}
atto	a	0.000 000 000 000 000 001 = 10^{-18}

(b) 10^6 Btu = 1.055 GJ = 0.17 barrels of oil^(c) = 100 kW-hr.^(d)

(c) One barrel of oil = 42 gallons.

(d) A conversion efficiency from heat to electricity of 0.33 is assumed.

(e) M is normally the SI symbol for mega; an exception in this report is the additional use of M in burnup units to denote heavy metal.

ABSTRACT

This is the first quarterly report issued on three programs at Battelle, Pacific Northwest Laboratories (PNL) sponsored by the Fuel Behavior Research Branch of the Division of Reactor Safety Research (RSR) of the Nuclear Regulatory Commission (NRC). This report covers the reporting period through March 1977 and includes background information on work performed prior to January 1977. The three programs are described below.

EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM

The primary objective of this program is to obtain well-characterized experimental data that will be used in the verification of the GAPCON-THERMAL and FRAP computer codes. The data will also provide a series of benchmarks for indexing other thermal performance codes used for reactor safety analyses. These objectives will be attained by conducting a series of in-reactor and ex-reactor tests. Status of and findings from these tests are described.

In-reactor tests are being conducted in the Halden Boiling Water Reactor (HBWR). Two, instrumented, six-rod fuel assemblies (IFA-431 and IFA-432) were designed to focus on the major uncertainties affecting nuclear fuel licensing in the U.S. IFA-431 had a design peak fuel rod power of 328 W/cm (10 kW/ft) and was discharged as planned after achieving a burnup of 372 GJ/kgM (4300 Mwd/MTM). IFA-432 has a design peak fuel rod power of 492 W/cm (15 kW/ft) and is presently being irradiated; goal burnup is 1730 GJ/kgM (20,000 Mwd/MTM). The initial irradiation experiments are directed towards obtaining data on fuel relocation mechanisms and kinetics. Fission gas release kinetics and the effects of various gas compositions are also being investigated to obtain sufficient data for code verification.

The ex-reactor tests are being conducted to extend the contact conductance work of Ross and Stoute to more relevant gas pressures, temperatures, and compositions. The thermal pulse and axial (longitudinal) techniques appear to be the best experimental means for investigating the sensitivity of gap and contact conductance to changes in the parameters of interest. Design, construction, and testing of and data acquisition from the Modified Pulse Design (MPD) and Modified Longitudinal Design (MLD) experiments are discussed.

FUEL SUBASSEMBLY PROCUREMENT AND IRRADIATION TEST PROGRAM

This program is to provide for 1) the design, fabrication, and assembly of a 4X type test train to be used in irradiation tests in the Power Burst Facility (PBF), 2) pretest predictions of stored energy tests in the PBF, 3) design and fabrication of a dual purpose six-rod test assembly for irradiation in the Halden Reactor, and 4) planning and design of a 16-rod LOCA assembly. The conceptual design of the 4X test assembly will attempt to improve functional reliability and promote test throughput through the use of a quadrant system. In this system a single cluster of instrumentation will be available for each of the four fuel rods and can easily be removed or replaced as required to facilitate calibration and repair.

Plans have been made to ship irradiated HBWR fuel rods to PBF for use in the forthcoming transient tests and new fuel has been fabricated for use in a later HBWR-PBF test series.

PRESSURE TRANSDUCER DEVELOPMENT AND TEST TRAIN INSTRUMENTATION QUALIFICATION PROGRAM

The aim of this program is to develop several commercial sources of compact dynamic pressure transducers suitable for use in the Fuel Behavior Research Branch and other RSR programs. An LVDT-Bellows pressure transducer operating on the principle of coupling an LVDT to the axial motion of a bellows caused by changes in the bellows internal pressure has been chosen for further evaluation in a device capable of dynamically measuring internal fuel rod pressures under reactor operating conditions. A commercial supplier has been contacted and several prototypes ordered. Design specifications and a qualification plan for test and evaluation are presented.

CONTENTS

LIST OF CONTRIBUTORS	ii
SUMMARY	iii
CONVERSION TABLE FOR U.S. CUSTOMARY TO SI UNITS	xiv
ABSTRACT	xv
FIGURES	xix
TABLES.	xxv
INTRODUCTION	1
EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM	1
FUEL SUBASSEMBLY PROCUREMENT AND IRRADIATION TEST PROGRAM	5
PRESSURE TRANSDUCER DEVELOPMENT PROGRAM	6
EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM	7
TASK A: HALDEN REACTOR IRRADIATION TESTS	7
Background for Task A	7
Current Progress on Task A	47
TASK B: EX-REACTOR DETERMINATION OF GAP AND CONTACT CONDUCTANCE	121
Background for Task B	121
Current Progress on Task B	138
FUEL SUBASSEMBLY PROCUREMENT AND IRRADIATION TEST PROGRAM	147
TASK A: DESIGN AND FABRICATION OF 4X TEST TRAIN	147
Background for Task A	147
Current Progress on Task A	148
TASK B: ANALYSIS OF PBF GAP CONDUCTANCE TESTS	152
Background for Task B	152
Current Progress on Task B	153
TASK C: DUAL PURPOSE THERMAL PERFORMANCE ASSEMBLY	154
Background for Task C	154
Current Progress on Task C	155
TASK D: SIXTEEN-ROD TEST ASSEMBLY	156
Background for Task D	156
Current Progress on Task D	157

PRESSURE TRANSDUCER DEVELOPMENT PROGRAM	159
TASK: DEVELOP PRESSURE TRANSDUCER	159
Background for Task	159
Resistance Strain Gauges.	160
LVDT - Bellows	160
Capacitance Strain Gauges	161
Potentiometric-Bellows and Piezoelectric Transducers	163
Resonant Cavity	163
Additional Transducer Types	164
Current Progress on Task.	165
REFERENCES.	167
APPENDIX A - SPECIFICATION FOR LVDT-BELLOWS TYPE PRESSURE TRANSDUCERS OPERATING IN A NUCLEAR ENVIRONMENT	A-1
APPENDIX B - QUALIFICATION PLAN FOR LVDT-BELLOWS TYPE TRANSDUCERS	B-1

FIGURES

1	Stored Energy in a PWR Fuel Rod	2
2	Code Evaluation Flow Chart	4
3	Instrumented Rod Data for Initial Diametral Gaps of 0.150-0.180 mm (0.006-0.007 in.)	4
4	Schematic Diagram of an NRC/PNL Instrumented Fuel Assembly (IFA-431)	9
5	Fuel Pellet Roundness at Three Locations	16
6	Cladding Roundness	16
7	Flow Chart for Evaluation of Uncertainties in Power Distribution	18
8	Comparison of Predicted Fuel Centerline Tempera- ture with Measured Temperature of Rod 1 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power.	24
9	Comparison of Predicted Fuel Centerline Tempera- ture with Measured Temperature of Rod 2 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power	24
10	Comparison of Predicted Fuel Centerline Tempera- ture with Measured Temperature of Rod 3 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power.	25
11	Comparison of Predicted Fuel Centerline Tempera- ture with Measured Temperature of Rod 4 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power.	25
12	Comparison of Predicted Fuel Centerline Tempera- ture with Measured Temperature of Rod 5 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power.	26
13	Comparison of Predicted Fuel Centerline Tempera- ture with Measured Temperature of Rod 6 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power.	26
14	Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 1 in IFA-431	27

15	Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 2 in IFA-431	27
16	Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 3 in IFA-431	28
17	Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 4 in IFA-431	28
18	Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 5 in IFA-431	29
19	Thermal Conductivity of PNL, 95% TD, Stable UO ₂ Fuel Compared with Previous Data	30
20	Fuel Pellet Stack Arrangement for Rod 4 Which Contains Xenon Fill Gas	33
21	Rod 1 Gap Conductance and Temperature Parameter Uncertainties.	36
22	Rod 6 Gap Conductance and Temperature Parameter Uncertainties.	36
23	Gap Conductance and Gap Conductance Uncertainty as a Function of Gap Temperature Gradient (Δt) for IFA-431 Fuel Rods at a Linear Heat Rating of 328 W/cm (10 kW/ft) and Based on Upper Thermo-couple Data	37
24	Schematic Drawing of IFA-431 Fuel Rod Without Pressure Transducer	38
25	Schematic Drawing of IFA-431 Fuel Rod with Pressure Transducer	39
26	Schematic Drawing of Pressure Transducer in IFA-431	40
27	Measured and GAPCON-THERMAL-2 Predicted Pressure History for Rod 1 at Full and Zero Power Operation	40
28	Measured and GAPCON-THERMAL-2 Predicted Pressure History for Rod 5 at Full and Zero Power Operation	41
29	Measured and GAPCON-THERMAL-2 Predicted Pressure History for Rod 6 at Full and Zero Power Operation	41

30	Rod 1 Pressure History at Zero Reactor Power with the Measured Pressures Normalized to the Expected Pressure Assuming No Sorbed or Fission Gas Release . . .	43
31	Rod 5 Pressure History at Zero Reactor Power with the Measured Pressures Normalized to the Expected Pressures Assuming No Sorbed or Fission Gas Release . . .	43
32	Rod 6 Pressure History at Zero Reactor Power with the Measured Pressures Normalized to the Expected Pressures Assuming No Sorbed or Fission Gas Release . . .	44
33	Flow Chart for Determining Gap Conductance . . .	52
34	Schematic Arrangement for Assembly Power Calibration . . .	54
35	Power Calibration Data for IFA-431 . . .	55
36	Thermal Conductivity of Fuel as a Function of Fuel Temperature for Bates and Mellinger Unirradiated Fuel and Lyons Irradiated Fuel . . .	63
37	T_r Versus q for TF2, Rod 1 (near beginning of life) . . .	78
38	Power and Temperature Data for TF2, Rod 1, Run 3 . . .	78
39	Normalized Data for TF2, Run 3 . . .	80
40	Thermal Resistance Versus Linear Heat Rating (q) for Rod 1 of IFA-431 at the TF2 Plane. . .	81
41	Plot of Comparative Computer Runs for Rod 1 of IFA-431 at the TF2 Plane . . .	82
42	Startup Temperature History for IFA-431 . . .	89
43	Longitudinal Cross Section of Rod 4 Operating at a Linear Heat Rating of 200 W/cm . . .	90
44	Flow Chart of Analysis Procedure . . .	90
45	Cutaway View of the Eccentric Portion of Rod 4 . . .	92
46	Fuel Centerline Temperature Versus Hot Gap as a Function of Gap Geometry for a Xenon-Filled Rod . . .	93
47	Gap Conductance Versus Hot Gap as a Function of Fill Gas for a Xenon-Filled Rod . . .	94
48	Arrangement of Temperature Sensors, Neutron Detectors, and Fuel Relative to Reference Axial Thermal Flux Profile, IFA-431 and 432. . .	99
49	Location of Test Assemblies in the Reactor and Fuel Rod Orientation . . .	100
50	Schematic Arrangement of Instrumented and Noninstrumented Fuel Rods Irradiated in IFA-431 and IFA-432 . . .	102

51	Fuel Stack with Displaced Pellet Fragment.	104
52	A Comparison of Elongation Detector Outputs During Ramp A and B for Rod 1 in IFA-431.	107
53	Schematic Arrangement of Fuel Rod Elongation Measurement System	109
54	Elongation Detector Output as a Function of Boiling Power at Constant Fuel Rod Linear Heat Rating.	110
55	A Comparison of Fuel Rod Elongations for Rods Containing Stable and Unstable Fuels During Initial Ramp to Power.	112
56	A Comparison of Fuel Rod Elongations for Rods Containing Stable and Unstable Fuel at Low Exposures.	112
57	Measured Fuel Thermal Conductivities for Nonirradiated Fuel Types Used in IFA-431 and IFA-432	113
58	Comparison of Fuel Rods Containing Fuel of Different Thermal Conductivities.	113
59	A Comparison of Fuel Rod Elongation for Rods Having Small and Large Gaps	115
60	Relationship of Fuel Centerline Temperature to Cladding Axial Elongation in Small Gap Rod	116
61	EX-GAPCON Program Progression.	124
62	Schematic of MPD Vessel (vacuum and low gas pressures) and System Components.	126
63	MPD Vacuum and Low Pressure Apparatus.	127
64	Thermal Diffusivity of ARMCO Iron from 200 to 820°C as Measured on MPD Apparatus Compared with Reported Thermal Diffusivity Data	128
65	Thermal Contact Resistance, R, Versus Eigenfunction, λ , (Equation 5) for an Aluminum-Aluminum Sample Pair	131
66	Thermal Contact Resistance, R, Versus Eigenfunction, λ , (Equation 5) for a UO ₂ /Zircaloy-4 Sample Pair	131
67	Normalized Temperature Versus Time for a 316 Stainless Steel-Brass Sample Pair Comparing Equation (10) with the Experimental Data	134
68	Modified Longitudinal Design (MLD) Apparatus	136
69	Ex-GAPCON Program Progression - March 1977	139
70	Problem Geometry and Boundary Conditions	140
71	Comparison of MPD Contact Conductance Results with that of Rapier, Dean, and Ross and Stoute.	145

72	Comparison of MPD Gap Conductance Data with that Predicted by the Lloyd, Kennard and GAPCON Models (He Atmosphere)	145
73	Comparison of MPD Gap Conductance Data with that Predicted by the Lloyd, Kennard and GAPCON Models (Ar Atmosphere)	146
74	4X Test Train Quadrant Hardware	149
75	Eight-Hole Head and Upper Assembly Hardware	150
76	Radioactive Instrument Package Transported from PBF Canal.	151
77	Entire Test Train Transported from PBF Canal	152
78	Capacitive Strain Gauge - Cross Section View	161
79	Resonant Cavity System for Measuring Fuel Rod Pressure	164
A-1	Schematic of Fuel Rod Pressure Measuring System	A.3

TABLES

1	Design Parameters and Instrumentation for Fuel Assemblies IFA-431 and IFA-432	8
2	Power Correction Factors	19
3	Power History of IFA-431	23
4	HBWR Operating Data	48
5	Fuel Thermal Conductivity Uncertainty Interval Ranges	63
6	Input Parameters and Uncertainties for IFA-431	65
7	Water Film Temperature Drop, Δt_f	68
8	Cladding Temperature Drop, Δt_c	69
9	Cladding Inside Surface Temperature, t_i	69
10	Fuel Surface Temperature, t_s	71
11	Gap Conductance, h_g	73
12	IFA-431 Power Drops	75
13	Estimated True Centerline Temperature Minus Initial Thermocouple Reading ($^{\circ}\text{C}$).	83
14	Estimated Bias in Normalized Temperature Slope for IFA-431 in June 1975	84
15	Corrected Slopes and Temperature Differences for Runs 3 and 11.	85
16	Recorrected Temperature Differences	85
17	Solutions for Eccentric Portion of Fuel Column	93
18	Effects of Calculational Method in the Eccentric Xenon Case	95
19	Uncertainties in Experimental Geometry	96
20	Summary of Observed Pellet-Cladding Interaction in IFA-431 and IFA-432	105
21	IFA-431 and IFA-432 Ramp Data.	106
22	Pre- and Post-Irradiation Measurements for Helium Filled Rods - IFA-431.	117
23	Results of Deduced Gap Widths for Helium Filled Rods - IFA-431	118
24	Parameters and Range of Interest	122
25	Stages of H_g and H_c Experimentation	125
26	Dual Purpose Thermal Performance Test Parameters	156
A.1	Summary of Requirements for LVDT-Bellows Type Pressure Transducers Operating in a Nuclear Environment	A.2

INTRODUCTION

This is the first quarterly report issued on three programs sponsored by the Fuel Behavior Research Branch of the Division of Reactor Safety Research (RSR) of the Nuclear Regulatory Commission (NRC). The report covers the period through March 1977 and includes some information on activities prior to January 1977 to provide the background needed to place the current activities in proper perspective. The three programs are described below:

EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM

This program was initiated in 1974. It is designed to focus on the major uncertainties (e.g., in thermal stored energy) affecting nuclear fuel licensing in the United States.

The thermal stored energy in a fuel rod is the driving function for the most severe postulated accident, the Loss of Coolant Accident (LOCA). The Final Acceptance Criteria for Emergency Core Cooling Systems (ECCS) require an assessment of the stored energy during normal operation as well as a gap conductance calculation throughout the duration of the accident. Uncertainties in stored energy calculations have caused temporary derating of many commercial power plants and delayed the startup of additional plants. Many of the uncertainties can be attributed to the lack of well-characterized data for fuel operating in the power ranges of commercial nuclear power plants. Specifically, the effects of fuel densification and fission gas on the fuel-cladding gap conductance are difficult to quantify, primarily because the applicable data are sparse.

Most of the uncertainty can be attributed to inadequate mathematical models assembled in computer codes that were developed to simulate fuel rod behavior over a wide range of postulated conditions. Although the total conductance of the fuel rod must be considered in the stored energy calculations, the largest uncertainties in the calculations are associated with changes to the fuel-cladding gap dimensions and gas compositions with their concomitant effects on the gap conductance.

Economic as well as safety issues provide incentives to reduce uncertainties in stored energy* calculations. These incentives are illustrated by Figure 1 which shows the stored energy in a PWR fuel rod as a function of linear heat rating and gap conductance. During normal operation, the stored energy is sufficiently low to preclude exceeding the ECCS peak cladding temperature limit of 2200°F (1204°C) should an accident occur. Phenomena such as densification were postulated to lower the gap conductance and thus raise the stored energy until the calculated peak cladding temperature exceeded the ECCS limit. The position of the ECCS limit on a stored energy curve is plant dependent. Additionally, typical power costs for derating plants range around \$1,000,000 per 1% derating for a year.

FIGURE 1. Stored Energy* in a PWR Fuel Rod⁽³⁾

During the evaluation of a licensing application, the NRC Division Nuclear Reactor Regulation Review process is similar to the flow chart in Figure 2. Several computer codes are used for an independent evaluation of applicant's fuel licensing codes. The GAPCON-THERMAL^(2,3,4,14) and

* In terms of volumetric average fuel temperature.

FRAP-S⁽¹⁵⁾ series of codes, part of the code package, are used for the stored energy calculations. During the initial development of the GAPCON-THERMAL series, a large amount of literature was evaluated to select appropriate data to verify the code or assist in model development. Because the thermal stored energy in the fuel rod is the primary driving force for many of the accidents, we concentrated on experiments that would provide data needed to assess the capability of various codes which calculate fuel temperatures and the fuel-cladding gap conductance. The fuel rod temperature data were either in the form of fuel thermocouple readings or had to be inferred from fuel microstructural changes, i.e., melt radii or grain growth radii; most of the data were microstructural changes, with their inherently large uncertainties. Although experiments containing fuel thermocouple tests provide the most reliable data, we determined there was considerable scatter (Figure 3).

In 1974, few well-characterized data existed for fuel life times beyond a few days. Before fuel densification became an industry-safety issue, the most limiting stored energy developed at beginning-of-life (BOL). Consequently, most efforts in code development concentrated on modeling BOL conductance.

Experimental verification of the computer codes and more well-characterized data would provide a means to quantify the uncertainties of simulating the conditions in a nuclear fuel rod. A collection of mathematical models (i.e., a computer code) is used to simulate the wide range of conditions postulated during an evaluation of reactor fuel safety. Any computer code that is forced to rely on a collection of empirical and semiempirical models for much of the analysis should be used primarily for interpolation. Some extrapolation can be accomplished with models based on first principles; however, well-characterized data are needed in either case to test code predictions.

Since the above evaluation clearly indicated that a strong incentive existed to obtain additional well-characterized data as well as provide a means to verify computer codes, PNL proposed the Experimental Verification of Steady-State Fuel Codes Program. The primary objective of this program is to obtain well-characterized experimental data that can be used in the verification

FIGURE 2. Code Evaluation Flow Chart⁽³⁾

FIGURE 3. Instrumented Rod Data for Initial Diametral Gaps of 0.150-0.180 mm (0.006-0.007 in.)⁽³⁾

of the GAPCON-THERMAL and FRAP computer codes. The data will also provide a series of benchmarks for indexing other thermal performance codes used for reactor safety analyses. These objectives are being attained by conducting a series of in-reactor and ex-reactor tests. The in-reactor tests are conducted in the Halden Reactor. The initial irradiation experiments are directed towards obtaining data on fuel relocation mechanisms and kinetics. Fission gas release kinetics and the effects of various gas compositions are also being investigated to obtain sufficient data for code verification. The ex-reactor tests are being conducted at PNL to extend the contact conductance work of Ross and Stoute to more relevant gas pressures, temperatures, and compositions and to conduct experiments under gap conditions where no data are currently available. This program consists of two tasks:

Task A - Halden Reactor Irradiation Tests

Task B - Ex-Reactor Determination of Gap Conductance

FUEL SUBASSEMBLY PROCUREMENT AND IRRADIATION TEST PROGRAM

This program is designed to provide for (1) the design, fabrication, and assembly of a 4X type test train to be used in irradiation tests in the Power Burst Facility (PBF), (2) pretest predictions of stored energy tests in the PBF, (3) design and fabrication of a dual purpose six-rod test assembly for irradiation in the Halden Reactor, and (4) planning and design of a 16-rod LOCA assembly. The existing design for a 4X test train will be modified or redesigned as required to provide enhanced performance and/or reduced costs. Where possible, the existing instrumentation and hardware will be utilized.

Continued instrumentation development and vendor qualification will be performed to ensure enhanced performance and cost effectiveness. Design analysis of the test train will be performed to establish that design criteria are met.

The dual purpose test assembly will be designed for steady-state irradiation in the Halden Reactor with subsequent testing of the irradiated fuel rods in PBF under transient or accident conditions.

The program consists of the following four tasks:

Task A - Design and Fabrication of 4X Test Train

Task B - PBF Gap Conductance Test Analysis

Task C - Dual Purpose Thermal Performance Assembly

Task D - Sixteen Rod Test Assembly

PRESSURE TRANSDUCER DEVELOPMENT PROGRAM

Under this program, a pressure transducer capable of operating under steady-state or transient conditions in PBF, Halden, and other Fuel Behavior Research Branch experimental programs is to be designed and the pertinent specification criteria identified. A suitable commercial source for prototypes and general supply will be identified and qualified.

EXPERIMENTAL VERIFICATION OF STEADY-STATE CODES PROGRAM

TASK A: HALDEN REACTOR IRRADIATION TESTS

The objectives of the tests are to obtain well-characterized data to be used in (1) verification of steady-state fuel codes, and (2) development of improved models for stored energy calculations.

Background for Task A

A test matrix (Table 1) was developed and two instrumented assemblies for the Halden Boiling Water Reactor (HBWR) were designed in 1974 to provide well-characterized data.^(2,3) The tests summarized in Table 1 will provide data on the effects of xenon, fuel eccentricity, gap closure, fuel densification, and fission gas release on the thermal stored energy in fuel rods that bracket contemporary commercial designs. A schematic diagram of a typical NRC/PNL instrumented fuel assembly is shown in Figure 4. When the two fuel assemblies, IFA-431 and 432, were designed in July 1974, minimal data were available describing the effects of burnup on LWR fuel and there were no data describing the effects of fuel densification on fuel temperatures.

Progress on this task and program has been described^(3,16-18) at the First through Fourth Water Reactor Safety Research Information Meetings, the last of which was held in September 1976. The detailed background information associated with this task is described below.

Reference Document

BNWL-1988,⁽¹⁾ a report in publication, contains information on the test design, precharacterization, and fuel assembly fabrication for IFA-431 and IFA-432. This report is intended to be a reference document for future analytical and experimental work conducted for the Experimental Verification of Steady-State Fuel Codes program as well as other Reactor Safety Research and industry safety programs. The test rationale, design, fabrication, and precharacterization data are included in extensive detail to ensure that all pertinent information on the assemblies will be available for future mathematical modeling and data reduction. The information is necessary to quantify the uncertainty in thermal stored energy and thus reduce restrictions on commercial power plant operation.

TABLE 1. Design Parameters and Instrumentation for Fuel Assemblies IFA-431 and IFA 432⁽⁵⁾

Fuel Assembly No.	Rod No.	Pellet OD, ^(a)		Cold Diametral Gap, ^(a)		Fill Gas ^(b)	Fuel Density, % TD	Fuel Type ^(c)	Instrumentation				Design Peak Rod Power		Design Burnup ⁽ⁱ⁾	
		mm	in.	mm	in.				Temperature Upper	Temperature Lower	Pressure	Cladding Length	W/cm	kW/ft	GJ/kgM	MWd/MTM
IFA-431	1	10.681	0.4205	0.229	0.009	He	95	Stable	TC ^(d)	TC	PT ^(e)	ES ^(f)	328	10	345	4,000
	2	10.528	0.4145	0.381	0.015	He	95	Stable	TC	TC	--	ES	328	10	345	4,000
	3	10.858	0.4275	0.051	0.002	He	95	Stable	TC	TC	--	ES	328	10	345	4,000
	4	10.681	0.4205	0.229	0.009	Xe	95	Stable	TC	TC	--	ES	328	10	345	4,000
	5	10.681	0.4205	0.229	0.009	He	92	Stable	TC	TC	PT	ES	328	10	345	4,000
	6	10.681	0.4205	0.229	0.009	He	92	Unstable	TC	TC	PT	ES	328	10	345	4,000
IFA-432	1	10.681	0.4205	0.229	0.009	He	95	Stable	TC	TC	PT	ES	492	15	1730	20,000
	2	10.528	0.4145	0.381	0.015	He	95	Stable	UT ^(h)	TC	--	ES	492	15	345	4,000 ^(g)
	3	10.858	0.4265	0.076	0.003	He	95	Stable	TC	TC	--	ES	492	15	345	4,000 ^(g)
	4	10.681	0.4205	0.229	0.009	Xe	95	Stable	TC	TC	--	ES	492	15	345	4,000 ^(g)
	5	10.681	0.4205	0.229	0.009	He	92	Stable	TC	TC	PT	ES	492	15	1730	20,000
	6	10.681	0.4205	0.229	0.009	He	92	Unstable	TC	TC	PT	ES	492	15	1730	20,000
	7	10.528	0.4145	0.381	0.015	He	95	Stable	--	--	--	--	492	15	1380	16,000
	8	10.681	0.4205	0.229	0.009	He	95	Stable	--	--	--	--	492	15	1380	16,000
	9	10.732	0.4225	0.179	0.007	He	95	Stable	--	--	--	--	492	15	1380	16,000

(a) Diametral gap = cladding ID - pellet diameter. All pellets have flat ends. The cladding for all rods has an OD of 12.789 mm (0.5035 in.) and an ID of 10.909 mm (0.4295 in.).

(b) Gas pressure inside the as-fabricated fuel rod after backfilling was one atmosphere at room temperature.

(c) Expected stability of as-fabricated fuel pellet to resist in-reactor densification.

(d) TC = Thermocouple

(e) PT = Pressure Transducer

(f) ES = Elongation Sensor

(g) Rods 2, 3, and 4 are removable; they are to be replaced by Rods 7, 8, and 9.

(h) UT = Ultrasonic Thermometer

(i) IFA-431 discharged in February 1976 with average burnup of 372 GJ/kgM (4300 MWd/MTM).

FIGURE 4. Schematic Diagram of an NRC/PNL Instrumented Fuel Assembly (IFA-431)

The report is organized as follows:

<u>Section</u>	<u>Purpose</u>
Test Rationale	States the justification for the tests based on an understanding of fuel safety problems in July 1974.
Pretest Predictions	Displays computer code predictions made with the GAPCON-THERMAL-2 and FRAP-S codes before IFA-431 and IFA-432 were charged.
Fuel Rod Design	Contains detailed description of the six rods in each assembly.
Fuel Assembly Design	Includes a description of the assembly hardware.
Fuel Pellet Fabrication	Describes the fabrication process for the three fuel types.
Precharacterization	Provides data of most importance to future data analysis and fuel rod modeling.
Fuel Rod and Assembly Fabrication	Presents a description of the final assembly stages conducted by Halden Project staff.
Appendix	Ensures that all information on the test assemblies will be available to establish initial conditions; includes supporting analytical results, fabrication details, precharacterization data, and certifications.

Fuel Rod Design Parameters

Development of this experimental program included the following considerations:

Contemporary Rod. We were interested in obtaining as many tie points as possible to cross-correlate the data and provide avenues to extrapolate these test results to U.S. commercial nuclear fuel. Accordingly, we selected as a reference a fuel rod with: 1) a BWR-6 geometry, 2) a 95% TD pellet, 3) a 0.009-inch (230- μ) diametral gap and 4) helium fill gas. In addition to minimizing the difficulty of extrapolating the data from the Halden tests to commercial plants, the larger BWR pellets tend to reduce perturbations induced by thermocouples. Cladding procurement problems were also minimized because atypical cladding dimensions are difficult to obtain from commercial sources. The 95% TD fuel density was selected to assure a densification resistant fuel, to provide a density typical of 1974 fuel designs, and to provide a tie point to previous thermal conductivity measurements on unirradiated fuel. Rod 1 in each assembly fulfills the above objectives.

Instantaneous Densification. The major impact of fuel densification⁽¹⁹⁾ was the assumption that the fuel underwent isotropic shrinkage directly proportional to the difference between the fabricated density and the terminal density. For example, a 93.5% TD BWR fuel pellet (12.395 mm diameter) would shrink 0.1239 mm if the NRC criteria of a 96.5% TD terminal density were applied. Since most BWR fuel rods of the above pellet size would contain an initial diametral gap of 280 to 305 μ , the shrinkage induced by densification would create a diametral gap of $\sim$ 380 to 430 μ . Consideration of tolerances, i.e., ± 0.038 mm on the initial gap, resulted in even larger gaps. Until data became available from the EEI/EPRI UO₂ densification program,^(20,21) the Halden program,⁽²²⁾ and individual fuel vendor programs, instantaneous densification was assumed (i.e., a gap of 405 μ , for the example above, was assumed to exist as soon as the plant reached full power). Therefore, Rod 2 in each assembly was designed with a 0.015-in. (380- μ) diametral gap to simulate instantaneous densification. The 380- μ gap in the BWR-6 geometry (10.68 mm reference pellet diameter) yields a gap/diameter (G/D) ratio of 0.036 which approximates the G/D ratios typically postulated for densified fuel in either the older BWR or PWR fuel designs.

Internal Reference. Rod 3 in each assembly was designed primarily for an independent check of rod powers. Both rods contain 95% TD fuel; however, Rod 3 in IFA-431 has an initial diametral gap of 0.002 in. ($\sim 50 \mu$) while Rod 3 in IFA has 0.003 in. ($\sim 75 \mu$). These gaps were chosen to provide good contact at power and yet minimize the potential for cladding failure induced by mechanical interaction. The small gap should be closed at power which produces very high gap conductances and thus reduces the temperature gradient across the fuel-cladding interface. Minimizing the temperature gradients across the gap is a good check of the effective thermal conductivity of the fuel in-reactor. Because thermocouples will indicate the fuel centerline temperatures and we will be able to calculate cladding temperatures, we can check rod powers in the assembly. The technique that will be used is based on the conductivity integral.⁽²³⁾ The rod will also provide an upper bound for gap conductance and could be used to simulate high burnup rods where the initial gap has been closed by various fuel relocation mechanisms.

Effects of Xenon. Experimental irradiations indicate that the apparent conductances in fuel rods filled either with Xe or Xe-Kr are better than the values calculated by accepted analytical codes. To understand the anomalous thermal behavior of Xe-filled rods, we designed Rod 4 in each assembly to contain 95% TD stable fuel, 0.009-in. ($230\text{-}\mu$) diametral gap, and Xe fill gas.

Verification of Fuel Stability. Results of recent experimental work provided the technology required to fabricate low density (e.g., 92% TD) fuel with various degrees of stability or resistance to irradiation-induced densification. New commercial designs under consideration will use low density, stable fuel. Rod 5 in each assembly contains 92% TD stable fuel and should provide one means of verifying recent fuel fabrication technology with regard to the effects of fuel structures on irradiation temperatures. This rod will also be a cross-correlation with Rod 6.

Densification Kinetics. Rod 6 in each fuel assembly contains 92% TD unstable fuel and a 0.009-in. ($230\text{-}\mu$) gap. Unstable fuel with a similar structure^(20,24) underwent significant densification. Data from Rod 6 will be used to verify NRC densification models and provide a check of applicant supplied models. Few data are now available; however, dimensional changes in

the unstable fuel and the concomitant effects on the gap conductance are probably not as large as calculated using 1974 licensing criteria. Fuel relocation mechanisms such as cracking and unrestrained swelling may mitigate some densification.

Assembly Powers and Burnups. By using larger rod arrays, new plants have incorporated smaller diameter rods and lower rod heat ratings. For example, BWR assembly designs have increased from 7 x 7 to 8 x 8 rod arrays, while PWR assemblies have been modified to increase the 14 x 14 rod array to 15 x 15, 16 x 16 or 17 x 17 rod arrays. As a result, heat ratings for the two experimental assemblies were selected to bracket peak rod powers for these plants. Thus the design power for IFA-431 is 10 kW/ft (328 W/cm) and for IFA-432, 15 kW/ft (492 W/cm).

Design assembly burnup for each rod in IFA-431 was set at 345 GJ/kgM (4000 MWd/MTM) to ensure complete densification, minimize fuel volume changes induced by fission product swelling, and retain the option of post-irradiation (PIE) confirmation of gap closure data obtained from low burnup rods. Three major differences between IFA-431 and IFA-432 exist in the design parameters. In IFA-432 the design power is 492 W/cm (15 kW/ft), the goal burnup is 1730 GJ/kgM (20,000 MWd/MTM), and the assembly contains an option to replace three rods. Depending on the results obtained from IFA-431, three replaceable rods from IFA-432 may be removed at 345 GJ/kgM (~4000 MWD/MTM) to provide cross-correlation with three identical rods in IFA-431.

Cross-Correlation. The tests were designed to: 1) ensure the means of cross-correlating the data, 2) provide as many independent checks of data validity as possible, 3) ensure against instrument failure, 4) ensure at least internal consistency on a relative basis, 5) and provide some tie points to commercial plant designs as well as other fuel research programs.

One of the basic premises of the test design was to provide a systematic approach that would allow adequate interpolation and extrapolation with computer codes. The first step in this approach was the decision to design IFA-431 and IFA-432 as identical assemblies. An identical design enhanced our ability to interpolate over a range of powers and also replicate initial conditions. For example, we will be able to duplicate all the data from the

IFA-431 first power ramp with IFA-432 power ramp. Use of identical assemblies also reduces the uncertainties associated with assembly and rod power distributions.

The power profile in Halden BWR was also considered during the design. We decided to place the top of the rod at the peak power location, which forces the bottom of the rod to operate at 70 to 80% of the peak rod power. To take advantage of the power distribution, thermocouples were placed in the top and bottom of each rod. No tests had been run in Halden with thermocouples penetrating both endcaps; however, Halden staff were able to develop a workable design. Thermocouples in both ends will allow us to check the ability of various codes to extrapolate over a short power range within the same rod. If a code cannot perform these calculations adequately, calculations of the temperature distribution over a 12-ft (3.65 m) fuel length may also be inaccurate. Using two thermocouples per rod will allow modelers to interpolate between rod powers of 230 W/cm and 490 W/cm with tie points at 325 W/cm and 390 W/cm, whereas single thermocouples would have provided data at only 325 W/cm and 490 W/cm. As a result of reactor cycling, data will be obtained over the entire range from 0 to 525 W/cm. This range should span most of the anticipated normal operating powers for fuel rods in commercial plants.

Tie points with commercial plants and other fuel research programs were also developed, such as selection of the BWR-6 fuel geometry, procurement of commercial quality tubing, and selection of assembly powers. Some of the cladding procured for this program was shipped to Aerojet Nuclear Company (ANC) for use in their Halden tests. Both programs also used the same starting power obtained from the Power Burst Facility (PBF) Program. The length of the rods in both IFA-431 and 432 were designed to be compatible with the PBF test trains; well-characterized irradiated fuel is needed for a number of tests in the PBF program. To provide a tie point to a much larger structural characterization program, some of the fuel structures were similar to those investigated in the EEI/EPRI UO₂ fuel densification study.

A correct assessment of rod powers and the distribution of power within the rods is important to assure the best possible thermal data. To attain

this, we placed seven neutron sensors in each assembly. One is located in the center of the assembly, three are located at the top plane of thermocouples, and three are located at the bottom plane of thermocouples. An extensive calibration of these sensors was conducted during the initial start-up of any assembly. In addition, Rod 3 (50- μ or 75- μ gap) was included as an internal standard. The small gap will be closed at power; thus the temperature gradient across the gap will be minimized. Since the coolant temperature and fuel centerline temperatures will be known, we will be able to obtain an independent check of rod power at both the top and bottom planes in the assembly. We will also be able to compare rod powers and fuel temperatures in both assemblies to assure consistent data.

Fuel and Cladding Precharacterization. An extensive precharacterization of the fuel and cladding was essential to assure quality data and reduce uncertainties. We completed thermal diffusivity measurements on each fuel type up to 1600°C which will span most of the fuel volume in either test. From previous experimental work, the heat capacity and density can be obtained to calculate the thermal conductivity of the fuel as a function of temperature. The thermal diffusivity data will reduce the uncertainty associated with gap conductances calculated from the experimental data.

Establishing the initial dimensions and void volumes within the pins was also necessary to adequately perform all thermal calculations. Consequently, we measured the lengths and diameters of each pellet and the cladding for each rod. All rods are loaded with flat-ended, 10% enriched UO_2 pellets. Each pellet was identified with a unique number to trace pellet types and position within the rod. With this information, we know the axial distribution of gap volume and the plenum volume. Pellet and cladding roundness profiles were also obtained to model the departure from ideal coaxial cylinders used in most computer code models. These roundness profiles (Figures 5 and 6) were determined with very sensitive gauging machines and the scale is amplified considerably. These data will be used to establish an uncertainty on the effective size of the as-fabricated gap.

Geometric densities were determined for all pellets and immersion densities for a significant fraction of the pellets. A correlation was developed

FIGURE 5. Fuel Pellet Roundness at Three Locations⁽²⁾

FIGURE 6. Cladding Roundness⁽²⁾

relating immersion density to geometric densities. These data will be used in two ways: 1) in a correction to rod powers caused by differences in mass distribution and 2) in the verification of NRC resintering models used to characterize the propensity of fuel to densify. Resintering tests were conducted on each fuel type to provide initial conditions and a pretest prediction for NRC resintering models.

The EEI/EPRI UO₂ densification program⁽²¹⁾ demonstrated that the pore-size distribution is an important parameter to characterize the stability of the fuel. We wanted to assure that the 95% TD stable fuel types were actually stable; we also wanted the 92% TD unstable fuel to undergo large amounts of in-reactor densification to determine the effects on fuel temperatures. Both fuel densities and pore-size distribution can be measured during postirradiation examination should it prove necessary to verify the relative stability of the fuel types.

Determination of Power Distribution

One of our objectives is to establish uncertainty values for the gap conductances deduced from measured centerline temperature. Most of the errors in the system accumulate in the gap conductance value because both the inner temperature of the cladding and the fuel surface temperature are calculated. Thus, evaluating uncertainties in the power distribution is one of the most critical factors for these stored energy tests. The flowchart for the evaluation is shown in Figure 7.

The first phase of work yielded the power correction factors listed in Table 2. Those factors listed under the column, Calibration Factor, were determined during the calibration. The axial shape factor corrects for the assumption that the mean value of flux along the length of the rod is equal to the flux at the detector locations.

The neutron detector average factor accounts for the failure of one of six vanadium neutron detectors. The average of the neutron detectors is reported as

$$1/2 \left[1/3 \sum_{i=1}^3 ND_i + 1/2 \sum_{i=4}^5 ND_i \right] = ND = Q_{A/K}$$

TABLE 2. Power Correction Factors^(2,3)

Rod Number	Fuel	Radial Tilt	Calibration Factor			Total (Startup)	Burnup
			Axial Shape	Neutron Detector Average	Gamma Heating		
1	1.019	1.106	0.952	1.038	0.988	1.1003	0.1% per 1000 MWd/MTM
2	0.9628	0.945	↓	↓	↓	0.8883	
3	1.0614	0.925	↓	↓	↓	0.9596	
4	1.0044	1.125	↓	↓	↓	1.1032	
5	0.9791	0.9870	↓	↓	↓	0.9435	
6	0.9878	0.9570	↓	↓	↓	0.9229	

where Q_A = assembly power
 K = the calibration factor.

The linear heat generation rate (LHGR) is:

$$LHGR = \frac{KND_i}{6L(1000)}$$

which assumes the flux at each rod is the same as the nearest detector. The above approach would be satisfactory if a severe flux tilt did not exist; however, IFA-431 was located in the outer ring position 6-14. We have corrected the calibration factor by a term equal to:

$$\frac{1/2 \left[\frac{1/3 \sum_{i=1}^3 ND_i + 1/2 \sum_{i=4}^5 ND_i}{1/6 \sum_{i=1}^6 ND_i} \right]}{1/2} = \frac{ND}{1/6 \sum_{i=1}^6 ND_i} = \frac{Q_A/K}{1/6 \sum_{i=1}^6 ND_i}$$

$$\text{where } ND_6 = ND_3 \frac{\frac{ND_4}{ND_1} + \frac{ND_5}{ND_2}}{2}$$

The factor for gamma heating was derived from Halden-supplied information and will vary with reactor power level. The effect is to lower the actual assembly power.

The flux tilt was as much as 40% across the IFA-431 assembly, and thus the flux at a rod in the neutron detector plane is not the same as the flux at the nearest detector location. We have deduced radial tilt factors from the positions of the rods and the detectors, and the radial tilt factor is presented in the second column in Table 2.

The fuel correction factors correct for differences in mass distribution within the separate rods. For example, the fuel correction factor would be 1.0 if all rods had the same fuel length, volume, and density. The factors combine to give the totals in Table 2. Comparisons of the relative changes within any one rod would not be affected by these corrective factors; however, they have a large influence on rod-to-rod comparisons (e.g., Rod 1 versus Rod 2). In addition, we are correcting for ^{235}U depletion effects at the rate of 1% relative per 1000 MWd/MTU.

Dynamic Temperature Measurements

A study was initiated to see if an independent estimate of the fuel centerline temperature could be obtained from the response of a thermocouple to a linear power change. Implementation of a series of power transients was accomplished by control rod movements far removed from the IFA-431 test assembly. The response of the entire reactor was similar to a point reactor. This was evidenced by the output of neutron detectors in other assemblies. Three power drops were performed for each series, and a series was obtained approximately once a month. The objective of each power cycle was to obtain ~20% linear reduction in power over a 60-sec time interval (actual drops for the initial series with IFA-431 were ~25% in <35 sec). Although the series of cycles were different, the replication of power drops within a given series was excellent and the data were reproduced.

Gap Conductance Inferred from IFA-431 Data

The program BESSEL and corrected rod powers were used to calculate gap conductances for each of the six rods in IFA-431. At the time of that study (prior to April 1976),⁽²⁾ we limited our analysis to the first power ramp because we had not completed the determination of all the correction factors that apply to the calibration factor. Effects such as burnup, control rod movement, and neutron detector response time were being evaluated. These

data were reduced with the PNL determined thermal conductivities as calculated from measured thermal diffusivities. A consistent bias existed in that the conductances calculated from the lower thermocouples were somewhat higher than those deducted from the upper thermocouples. This bias may be introduced by uncertainties in the neutron detector position at the lower thermocouple plane where the axial gradient is most severe. Neutron detector response in up-power ramps may also contribute. The data were also very sensitive to small differences in calculated coolant channel temperature gradients as evidenced by the upward shift in the data deduced from the upper thermocouples. This shift was obtained as a result of re-evaluating the coolant ΔT (a few degrees) between upper and lower thermocouple planes. Similar shifts were produced by minor changes in the ratio of upper and lower powers. These results: 1) emphasized that most system errors eventually accumulate in the gap conductance uncertainties and 2) justified the need for a thorough analysis of power correction factors.

A GAPCON-THERMAL-2 comparison with the data was made prior to April 1976.⁽²⁾ Although the code consistently underpredicted the data by a small margin (with the exception of Rod 4), the amount of underprediction was consistent with the objectives of NRC Division of Technical Review (the code is used to evaluate licensing applications). The philosophy of predicting a lower bound for a data base is gradually changing to a best estimate approach as the results of uncertainty studies become available.

Results such as shown in Figure 3 provide the incentive for uncertainty studies. After the data from the tests in this program are validated, we intend to perform post-test predictions with both GAPCON-THERMAL and FRAP-S codes. This will enable us to suggest areas for improved model development. Minor modifications to the fuel thermal expansion model, fuel cracking models, or consideration of pellet-cladding profiles depicted in Figures 5 and 6 should produce good agreement with the data from all rods of IFA-431 except Rod 4. The preliminary results from Rod 4 (xenon filled) suggested that the conductances were much lower than previously reported and that a more rigorous analytical approach is required.

Comparison of Pretest Predictions with Preliminary Data from IFA-431

Pretest prediction were compared with the preliminary data from IFA-431. The results of this study were presented in a paper⁽²⁵⁾ in September 1976 and those same results are described below.

The pretest predictions were made using the GAPCON-THERMAL-2⁽⁴⁾ code (the version dated September 1, 1974) and FRAP-S⁽¹⁵⁾ code (MOD 002-Vers 2F-MATPRO MOD 007). The GAPCON-THERMAL-2 code is used by the Division of Nuclear Reactor Regulation of NRC for evaluating applicants calculations during a licensing review. The FRAP-S code is under development by the Division of Reactor Safety Research of NRC as part of their safety analysis code package.

The pretest predictions made with both of the above codes were compared with preliminary data from IFA-431. The data were preliminary in the sense that correction factors needed to be supplied to obtain the power distribution within the assembly and the uncertainty analyses of the data had not been completed at that time. Every effort was made to ensure both codes received identical input as well as supply the special input required by each code. The power history selected for the simulation consisted of three cycles to full power at arbitrary times within the irradiation. The power history for IFA-431 used in the two codes is shown in Table 3. The major difference in the input for two codes was the lack of a xenon fill gas option and densification model in FRAP-S.

Although the data were preliminary and post-test predictions had not been completed at that time, we made a comparison of data selected from our master file and the predicted results. The first phase used the data from initial rise to power of IFA-431 and compared centerline temperatures at various linear heat ratings. The second phase compared centerline temperatures as a function of burnup. The simulation for the codes was based on a constant power of 10 kW/ft (328 W/cm); however, the rod powers varied within the assembly and during the irradiation. Consequently, we selected data points from the master file that corresponded to steady-state conditions at 10 kW/ft in order to make a valid comparison with the pretest predictions.

The first-phase results showing the pretest predictions for each code and the preliminary data are shown in the Figures 8 through 13. For the first phase, the comparison of centerline temperatures with the data, both codes overpredicted the results with FRAP-S being somewhat more conservative than

TABLE 3. Power History of IFA-431

Time (hr)	Peak Rod Power		Time (hr)	Peak Rod Power	
	(W/cm)	(kW/ft)		(W/cm)	(kW/ft)
0.01	78	2	132.0	328	10
2.4	78	2	156.0	0	0
4.8	155	4	204.0	328	10
7.2	233	6	228.0	0	0
9.6	310	8	1680.0	328	10
12.0	328	10	3360.0	328	10
60.0	328	10	6720.0	328	10
84.0	0	0			

GAPCON-THERMAL-2. The exception was Rod 4 (xenon fill gas) for which FRAP-S predictions were lower than GAPCON-THERMAL-2 and reasonably close to the data. The latter FRAP-S comparison was made with a fission gas fill gas option in the code.

Subsequent FRAP-S calculations were made by E. T. Latts of EG&G in October 1976 using a slightly different version of the code (FRAP-S2 MOD 002 VERS 02G MATPRO MOD 007). Changes were made to the gas thermal conductivity subroutines, fuel swelling models, and a radial power profile option was used that was not available in the version supplied to PNL. The EG&G results have been superimposed on the beginning-of-life cases (Figures 8 to 13). The FRAP-S2 comparisons with the data were improved for all helium-filled rods; however, the predictions were somewhat below the data for Rods 2 and 3. Code modifications produced FRAP-S2 predictions for the xenon rod, Rod 4, that were considerably below the data. No additional calculations were made by EG&G to simulate the effects of fuel burnup.

The effects of fuel burnup were investigated in the second phase. The results (Figures 14 through 18) show that the GAPCON-THERMAL-2 code predictions approach the data after the initial period of irradiation while the FRAP-S code tended to seek a plateau above the data.

Post-test predictions are planned with both codes. The power ramp comparisons should be improved once the PNL UO_2 conductivities determined for each fuel type are input into the codes. The post-test predictions will follow a close approximation of the actual power history and will be compared with the corrected data with its associated uncertainty bands.

FIGURE 8. Comparison of Predicted Fuel Centerline Temperature with Measured Temperature of Rod 1 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power
 (TC = thermocouple, L = lower, U = upper)

FIGURE 9. Comparison of Predicted Fuel Centerline Temperature with Measured Temperature of Rod 2 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power
 (TC = thermocouple, L = lower, U = upper)

FIGURE 10. Comparison of Predicted Fuel Centerline Temperature with Measured Temperature of Rod 3 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power (TC = thermocouple, L = lower, U = upper)

FIGURE 11. Comparison of Predicted Fuel Centerline Temperature with Measured Temperature of Rod 4 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power (TC = thermocouple, L = lower, U = upper)

FIGURE 12. Comparison of Predicted Fuel Centerline Temperature with Measured Temperature of Rod 5 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power (TC = thermocouple, L = lower, U = upper)

FIGURE 13. Comparison of Predicted Fuel Centerline Temperature with Measured Temperature of Rod 6 in IFA-431 at Various Linear Heat Ratings During the Initial Rise to Power (TC = thermocouple, L = lower, U = upper)

FIGURE 14. Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 1 in IFA-431

FIGURE 15. Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 2 in IFA-431

FIGURE 16. Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 3 in IFA-431

FIGURE 17. Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 4 in IFA-431

FIGURE 18. Effect of Burnup on Predicted and Measured Fuel Centerline Temperature and Effect of Burnup on Predicted Internal Gas Pressure for Rod 5 in IFA-431

The trends apparent from the burnup simulations in September 1976 indicated that additional effort would be required to adequately calculate the effects of fuel burnup. Modeling areas that need to be emphasized include fuel thermal conductivity, gas and gas mixture conductivity, gas release, fuel relocation, and fuel densification.

GAPCON-THERMAL-2 Comparisons with IFA-431 Data

In September 1976,⁽³⁾ we indicated that we could begin to test computer code simulations after the appropriate power correction factors were applied to the data and precharacterization data were available. The GAPCON-THERMAL-2 code uses the Lyon's thermal conductivity relationship for licensing calculations. The thermal diffusivity of each fuel type was measured at PNL and converted to thermal conductivity. A comparison of the conductivity data for 95% TD stable fuel and other UO_2 data is shown in Figure 19. Good agreement was obtained between predicted and measured centerline temperatures on the first power ramp when the PNL conductivities were used in the code. The code comparison with the PNL conductivities was within the data uncertainty for either rod. Use of the PNL-measured conductivities lowered the predicted centerline temperature 8%.

FIGURE 19. Thermal Conductivity of PNL, 95% TD, Stable UO_2 Fuel Compared with Previous Data⁽³⁾

The next phase consisted of testing the code over the approximate power history for Rod 1 (contemporary rod) and Rod 6 (unstable fuel). The results indicated a good comparison if the Lyon's thermal conductivity equation were used; however, the use of the PNL measured thermal conductivity resulted in an $\sim 180^{\circ}\text{F}$ (100°C) underprediction towards the end of life. Both cases used the relocation model as described in the GAPCON-THERMAL-2 documentation. If the relocation model was not used, the code predicted values $\sim 700^{\circ}\text{F}$ (390°C) higher than the data. These results emphasize the need for an improved relocation model which is being developed at PNL from these and other rods in the tests.

Fuel Densification. As reported in September 1976⁽³⁾ the results obtained from Rod 6 (92% TD unstable fuel) of IFA-431 were encouraging in that densified fuel apparently does not produce extraordinarily high fuel temperatures. Maximum temperature increases at constant power levels are $\sim 360^{\circ}\text{F}$ (200°C). The fuel, as characterized by resintering tests and pore size-distribution measurements, should have densified considerably. The GAPCON-THERMAL-2 code predictions are $\sim 1300^{\circ}\text{F}$ (720°C) or 57% higher than the data if Lyon's thermal conductivity is used. Predictions using the PNL measured thermal conductivity were $\sim 1200^{\circ}\text{F}$ (670°C) higher.

These predictions were made using the NRC densification model which is based on data obtained from the Halden Program⁽²²⁾ and the EEI/EPRI UO_2 densification program.⁽²¹⁾ Resintering tests at 1700°C for 24 hr provide density changes that bound most of the in-reactor density changes. The EEI/EPRI UO_2 densification program provided data needed for the resintering test correlation and is used as a basis for determining the terminal densities. The densification kinetics portion of the NRC model was derived from Halden data and provides density change as a function of burnup.

The small temperature increase indicated by the data and the large overprediction by the code were very encouraging because licensing restrictions related to fuel stability could possibly be relaxed. The radial shrinkage postulated for densified fuel may be mitigated by fuel cracking and other relocation mechanisms. A series of postirradiation examinations are underway to define the final gas composition, residual gap, and fuel density changes.

The large overprediction when the densification mode was used in the code was also very encouraging because it suggests the radial shrinkage induced by densification is mitigated by gap closure mechanisms. Preliminary data obtained as a function of burnup from Rod 2 (simulating instantaneous densification) and Rod 6 suggested that the fuel temperatures were tending to converge and supported a gap closure mechanism (the temperatures in Rod 2 were still higher than those in Rod 6). Computer simulations of the xenon rod behavior also tended to produce higher temperatures. Better comparisons are expected as fuel relocation and the effects of xenon are better characterized.

Effects of Xenon and Asymmetric Pellet-Cladding Gap Geometry. Early work⁽²⁶⁻²⁸⁾ indicated that most codes tend to overestimate the effects of low-conductivity gases. More recent work⁽²⁹⁾ showed good comparison with laboratory thermal conductivities and a strong effect of fuel eccentricity. Other experimental work suggested that the conductivity of a helium-xenon mixture was similar to helium. These results are summarized below:

- Cohen et al.^(24,30) reported no apparent effect of the gas atmosphere within the rods. Rods having diametral gaps of about 75 μ showed no apparent difference between He and a 25% Kr-75% Xe mixture.
- Horn⁽²⁷⁾ indicated mixtures of He-38% Xe and He-60% Xe resulted in about the same gap conductance as 100% He. Horn's tests were conducted at high heat ratings with initial diametral gaps of about 150 μ .
- Lawrence et al.⁽²⁸⁾ reported a He-41% Xe mixture had no apparent effect (relative to 100% He) on the calculated gap conductance. The tests reported by Lawrence et al. consisted of irradiating UO_2 -25 wt% PuO_2 rods that have had diametral gaps of about 280 μ at about 24 kW/ft.

To understand the anomalous thermal behavior of Xe-filled rods, we designed Rod 4 in each assembly to contain 95% TD stable fuel, have a 230- μ diametral gap, and be filled with xenon gas. We also designed the fuel stack to study the effects of fuel eccentricity. The top of the rod contains pellets centered with a molybdenum rod. Fabrication tolerances preclude a perfect simulation of a concentric stack, but a relative comparison can be made with the lower portion of the stack. The lower portion of the fuel stack was

forced into line contact with the cladding by using a molybdenum rod inserted into holes drilled off center in the pellets. A schematic of the design is shown in Figure 20.

FIGURE 20. Fuel Pellet Stack Arrangement for Rod 4 Which Contains Xenon Fill Gas (one atmosphere at room temperature)

As indicated in April 1976,⁽²⁾ data derived from the IFA-431 tests showed a much larger effect of xenon than suggested by the experiments discussed above. A relative comparison between rods with similar gaps operating at 255 W/cm indicated that the centerline temperature in the xenon-filled rod was approximately 700°C higher than the centerline temperature in the helium-filled rod. These results were more consistent with earlier code predictions; however, the code still overpredicted the effects of xenon. In addition, the results showed that eccentricity is indeed a factor in reducing fuel temperatures.

The analysis of these data has proven to be complex. We are in the process of simulating various degrees of fuel eccentricity and relocation with a finite difference code that can accommodate asymmetric geometries. The results obtained prior to April 1976⁽²⁾ showed that a reasonable amount of fuel relocation could produce good agreement with the concentric portion of the xenon-filled rod. Simulation of the eccentric condition indicated that line contact was not sufficient to give agreement with the data. This suggested that fuel cracking and movement need to be considered.

Prior to April 1976, we were also simulating the transient behavior with the above code to evaluate various hypotheses which had been suggested to explain the complex behavior. The data and code simulation suggested that less conventional modes of heat transfer may need to be invoked to simulate the behavior of the xenon-filled rod. At that time we considered only gas and solid conduction and radiation heat transfer.

Uncertainty Analyses

As indicated in September 1976,⁽³⁾ an extensive error and uncertainty analysis of the data is being conducted on these experiments to quantify or at least provide some measure of the reliability of the results. Since most of the data are actually single sample in nature due to the dynamic conditions in the fuel rods, it is difficult to apply standard methods. Consequently, we chose to define uncertainty as the possible value the error might have. Error is defined as the difference between true and observed values and is a fixed number for a single measurement while the uncertainty may vary depending on the situation. Attaining the objective of defining the reliability of the results depends on choosing a methodology for: 1) estimating uncertainty in the variables, 2) calculating the propagation of the uncertainties and 3) reporting results in a concise manner.

Engineering estimates of the uncertainties were required for a number of the input variables because of the single sample nature of the test. Engineering estimates of uncertainty levels typically correspond to maximum limits. We assumed the engineering estimates are equivalent to three standard deviations or $e = \pm(\frac{3\sigma}{\mu})100$ where μ is the typical value and σ = one standard deviation. We also assumed that the factors in the various equations needed for the rod power, gap conductance, etc. calculations are statistically independent.

One of the flow charts we have used for these uncertainty analysis is shown in Figure 7. The preliminary results for uncertainties in parameters in the IFA-431 test were reported in September 1976⁽³⁾ and at that time it was apparent that additional work is required to verify second order effects and to evaluate the sensitivity of the results to the various parameters in the calculation.

The gap conductance uncertainties were obtained using a first order Taylor series approximation to the nonlinear functions in the calculation. Approximations made by the use of the Taylor series were justified on the relative low uncertainties in the input parameters. The uncertainties in the gap conductance values consist of uncertainties in measured values as well as formula approximation for calculated input. The uncertainty analysis depends on a large number of measured and calculated parameters. We are in the process of evaluating the validity of engineering estimates for the input parameters as well as a check of calculated values. Preliminary results⁽³⁾ obtained from a power ramp at approximately 1500 MWd/MTM are shown in Figures 21 and 22 and indicate that uncertainty in the fuel surface temperature increases with linear heat rating more than the inner cladding surface temperature uncertainty. The uncertainty for the gap conductance data reaches a minimum between 4 and 6 kW/ft. We currently believe the values obtained below 3 kW/ft may be the result of a small bias in the calibration of assembly power. Although this aspect is still under investigation, power correction factors ranging between 1 and 10% give reasonable results. We are evaluating the results from internal standard in the test to arrive at these factors. In either case, preliminary results indicate gap conductance uncertainty for Rods 1 and 6 will be about 20 to 25%.

Also shown on Figures 21 and 22 is a dimensionless parameter consisting of the ratio of the fuel surface temperature, t_s , to gap temperature gradient, Δt_{gap} . This parameter has been dubbed the GAPCON parameter. The GAPCON number is a measure of the sensitivity of the gap temperature gradient to the uncertainty in fuel and cladding surface temperature as well as the magnitude of these temperatures. It has proven useful in these analyses as a parameter to tie together the results from various rods within IFA-431 and will be used to assess the results from IFA-432.

Preliminary results of the uncertainty analyses and gap conductances inferred from the data from all the rods in IFA-431 are shown in Figure 23. The conductances for small gap rods (e.g., Rod 3) are relatively high and the uncertainty exceeds 50%. The large uncertainty is caused by the uncertainties in surface temperature and linear heat rating combined with a relatively small gap temperature gradient. With very large gap temperature gradients (e.g.,

FIGURE 21. Rod 1 Gap Conductance and Temperature Parameter Uncertainties⁽³⁾

FIGURE 22. Rod 6 Gap Conductance and Temperature Parameter Uncertainties⁽³⁾

FIGURE 23. Gap Conductance and Gap Conductance Uncertainty as a Function of Gap Temperature Gradient (Δt) for IFA-431 Fuel Rods at a Linear Heat Rating of 328 W/cm (10 kW/ft) and Based on Upper Thermocouple Data^(3,5)

Rod 4) and thus low gap conductances, the uncertainty is reduced to low values. We intend to complete similar calculations at different burnup and power levels to provide more data as a function of gap temperature gradient.

In-Core Pressure Measurements from IFA-431

A preliminary draft of an interim topical report⁽³¹⁾ on the pressure data obtained during the irradiation of IFA-431 was prepared. Some of the material from that draft is presented below.

The amount and composition of gas within an operating fuel rod are important because of their effect on internal rod pressures and fuel-to-cladding gap conductance, respectively. The effect of gas composition on gap conductance and thus fuel temperatures has been discussed in two previous reports.^(29,32)

General Description of the Rods. A cutaway view of the fuel rod design is shown in Figure 24 (without pressure transducers) and Figure 25 (with pressure transducers). BWR-6 fuel rod geometry was selected to make the tests as relevant as possible to contemporary LWRs. BWR-6 pellets are also larger than PWR pellets which tends to reduce thermocouple perturbations. Each fuel rod nominally contains 45 fuel pellets, 0.5 in. (12.7 mm) long for an active cold fuel length of 22.5 in. (571 mm). Dysprosium oxide pellets are located at each end of the pellet column and a helical spring located in the plenum maintains a compact fuel stack. The cladding for all rods is annealed, seamless Zircaloy-2, with an OD of 0.5035 in. (12.789 mm) and an ID of 0.4295 in. (10.909 mm). A detailed description of the IFA-431 assembly is presented in BNWL-1988.⁽¹⁾

FIGURE 24. Schematic Drawing of IFA-431 Fuel Rod Without Pressure Transducer

FIGURE 25. Schematic Drawing of IFA-431 Fuel Rod with Pressure Transducer

As shown in Table 1, only Rods 1, 5, and 6 of IFA-431 were instrumented with pressure transducers. Each of three rods had a diametral gap of 0.009 in. (229 μ) and contained helium fill gas (one atmosphere at room temperature). Rod 1 is the reference design with 95% TD stable fuel. Rod 5 is designed with a 92% TD stable fuel to confirm the effects of fuel density and to be used as a reference against the 92% TD unstable fuel in Rod 6. Rod 6 is designed to provide information about the effects of densification on heat transfer.

Pressure Instrumentation. Rods 1, 5, and 6 were instrumented with Halden Project internal pressure transducers. The sensing mechanism in the transducer is a thin platinum membrane which is exposed on one side to internal rod gas of unknown pressure, whereas the other side is exposed to externally-controlled helium gas pressure. The pressure balance across the membrane is indicated by the opening of an electrical circuit between the membrane and a contact. Pressure measurements are taken by manually pressurizing the controlled helium gas to about 1 kp/cm^2 * above the highest pin pressure, and then slowly releasing the pressure until the circuit is opened. This procedure is performed identically for each pressure measurement. The accuracy of this system is reported to be $\pm 0.20 \text{ kp/cm}^2$ in the ranges 0 to 240°C and 0 to 30 kp/cm^2 . A schematic of the pressure transducer is shown in Figure 26.

Presentation of Data. The internal gas pressure data measured at full and zero reactor power for Rods 1, 5, and 6 are presented in Figures 27 through 29. Also, shown in Figures 27 through 29 are the pressures predicted by GAPCON-THERMAL-2 for these particular rods. BNWL-2099⁽³¹⁾ contains all

* 1 kp/cm^2 = 1 N/cm^2 = 1 atm (technical) = 0.968 atm (normal).

FIGURE 26. Schematic Drawing of Pressure Transducer in IFA-431

FIGURE 27. Measured and GAPCON-THERMAL-2 Predicted Pressure History for Rod 1 at Full and Zero Power Operation

FIGURE 28. Measured and GAPCON-THERMAL-2 Predicted Pressure History for Rod 5 at Full and Zero Power Operation

FIGURE 29. Measured and GAPCON-THERMAL-2 Predicted Pressure History for Rod 6 at Full and Zero Power Operation

pressure measurements on a month-by-month basis for each of these three rods. The pressure data were taken manually on a demand basis which varied from a few hours to several days. They cover the period from beginning of irradiation life, June 6, 1975, through end-of-life, February 13, 1976.

The measured pressures at or near zero reactor power have been normalized to the expected rod pressures. These normalized pressures are plotted versus chronological time in Figures 30 through 32. The expected pressures are estimated from the one atmosphere fill gas (assuming no sorbed gas release), the calculated void volumes (from dimensional measurements, specifications, and taking into account thermal expansion), and the moderator temperature at the time of the measurement. The pressure ratio (measured/expected) at zero power permits a consistent comparison between the rods because the void volume differences are normalized out. In addition, the expected (i.e., calculated) pressures at zero power have a smaller margin for error because temperatures and differential thermal expansion between the fuel and cladding can be estimated with greater confidence. Also, shown in the figures above is an estimate of the error in the pressure ratio based on instrument error and errors in estimating rod volume (calculated from pellet and clad dimension uncertainties).

Rods 1 and 5 show the same initial results in that rod pressures prior to the first excursion to power on June 6 and 7 were very near those expected (see Figures 30 and 31). After this one day excursion to power, pressures for both rods had decreased by 25 to 30%. These lower-than-expected pressures exceed our estimate of the uncertainty in the pressure ratios and thus would be expected to be a result of the excursion to power. A larger than estimated error in the calculated void volumes would not cause a significant deviation in the measured/expected pressure ratios. This is because all rods are filled to a pressure of one atmosphere at room temperature regardless of void volume and if void volume and amount of gas remain constant during in-reactor operation, taking into account thermal expansion effects, the pressure ratio will be proportional to the change in absolute temperature of the gas. From Figures 30 and 31 we can also see that pressures from Rods 1 and 5 continued to decrease between June 15 and September 19, but at a slower rate. There is a

FIGURE 30. Rod 1 Pressure History at Zero Reactor Power with the Measured Pressures Normalized to the Expected Pressures Assuming No Sorbed or Fission Gas Release

FIGURE 31. Rod 5 Pressure History at Zero Reactor Power with the Measured Pressures Normalized to the Expected Pressures Assuming No Sorbed or Fission Gas Release

FIGURE 32. Rod 6 Pressure History at Zero Reactor Power with the Measured Pressures Normalized to the Expected Pressures Assuming No Sorbed or Fission Gas Release

25 to 13% decrease after ~2800 MWd/MTM and 4375 MWd/MTM burnup for Rods 1 and 5, respectively. Rod 1 pressures were not obtained beyond December 18, 1975 (~2800 MWd/MTM) because of a malfunction in the pressure transducer.

Rod 6 is significantly different, showing 10 times higher gas pressures at startup than Rods 1 and 5, and 8 times higher than would be expected from the initial one atmosphere fill gas pressure. This pressure decreased by ~40% in the first few hours and then decreased at a more gradual rate until July 20, 1975 (~700 MWd/MTM) at which time the pressure leveled off and remained relatively constant until the long reactor outage began on September 20, 1975. The long outage startup on November 31 shows pressures at zero power to have dropped to or below one atmosphere, the minimum measurable by the pressure transducer. At full reactor power, as shown in Figure 29, pressures were slightly above the one atmosphere limit in a few instances.

Tentative Conclusions from Preliminary Analysis. The pressure data from Rods 1, 5, and 6 in the IFA-431 assembly gave some unexpected results which, at the time of the preliminary report draft (August 1976), were difficult to explain, particularly since the postirradiation results were not available. However, the preliminary analysis of the data did make it possible to come to some very tentative conclusions in that draft but those conclusions will have to be substantiated with the postirradiation results. Those tentative conclusions are presented below.

a) Rods 1 and 5 of IFA-431. An analysis of pressure data from Rods 1 and 5 has shown that:

- Pressures decrease by ~25% to a value lower than expected after the first excursion to power. The possibility of helium adsorption was considered but was found to be a relatively small effect based on UO₂-helium adsorption data from the literature. Pellet cracking as a mode of creating additional open porosity and thus void volume was considered but the magnitude of this effect is unknown.
- Pressures decreased 13 to 25% during irradiation. Reaction of the slow-reacting sorbed gases (nitrogen, carbon monoxide, and carbon dioxide) with the cladding is considered a likely explanation for the gradual pressure decrease. A leak around the thermocouple leads is also considered but no evidence was found to support this possibility. Irradiation entrapment of the helium by knock-on was found to be a small effect based on the model and data given by Lewis.^(33,34)

b) Rod 6 of IFA-432. An analysis of pressure data from Rod 6 has shown that:

- Pressures during and immediately following the first excursion to power were a factor of 10 higher than Rods 1 and 5 and a factor of 8 higher than expected. This may be the result of sorbed gas release (either nitrogen, carbon monoxide, or carbon dioxide); however, very little data exist to support this conclusion.

Mercury immersion density measurements on the fuel and the post-irradiation results will help in evaluating this possibility. Release of gas entrapped in the fabricated porosity during the densification process has been ruled out as a possibility for the high pressures. The possibility of an instrument malfunction has not been ruled out because of other abnormal changes in Rod 6 pressures.

- A rapid 40% decrease in pressures occurred during the first few hours of irradiation. No satisfactory physical phenomena could be found to satisfactorily explain such a large pressure decrease other than the creation of additional open porosity by pellet cracking or some other unknown phenomena.
- A gradual 40% pressure decrease occurred between June 20 and July 20, 1975. A malfunctioning instrument cannot be ruled out. However, fuel densification may also be a cause because the observed pressure drop agrees well with the void volume expected from densification. Reaction of the slower reacting sorbed gases (i.e., nitrogen, carbon monoxide, and carbon dioxide) were also considered as a possible cause for the gradual pressure decrease.
- Pressures dropped significantly to a value near or below the detection limit after the long outage startup on December 12, 1975. No physical phenomena could be hypothesized to explain this large drop in pressures other than an instrument malfunction.

Current Progress on Task A

Progress on Task A during the period January-March 1977 is described below.

Project Management (Task A plus Task B)

Liaison with the NRC representative at the Halden Reactor Project is being continued to 1) obtain irradiation data from IFA-431 and IFA-432, and 2) coordinate postirradiation examination requirements for IFA-431 rods. Technical coordination and planning (Buff Book) of other aspects of the program continues to be funded out of this subtask. Contributions to Fuel Behavior, Cladding Behavior, and PBF review group meetings as well as the RSR Reactor Safety Information Exchange meeting are funded out of this subtask. During this reporting period, two papers^(8,9) were presented by PNL at the Enlarged Halden Programme Meeting on Water Reactor Fuel and Core Performance in March 1977 at Standerstolen, Norway. A summary of a paper⁽¹³⁾ was submitted to the American Nuclear Society for its Twenty-third Annual Meeting in June 1977. Presentations on the status of Tasks A and B were made at the Mid-year Technical Review of RSR-Fuel Behavior Research Branch Programs at PNL in Richland, Washington on January 19, 1977.

Irradiation Tests

As indicated earlier, the two irradiation test assemblies, each containing a cluster of six instrumented fuel rods, are essentially identical in design with one, IFA-431, operating at a linear heat rating of 10 kW/ft (328 W/cm) and the other, IFA-432, operating at 15 kW/ft (492 W/cm). The design parameters and instrumentation for the test assemblies are shown in Table 1. The two assemblies were charged into different channels of the Halden Boiling Water Reactor (HBWR), which is located in Norway. IFA-431 began operation in June of 1975 and was discharged in February 1976 after reaching an average burnup of 372 GJ/kgM (4300 MWd/MTM); the design goal burnup for IFA-431 was 345 GJ/kgM (4000 MWd/MTM). IFA-432 was charged into the reactor in December 1975 and will run until late in 1977. The HBWR is a natural circulation reactor which currently operates at a power level of 12 MW. The reactor operating data are shown in Table 4.

TABLE 4. HBWR Operating Data

Power Level	12 MW
Reactor Pressure	500 psi (34 atm)
Heavy Water Saturation Temperature	464°F (240°C)
Plenum Inlet Temperature	459°F (237°C)
Thermal Flux	$\sim 3 \times 10^{13}$ n/cm ² -sec
Fast Flux (>1 MeV)	$\sim 5 \times 10^{11}$ n/cm ² -sec
Average Fuel Power Density	14.8 W/g

Instrumentation. Test assembly instrumentation includes six vanadium neutron detectors to monitor the neutron flux adjacent to the fuel rods and one cobalt neutron detector to monitor the neutron flux changes during power transients.

Test channel instrumentation includes inlet and outlet channel thermocouples, turbine flow meters, and a fuel rod failure detector. A schematic showing IFA-431 in its test channel is shown in Figure 4. IFA-432 is identical except for an ultrasonic thermometer in the upper end of Rod 2.

Data from the instrumentation except for the pressure monitors is automatically taken every 15 min by a data logging system and stored on magnetic tape. The pressure data are manually taken.

Fuel Rods. The bases for the designs of each fuel rod were discussed earlier. Each fuel rod is instrumented with two temperature sensors to measure the central fuel temperature and with one cladding elongation sensor to detect the time and extent of pellet-cladding interaction. All of the temperature sensors are thermocouples except for one in IFA-432, which is an ultrasonic thermometer (UT). Three rods in each assembly are equipped with a pressure transducer to monitor the internal rod pressure. All rods contain UO₂ pellets enriched to 10 wt% ²³⁵U. The pellets have flat ends and are 12.7 mm (0.5 in.) long.

Determination of Gap Conductance Uncertainty

BNWL-2091,⁽³⁵⁾ a report prepared by PNL in August 1976, describes the error analysis of gap conductance for IFA-431. A paper⁽³⁶⁾ on this subject

was also presented in November 1976. The latest paper, BNWL-SA-6161,⁽⁵⁾ was completed in February 1977; it describes the methodology developed for estimating the reliability of gap conductances deduced from measured centerline fuel temperatures. That methodology is presented below.

The primary objective of the uncertainty study is to estimate the reliability of pellet-cladding gap conductances deduced from measured fuel centerline temperatures. The uncertainty analysis consists of applying a selected method for calculating gap conductance, assigning uncertainty values to the input parameters, and performing linear propagation of uncertainty analyses to determine the uncertainties for the deduced gap conductance.

A flow chart showing the factors which influence the measured fuel centerline temperatures and the deduced fuel rod linear heat ratings is shown in Figure 7. The figure shows the instrumentation errors which affect the fuel rod test data and the physical characteristics which affect the test parameters being monitored. The physical characteristics are those phenomena associated with the fuel, cladding, thermal-hydraulics, and neutron flux which cause the measured parameter to be what it is. The instrument errors are associated with the measurements and readout instruments and in general do not affect the measured parameters.

In the following sections, the method for calculating gap conductance is presented, the input parameter uncertainties are established, the calculational method used to combine these into a gap conductance uncertainty is developed, and an example of the calculations is given.

Method of Determining the Gap Conductance. The $\int K(t)dt$ method is used to calculate the gap conductance. The pellet-cladding gap conductance in a fuel rod is defined by the following equation:

$$h_g = \frac{2q}{\pi(d_i + d_p)(t_s - t_i)} \quad (1)$$

where: h_g = pellet-cladding gap conductance

q = linear heat rating

t_s = fuel surface temperature

t_i = cladding inside surface temperature

d_i = cladding inside diameter

d_p = pellet diameter.

Values for all of the parameters in Equation (1) are known except for the fuel surface temperature, t_s , and the cladding inside surface temperature, t_i . The calculation for t_i is straightforward; t_s is calculated using the $\int K(t)dt$ method.

The linear heat rating of the fuel pellet can be related to the $\int K(t)dt$ by:

$$\int_{t_s}^{t_c} K(t)dt = \frac{qF}{4\pi} \quad (2)$$

where: t_c = fuel center temperature
 t_s = fuel surface temperature
 $K(t)$ = thermal conductivity of the fuel as a function of temperature
(defined as $K(t) = A + Bt + Ct^2 + Dt^3$)
 q = linear heat rating of the fuel
 F = flux depression factor which depends on pellet dimensions and enrichment.

The thermal conductivity data of the fuel as a function of temperature, $K(t)$, is fitted to an equation which is integrated to provide the $\int K(t)dt$. Thus, all of the quantities in Equation (2) are known except t_s , which can be calculated.

The inside surface temperature of the cladding is calculated from Equations (3), (4), and (5):

$$t_i = t_w + \Delta t_f + \Delta t_c \quad (3)$$

where: t_i = cladding inside surface temperature
 t_w = coolant temperature adjacent to point of measurement
 Δt_f = water film temperature drop
 Δt_c = cladding temperature drop.

The water film temperature drop (Δt_f) and cladding temperature drop (Δt_c) are expressed as:

$$\Delta t_f = \frac{q}{\pi d_o h_f} \quad \text{and} \quad (4)$$

$$\Delta t_c = \frac{q \ln (d_o/d_i)}{2 b} \quad (5)$$

where: q = linear heat rating
 d_o = outside diameter of cladding
 d_i = inside diameter of cladding
 h_f = water film heat transfer coefficient
 b = thermal conductivity of cladding.

Once t_s from Equation (2) and t_i from Equation (3) are calculated, the gap conductance, h_g , can be calculated from Equation (1). A flow chart for determining the gap conductance is shown in Figure 33.

In order to calculate the gap conductance uncertainty, the uncertainty associated with each input parameter must be considered and values assigned.

Input Parameter Uncertainties. The most important parameters which affect the calculated gap conductance are associated with the fuel:

- linear heat rating
- thermal conductivity
- centerline temperature
- thermal neutron flux depression.

The linear heat rating is the most influential parameter in calculating the gap conductance and uncertainty because it enters into the calculations three times. It appears in the calculation of the fuel surface temperature, the cladding ID temperature, and the gap conductance. The linear heat rating for each fuel rod is calculated from the measured fuel assembly power.

a) Fuel Assembly Power. At the beginning of the first irradiation cycle, the total power in the test assembly is calibrated thermal-hydraulically under forced convection cooling and is related to the average of the neutron detector currents. After this calibration the assembly is operated under natural circulation cooling, and the assembly power and individual fuel rod linear heat ratings are deduced from the neutron detector currents. The calibration is not repeated.

FIGURE 33. Flow Chart for Determining Gap Conductance

The test assembly inlet ports near the bottom of the assembly as shown in Figure 4 are closed for the forced convection calibration run by activating the calibration valve.

Forced circulation cooling is introduced from the subcooled plenum chamber and boiling is established in the channel. This is shown schematically in Figure 34.

The assembly is operated at constant power which is controlled by the neutron detector readings while the subcooling is varied. The boiling power is computed and plotted versus the measured subcooled power. Figure 35 shows a plot copied from digital computer results. The plots of boiling power versus subcooled power are very close to a straight line. The intersection of the extrapolated plots with the abscissa gives the minimum subcooled power required to suppress boiling in the channel under ideal conditions. The subcooled power is equal to the total channel power which is corrected for the moderator heating effect to give the channel power associated with the assembly.

The accuracy of the assembly power determination depends on the following factors:⁽³⁷⁾

- calibration of inlet and outlet turbine flow meters
- calibration of coolant thermocouples
- temperature profile across the coolant channel where the inlet and outlet thermocouples are located
- influx of heat from the moderator into the coolant channel
- slip factor constancy
- signal to noise ratio
- accuracy of data handling equipment.

The outlet turbine accuracy is very good, better than $\pm 0.5\%$, while the inlet turbine calibration is more doubtful due to the short and complicated inlet section. Comparison with the outlet turbine immediately prior to measurements gives good control, however, and the accuracy obtained is estimated to be better than $\pm 2\%$.

FIGURE 34. Schematic Arrangement for Assembly Power Calibration

FIGURE 35. Power Calibration Data for IFA-431

The calibration of coolant thermocouples is very good, about $\pm 2 \mu\text{V}$. An 18°F (10°C) temperature rise in the channel has an accuracy of $\pm 0.5\%$.

The nonuniform temperature distribution across the inlet to the channel is negligible because there is excellent mixing before the coolant enters the assembly. The distribution across the outlet, however, is difficult to determine as there is little information available. The best estimate is obtained by studying the consistency of the measurements and the degree of agreement between the various calibration methods.

The influence of the heat flux from the moderator is small. It can amount to as much as 10 kW at low channel power and high subcooling, but it is taken into account, so even if some error is involved in determining this heat flux, the overall error contribution is very small, probably less than $\pm 0.5\%$.

The calibration method is based on assuming a constant slip factor during a subcooled power ramp. The results of the measurements give a plot of boiling power versus subcooled power which is very close to a straight line, indicating that the assumption is valid.

The signal to noise ratio does not normally cause any difficulty with sound instruments. The noise is easily filtered out.

There is always full control with the performance of the data handling system, so errors caused in this equipment can be neglected compared to errors from other sources.

Another factor which influences the accuracy of assembly power indication by the beta current neutron detectors is the specificity of the power indicators themselves. Part of the neutron flux registered by these instruments originates in other fuel assemblies, and this portion of the signal will depend on the flux distribution in the core.

Taking the above factors into account, Halden estimates that the in-reactor assembly power measurements have accuracies in the ± 3 to $\pm 5\%$ range.⁽³⁷⁾

A value of $\pm 4\text{--}1/2\%$ was selected for the IFA-431 assembly power accuracy.

b) Neutron Detector Output. Each assembly is equipped with six vanadium, self-powered, beta current neutron detectors to monitor the power in the fuel assembly after the initial thermal-hydraulic calibration. One detector failed in the IFA-431 assembly shortly after startup.

Each vanadium neutron detector is 3.93 in. (100 mm) long and is located so that the center of the detector and the thermocouple junction are located on essentially the same plane.

The neutron detector output is an average of the current generated over the length of the detector. A nonsymmetrical flux gradient or spiked flux along the length of the detector could produce incorrect power generation results. It appears that the axial flux gradients along the neutron detectors are symmetrical and without spikes, so errors from this source should be minimal.

The neutron detectors used in the test assemblies are not calibrated. Their precisions are based on the results of the irradiation of 30 similar type vanadium neutron detectors in the Studsvik R2-0 Reactor in Sweden. The 30 detectors were irradiated in a thermal neutron flux of 1.1×10^{10} n/cm²-sec. The error limits for the outputs of the detectors are estimated to be $\pm 2.5\%$ at a neutron flux of 1.1×10^{11} n/cm²-sec.

In addition to correlating the detector outputs to the neutron flux in the Studsvik Reactor, Halden has conducted long-term tests of similar neutron detectors in the HBWR. According to M. Brakas, B. Söderlund and O. Strindehag of Halden in 1972, these long-term tests have established the detectors as a reliable and accurate instrument without a measurable change in sensitivity at the higher flux levels in the HBWR.

The sensitivities of the test assembly neutron detectors are calculated from the sensitivities of the calibrated detectors and the physical characteristics of the test assembly detectors supplied by the manufacturer. To account for uncertainties in this calculation, $\pm 0.5\%$ is added to the $\pm 2.5\%$ for the calibrated detectors to give an uncertainty of $\pm 3.0\%$ for the test assembly detectors.

The vanadium detectors have a calculated burnup rate of 0.013%/month at a neutron flux of 1×10^{13} n/cm²-sec. Based on this rate, the neutron detector end-of-life (EOL) burnup for IFA-431 is 0.06%. Because of this low value, the neutron detector outputs were not corrected for burnup.

A disadvantage of the vanadium detectors is their slow response time (5.5 min, 0 to 63%) to power changes. Consequently, during up and down power ramps, a correction factor should be considered for the output values.

c) Fuel Rod Linear Heating Rating. The fuel rod linear heat rating is calculated from the following equation:

$$q = CKNC_N \quad (6)$$

where: q = linear heat rating

C = conversion factor which converts assembly power to
individual rod linear heat rating

K = calibration factor: ratio of assembly power to average of neutron detector currents

N = single neutron detector current

C_N = neutron flux tilt correction factor.

The uncertainty associated with the conversion factor, C, takes into account the uncertainties in the dimensions, density, and enrichment of the fuel. This uncertainty is calculated to be $\pm 0.2\%$. The uncertainty associated with N was evaluated as $\pm 3\%$.

The IFA-431 test assembly is located in the outermost ring of the reactor where a significant radial flux tilt exists across the test assembly. This necessitates correcting the output signals from the neutron detectors to the axis of the fuel rods. This is accomplished with the flux tilt factor, C_N . We are assuming a $\pm 2\%$ uncertainty in the flux tilt factor.

The uncertainties associated with the calibration factor, K, and the linear heat rating, q, are developed in the following three paragraphs.

The available data for calculating the uncertainty for q consist of typical values for C, K, N, and C_N along with uncertainty intervals for each factor where the errors are engineering estimates of error levels generally corresponding to maximum limits. The assumptions which are basic to this analysis are:

- The engineering estimates of the error limits are equivalent to three standard deviation limits, i.e., $\pm 5\%$ corresponds to $\pm \left(\frac{3\sigma}{\mu} \right) 100$ where μ is the typical value and σ is one standard deviation.
- The four factors C, K, N and C_N are statistically independent. This assumption is reasonable since K is determined at the initial startup and N is obtained from subsequent measurements. Furthermore, the variability in C arises from measurement errors associated with the dimensions, density, and enrichment of the fuel, and C_N is a flux tilt correction independent of neutron detector output.

By propagation of uncertainties, the 3σ uncertainty for the linear heat rating is

$$U_q = (U_C^2 + U_K^2 + U_N^2 + U_{C_N}^2)^{1/2} \quad (7)$$

where U_C is the engineering percent uncertainty on C, U_K is the engineering percent uncertainty on K, U_N is on N, and U_{C_N} is on C_N . In order to utilize the above relationship, the 3 σ percent uncertainty must be calculated for factor K.

The equation for calculating K for IFA-431 is:

$$K = \frac{Q_{ch}}{[(N_1 + N_2 + N_3)/6] + [(N_4 + N_5)/4]} \quad (8)$$

where Q_{ch} is the calculated assembly power and N_i is the output from the i^{th} neutron detector. If it is assumed that the neutron detectors and the calculated assembly power are all statistically independent, then the above equation can be used along with the propagation of errors to obtain the estimated error limits for K. Utilizing these assumptions, the error limits for K for IFA-431 are given by:

$$U_K = (U_{ch}^2 + \frac{5}{24} U_N^2)^{1/2}. \quad (9)$$

Because one detector failed in IFA-431, the neutron flux at the failed detector location is calculated from the operating detectors as follows:

$$N_6 = N_3 \frac{[(N_4/N_1) + (N_5/N_2)]}{2}. \quad (10)$$

The uncertainty limits equation is:

$$U_{N_6} = \left\{ U_{N_3}^2 + \left(\frac{N_3 N_4}{2 N_1 N_6} \right)^2 (U_{N_1}^2 + U_{N_4}^2) + \left(\frac{N_3 N_5}{2 N_2 N_6} \right)^2 (U_{N_2}^2 + U_{N_5}^2) \right\}^{1/2} \quad (11)$$

Detector No. 6 is located between Rods 4 and 5 at the upper thermocouple location. Consequently, the uncertainty of the linear heat rating for these two rods will be greater than that for the other rods.

d) Centerline Fuel Temperature. Twelve thermocouples are used in IFA-431 for measuring the central fuel temperatures. The thermocouples have grounded junctions with 0.052-in. (1.575-mm) OD, W-22% Re sheaths and W-5% Re/W-26% Re seven-stranded thermocouple wires with beryllium oxide insulators.

The thermocouples were fabricated and calibrated by the Idaho National Engineering Laboratory (INEL). Calibration of the thermocouples over the range of use produces a brittle assembly which is fragile and subject to breakage. Consequently, only one thermocouple, which was not used in the in-reactor test, was calibrated.

The thermocouple was calibrated against a reference thermocouple of bare W-5% Re/W-26% Re and an optical pyrometer as a second reference. The reference thermocouple and the optical pyrometer agreed within 40°F (22°C) up to 4000°F (2204°C), but as the temperature approached 4500°F (2482°C), the two differed more widely. The optical pyrometer was thought to be closer since the 4500°F temperature is above that given in most calibration tables for W-Re thermocouples. The calibrated thermocouple had the following limits of error:

Ambient to 1000°F (538°C)	= ±10°F (5.5°C)
1000°F (538°C) to 4000°F (2204°C)	= ±1% of reading
4000°F (2204°C) to 4500°F (2482°C)	= ±2% of reading.

Irradiation of the thermocouples will have long-term effects caused by the shunting of the EMF's by conduction across the insulators, by transmutations in the thermocouple materials, and by temperature gradients along the thermocouple wires.

The insulator shunting effect was reduced to a negligible level by using beryllium oxide insulators.

The thermocouples used in these tests employ tungsten and rhenium which transmute under neutron irradiation to rhenium and osmium, respectively. These transmutations are the main cause of the reduction in the signal from the thermocouples with irradiation seen by investigators.⁽³⁸⁾ However, there is considerable variation in the magnitude of the effect seen. The reason for this variation is probably that the effect is not confined to changes occurring at the hot junction. The composition changes resulting from the transmutations give rise to inhomogeneities, causing Seebeck EMF generation in the wire situated within the temperature gradient.⁽³⁹⁾ The data from Reference 40 indicate an order of magnitude of about 10% for the downward drift of the EMF at a thermal neutron fluence of $1 \times 10^{21} \text{ n/cm}^2$.

The expected fluence for IFA-431 is about 1×10^{20} n/cm². Based on Reference 40, this fluence would indicate a downward EMF drift of 1% for IFA-431 at EOL. No correction factor is applied to the IFA-431 data. However, an error value of $\pm 1\%$ is assumed for the irradiation effects.

The average error band associated with the instrument measurement and conversion techniques over the range of temperature measurements was estimated in 1976 to be $\pm 0.6\%$, according to M. J. Brakas, T. Stein, S. Saether and B. Aarset of Halden.

Combining the calibration errors of 1% or 2%, the irradiation effects error of 1%, and the instrumentation of 0.6% and rounding off, gives the following estimated total uncertainties for the centerline fuel temperatures:

	<u>% of Reading</u>
1000°F (538°C) to 4000°F (2204°C) =	± 3
4000°F (2204°C) to 4500°F (2482°C) =	± 4

e) Fuel Thermal Conductivity. The fuel thermal conductivities of the three fuel types employed in the test, 95% TD stable, 92% TD stable, and 92% TD unstable, were determined. The method consists of heating one surface of a thin sample disc with a short heat pulse from a laser beam. The heat pulse passes through the sample, and the temperature transient on the back surface of the sample is measured and recorded. The thermal diffusivity is determined from the shape of the temperature-versus-time curve.

The temperature transients were measured optically using a liquid nitrogen cooled, indium antimonide, infrared detector. The signal from the detector was displayed on an oscilloscope and recorded on film. Corrections were made for heat losses, but pulse time corrections were not required.

Each sample was initially heated to 1300 to 1400°C with measurements being taken at approximately 100°C intervals. On cooling, measurements were taken at 200 to 250°C intervals. Subsequently, each sample was heated to 1600 to 1650°C. The samples were held at this temperature for 4 to 5.5 hr. Measurements were made to compare the thermal diffusivity before and after heat treatment. The sample was then cooled to room temperature and again heated to 1530 to 1650°C with measurements being made at 200 to 300°C increments.

The thermal diffusivity, α (cm^2/sec), was calculated from the relationship

$$\alpha = \frac{t_c d^2}{t_{1/2}} \quad (12)$$

where $t_{1/2}$ is the time for the back surface of the sample to reach 1/2 the maximum temperature; t_c is a heat loss correction which is determined from the shape of the time-temperature curve; and d is the sample thickness.

The thermal conductivity, λ ($\text{W/cm}\cdot^\circ\text{C}$), was calculated for each thermal diffusivity data point from the relationship

$$\lambda = 4.186 \alpha \cdot C_p \cdot \rho \quad (13)$$

where C_p ($\text{cal/g}\cdot^\circ\text{C}$) is the heat capacity and ρ (g/cm^3) is the sample density.

The above limited amount of out-of-reactor thermal conductivity and temperature data was analyzed by a computer program which performs least squares solutions of linear equations by orthogonal transformations. The program generates coefficients for a third order polynomial, predicts a true value for the thermal conductivity at the input temperatures, and estimates a 95% upper and 95% lower limit. The thermal conductivity uncertainty intervals employed in this analysis are based on the limits estimated by the program for the out-of-reactor data.

The thermal conductivity uncertainty increases as a function of fuel temperature. The uncertainty interval ranges and temperature ranges for the test fuel rods are listed in Table 5.

It is expected that as the irradiation proceeds, the thermal conductivity of the fuel will decrease. As data from the tests become available, those data which are applicable in ascertaining changes in the fuel thermal conductivity will be analyzed and where appropriate the unirradiated fuel thermal conductivity function will be updated. A plot of UO_2 thermal conductivity as a function of fuel temperature for Bates and Mellinger irradiated UO_2 data and Lyons⁽⁴⁰⁾ unirradiated UO_2 data is shown in Figure 36.

TABLE 5. Fuel Thermal Conductivity Uncertainty Interval Ranges

Rod Number	Fuel Thermal Conductivity Uncertainty, $\pm\%$	Temperature Range, $^{\circ}\text{F}$ ($^{\circ}\text{C}$)
1	1.5 - 3.0	640-1990 (338-1088)
2	1.5 - 3.6	695-2260 (368-1238)
3	1.4 - 2.4	550-1705 (288-929)
4	1.9 - 7.5	1340-3085 (727-1696)
5	1.5 - 2.3	595-2110 (313-1154)
6	1.1 - 2.0	640-2180 (338-1193)

FIGURE 36. Thermal Conductivity of Fuel as a Function of Fuel Temperature for Bates and Mellinger Unirradiated Fuel and Lyons Irradiated Fuel

f) Thermal Neutron Flux Depression. The flux depression factor, F , was taken from calculations presented in Reference 6. The flux distribution is described in terms of the inverse diffusion length for neutrons in the fuel. The cross sections used in the calculation of the inverse diffusion length are average cross sections. For neutron absorption these cross sections were obtained from effective cross sections compiled by Westcott.⁽⁴¹⁾

Because the neutronic values used in the calculation of the flux depression factor are well established, we are assuming an uncertainty of $\pm 5\%$ for the flux depression factor, F .

g) Water Film Heat Transfer Coefficient. Experiments were performed in the SAPHIR swimming pool type reactor at the Institute for Reactor Research in Würenlingen and in SILOETTE at the Center for Nuclear Studies in Grenoble to determine heat transfer coefficients.⁽⁴²⁾

The average uncertainty for the SAPHIR data is $\pm 9.5\%$ and that for SILOETTE is $\pm 15.1\%$. The author did not give reasons for this difference nor were the reasons apparent from the data. A heat transfer coefficient calculated using Nusselt, Reynolds, and Prandtl Numbers was shown by the author to lie within the range of uncertainty.

An uncertainty value of $\pm 15\%$ for the water film heat transfer coefficient was selected.

h) Additional Uncertainties. Additional uncertainties considered are those associated with crud deposition on the cladding surface, bulk water temperature, and cladding thermal conductivity. The uncertainties for the latter two parameters are as follows:

	<u>$\pm\%$ Uncertainty</u>
Bulk water temperature	1
Cladding thermal conductivity	5

Crud deposition on fuel rods irradiated in the HBWR has rarely been observed during postirradiation examination. Crud was found on two assemblies which had a square lattice of fuel rods in a square shroud and on a burnout test which was taken into critical heat flux conditions numerous times. On

fuel rods irradiated in assemblies with circular shrouds (IFA-431 and IFA-432 are in circular shrouds), adherent crud deposits have been virtually absent even to burnups in the 30,000 MWd/MTM range, according to A. Hanevik of Halden in December 1975. In view of this history of crud deposition in the HBWR, we are assuming crud-free surfaces during the irradiation. If postirradiation examination shows significant amounts of crud deposition, appropriate correction factors will be applied to the data.

i) Compilation of Input Parameters and Uncertainties. A compilation of the input parameters and uncertainties used in the analyses is presented in Table 6.

TABLE 6. Input Parameters and Uncertainties for IFA-431

<u>Fuel Assembly</u>	<u>Uncertainty $\pm$ %</u>
Power	4.5
Calibration Factor	4.7
Neutron Detector Currents N_1, N_2, N_3, N_4, N_5	3.0
at location of failed detector, N_6	3.4
Conversion Factor	0.2
<u>Fuel Rods</u>	
Linear heat rating	
Rods 1-6 at lower thermocouple location and Rods 1,2,3 and 6 at upper thermocouple location	5.6
Rods 4 and 5 at upper thermocouple location	6.3
Fuel thermal conductivity	
<u>Rod No.</u>	
1	1.5-3.0
2	1.5-3.6
3	1.4-2.4
4	1.9-7.5
5	1.5-2.3
6	1.1-2.0
Centerline temperature	
Thermocouple	
1000°F (538°C) - 4000°F (2204°C)	3
4000°F (2204°C) - 4500°F (2482°C)	4
Cladding thermal conductivity	5
Dimensions, density, enrichment	0.2
<u>Neutronic</u>	
Thermal neutron flux depression in fuel	5
Thermal neutron flux tilt across test assembly	2
<u>Thermal-Hydraulic</u>	
Water film heat transfer coefficient	15
Bulk water temperature	1
<u>Assembly Instrumentation</u>	
Neutron detectors	3

Method of Calculating Gap Conductance Uncertainty. After the gap conductances are deduced from the test data, their estimated uncertainties are calculated. This section presents the calculational methods employed and an example of their application.

A distinction between variability, due to measurement and other causes, and bias is necessary. For example, the linear heat rating value may not coincide with the true, but unknown, linear heat rating due to two different reasons. First, the process of determining the linear heat rating involves making measurements which are subject to error. If the process were repeated a number of times under the same conditions, a distribution of values would be obtained centered at some average value. This average value does not necessarily coincide with the actual true value. This difference is called the bias. The variability around the average value is the uncertainty as used in the following analysis. Hence, the uncertainty determined for the gap conductance is concerned with the variability described above and does not take into account any possible bias.

The uncertainty analysis is based on a few stringent assumptions. These are necessary in order to make any progress at all due to the lack of actual data concerning the variability of the input variables, e.g., diameters, temperatures and linear heat rating. The assumptions are:

1. For each input variable, not calculated by Equations 1 to 5, the error distribution is Gaussian about a center value μ . Moreover, the uncertainty is adequately described by the relative variance $C^2 = \sigma^2/\mu^2$. Note this implies that the variability increases with increases in the center value, i.e., the percent uncertainty is constant.
2. There is zero covariance between any two of the input variables. In particular, this implies there is no correlation between the measurement errors on the input variables.
3. For the nonlinear functions of the input variables used in Equations 1 to 5, the propagation of errors by first order Taylor series provides an adequate approximation.

4. The distributions of the derived variables, e.g., h_g , are sufficiently symmetric that Assumption 1 above holds for the derived variables.

Assumption 4 deserves additional explanation. The explanation will be given for gap conductance. Suppose that it were possible to conduct a large number of experiments exactly like the IFA-431 test. Furthermore, assume that the necessary measurements needed to calculate the gap conductance are taken at the same point of time, i.e., same burnup, power, etc., for each of the experiments. The gap conductances calculated under these conditions will form a distribution of values. Assumption 4 states that this distribution is approximately symmetric about the true gap conductance value for the conditions of the experiment. All of the assumptions are concerned with this hypothetical experimental framework.

An elementary discussion of the Taylor's series approximation is given in Reference 43. The error propagation for the gap conductance follows the framework described by Jaech.⁽⁴³⁾ In the case of the fuel surface temperature, which is defined implicitly in an integral equation, a little ingenuity enables the variance of t_s to be obtained without an explicit solution. The main idea is to recognize that the variances of the right and left hand sides must be equal. The details are given later.

The notation is in terms of relative variances. Let w and v be variables with standard deviations σ_w and σ_v , means μ_w and μ_v , and covariance σ_{wv} , respectively. The relative variance of v is $C_v^2 = \sigma_v^2 / \mu_v^2$ and the relative covariance of w and v is $C_{wv} = \sigma_{wv} / \mu_w \mu_v$. It is convenient at times to use C_{ww} in place of C_w^2 and σ_{ww} in place of σ_w^2 . The square root of the relative variance C_w is also called the coefficient of variation, i.e., $C_w = \sigma_w / \mu_w$. For the present assume that the information available consists of the necessary means, standard deviations and covariances. Where possible the equations will be in terms of the relative variances and covariances.

Engineering limits, or uncertainties, require additional interpretation and/or assumptions before they are used. For example, what does the statement "The uncertainty of the cladding inside diameter is $\pm 0.03\%$ " mean? One interpretation is that the error distribution is Gaussian and its standard

deviation is $0.0003 d_i/3$ where d_i is the true value. The division by 3 corresponds to the assumption that the uncertainty gives the maximum deviation expected. Moreover, this maximum deviation is interpreted to be equivalent to $\pm 3 \sigma$ on the Gaussian distribution and, hence, includes 99.75% of the distribution. This is the interpretation assumed for the uncertainties quoted in the gap conductance problem.

The five output values of interest are Δt_f , Δt_c , t_i , t_s and h_g . Their relative variances now are derived and shown in Tables 7 to 11.

a) Water Film Temperature Drop - Δt_f

$$\Delta t_f = \frac{q}{\pi d_o h_f} \quad (14)$$

$$C_{\Delta t_f}^2 = C_q^2 + C_{d_o}^2 + C_{h_f}^2 \quad (15)$$

TABLE 7. Water Film Temperature Drop, Δt_f

	d_o	h_f	q	Δt_f
Value	1.2789 cm	14.195 W/cm ² -°C	250 W/cm	4.38 °C
Uncertainty	±0.03%	±15%	±4.6%	±15.67%
Relative Variance	1.00-08*	2.50-03	2.35-04	2.735-03

In this case, the uncertainty in Δt_f is dominated by the uncertainty in the water film heat transfer coefficient with the uncertainty in the cladding outside diameter being inconsequential.

b) Cladding Temperature Drop - Δt_c

$$\Delta t_c = \frac{q \ln(d_o d_i)}{2\pi b} \quad (16)$$

$$C_{\Delta t_c}^2 = C_q^2 + C_b^2 + \frac{C_{d_o}^2 + C_{d_i}^2}{[\ln(d_o/d_i)]^2} \quad (17)$$

*1.00-08 means 1.00×10^{-8}

TABLE 8. Cladding Temperature Drop, Δt_c

	d_o	d_i	b	q	Δt_c
Value	1.2789 cm	1.0909 cm	0.156 W/cm-°C	250 W/cm	40.55 °C
Uncertainty	±0.03%	±0.03%	±3%	±4.6%	±5.49%
Relative Variance	1.0-08	1.0-08	1.0-04	2.35-04	3.35-04

The uncertainties in the thermal conductivity and the fuel linear heat rating are the dominating terms in the cladding temperature drop.

c) Cladding Inside Surface Temperature - t_i

$$t_i = t_w + \Delta t_f + \Delta t_c \quad (18)$$

$$C_{t_i}^2 = \left(\frac{t_w}{t_i}\right)^2 C_{t_w}^2 + \left(\frac{\Delta t_f}{t_i}\right)^2 C_{\Delta t_f}^2 + \left(\frac{\Delta t_c}{t_i}\right)^2 C_{\Delta t_c}^2 + 2\left(\frac{\Delta t_f \Delta t_c}{t_i^2}\right) C_{\Delta t_f, \Delta t_c} \quad (19)$$

where

$$C_{\Delta t_f, \Delta t_c} = C_q^2 - \frac{C_{d_o}^2}{\left[\ln(d_o/d_i)\right]} \quad (20)$$

In this case there is a relative covariance between Δt_f and Δt_c since they are both functions of q and d_o . For the values given in Table 8,

$$C_{\Delta t_f, \Delta t_c} = 2.35-04 \quad (21)$$

TABLE 9. Cladding Inside Surface Temperature, t_i

	Value	Uncertainty	Relative Variance	Contribution to Relative Variance t_i
t_w	238 °C	±0.5%	2.78-08	1.97-08
Δt_f	5.38 °C	±15.67%	2.735-03	6.55-07
Δt_c	40.55	±5.49%	3.35-04	6.88-06
Relative covariance $\Delta t_f, \Delta t_c$				
t_i	282.93°C	±0.88%	8.59-06	

The uncertainty for t_i could be reduced most effectively by reducing the uncertainty in the linear heat rating and the thermal conductivity of the cladding.

d) Fuel Surface Temperature - t_s

The fuel surface temperature is defined implicitly by

$$\int_{t_s}^{t_c} K(t) dt = \frac{qF}{4\pi} \quad (22)$$

where $K(t)$ is a third degree polynomial with the coefficients estimated by linear regression techniques. Although it is theoretically possible to solve for t_s explicitly, the solution is complicated. The uncertainty of t_s may be approximated without an explicit solution. If $K'(t)$ denotes the integrated polynomial, i.e.,

$$K(t) = a + bt + ct^2 + dt^3 \quad (23)$$

$$\text{and } K'(t) = at + \frac{b}{2}t^2 + \frac{c}{3}t^3 + \frac{d}{4}t^4, \quad (24)$$

then the above equation for t_s may be written

$$K'(t_s) = K'(t_c) - \frac{qF}{4\pi} \quad (25)$$

Hence,

$$\text{Var}[K'(t_s)] = \text{Var}[K'(t_c) - \frac{qF}{4\pi}] \quad (26)$$

The propagation of errors can be performed as usual and subsequently solved for $\sigma_{t_s}^2$. This procedure gives

$$c_{t_s}^2 = \frac{X(t_c)' \Sigma X(t_c) - X(t_s)' \Sigma X(t_s) + t_c^2 K(t_c)^2 c_{t_c}^2 + \left(\frac{qF}{4\pi}\right)^2 (c_q^2 + c_F^2)}{t_s^2 K(t_s)^2} \quad (27)$$

where

$$X(t)' = (t, \frac{1}{2} t^2, \frac{1}{3} t^3, \frac{1}{4} t^4), \text{ and} \quad (28)$$

Σ = covariance matrix for regression coefficients of $K(t)$.

If it is assumed that the function $K(t)$ is known, i.e., the coefficients are known, then the calculation simplifies to

$$c_{t_s}^2 = \frac{t_c^2 K(t_c)^2 c_{t_c}^2 + \left(\frac{qF}{4\pi}\right)^2 [c_q^2 + c_F^2]}{t_s^2 K(t_s)^2} \quad (29)$$

Since the function $K(t)$ is determined only once for the IFA-431 experiment, there is a question whether the estimation of the regression relationship contributes to the variability of t_s or introduces a bias in t_s . As an example of the computations, the following information is given.

$$X(t_c)' = (830 \quad 3.445+05 \quad 1.905957+08 \quad 1.186458+11) \quad (30)$$

$$X(t_s)' = (450 \quad 1.0125+05 \quad 3.0375+07 \quad 1.025156+10) \quad (31)$$

$$\Sigma = 10^{-6} \begin{bmatrix} 1.7639 & -7.8583-03 & 9.3653-06 & -3.2519-09 \\ -7.8583-03 & 4.1342-05 & -5.3056-08 & 1.9209-11 \\ 9.3653-06 & -5.3056-08 & 7.1311-11 & -2.6624-14 \\ -3.2519-09 & 1.9209-11 & -2.6624-14 & 1.0170-17 \end{bmatrix} \quad (32)$$

$$X(t_c)' \Sigma X(t_c) = 8.280237-02 \quad (33)$$

$$X(t_s)' \Sigma X(t_s) = 5.475770-02 \quad (34)$$

TABLE 10. Fuel Surface Temperature, t_s

	Value	Uncertainty	Relative Variance	Contribution to Relative Variance of t_s
q	250 W/cm	$\pm 4.6\%$	2.35-04	1.95-04
F	0.85	$\pm 5\%$	2.78-04	1.414-04
t_c	830°C	$\pm 2\%$	4.44-05	7.526-05
$X(t_c)' \Sigma X(t_c)$				1.47-04
$X(t_s)' \Sigma X(t_s)$				-9.59-05
t_s	450°C	$\pm 5.9\%$	3.86-04	

e) Gap Conductance - h_g ($W/cm^2-^{\circ}C$)

$$h_g = \frac{2q}{\pi(d_i + d_p)(t_s - t_i)} \quad (35)$$

$$= \frac{q}{\pi d \Delta t} \quad (36)$$

where

$$d = \frac{d_i + d_p}{2} \quad (37)$$

and

$$\Delta t = t_s - t_i. \quad (38)$$

Then

$$C_{h_g}^2 = C_q^2 + C_d^2 + C_{\Delta t}^2 - 2C_{q,\Delta t} + 2C_{\Delta t,d} \quad (39)$$

with

$$C_{\Delta t}^2 = \frac{t_s^2 C_{t_s}^2 + t_i^2 C_{t_i}^2 - 2t_i t_s C_{t_i t_s}}{(t_s - t_i)^2} \quad (40)$$

$$C_d^2 = \frac{d_i^2 C_{d_i}^2 + d_p^2 C_{d_p}^2}{4d^2} \quad (41)$$

$$C_{q,\Delta t} = -\frac{C_q^2}{\Delta t} \left(\frac{qF}{4\pi K(t_s)} + \Delta t_f + \Delta t_c \right) \quad (42)$$

$$C_{\Delta t,d} = \frac{1}{2} \frac{d_i \Delta t_c}{d \Delta t \ln(d_o/d_i)} C_{d_i}^2 \quad (43)$$

$$C_{t_i,t_s} = \frac{-\frac{qF}{4\pi} (\Delta t_f + \Delta t_c) C_q^2}{t_i t_s K(t_s)} \quad (44)$$

TABLE 11. Gap Conductance, h_g

	Value	Uncertainty	Relative Variance	Contribution to Relative Variance of h_g
d	1.0795	$\pm 0.02\%$	5.00-09	5.00-09
q	250 W/cm	$\pm 4.6\%$	2.35-04	2.35-04
Δt	167.07 °C	$\pm 16.6\%$	3.07-03	3.07-03
q, Δt				1.03-03
d, Δt				1.54-08
h_g	0.441 W/cm ² -°C	$\pm 19.8\%$	4.335-03	

The uncertainty in the fuel surface temperature as well as the uncertainty in the linear heat rating are the largest contributors in the gap conductance uncertainty. Since the linear heat rating uncertainty contributes to the fuel surface temperature uncertainty, considerable gains could be made by reducing its uncertainty.

If the gap conductance is calculated utilizing only the inside cladding diameter, then

$$h_g = \frac{q}{\pi d \Delta t} \quad (45)$$

where $d = d_i$. The relative variance may be obtained using the same equations but with the following two changes

$$C_d^2 = C_{d_i}^2 \quad (46)$$

$$C_{\Delta t, d} = \frac{\Delta t_c}{\Delta t \ln(d_o/d_i)} C_{d_i}^2 \quad (47)$$

Application to IFA-431. The relationship between gap conductance, gap conductance uncertainty, and gap temperature gradient (Δt) at a linear heat rating of 328 W/cm (10 kW/ft) for the six test rods is shown in Figure 23. The upper and lower bounds for gap conductance and gap conductance uncertainty are Rods 3 and 4, respectively. Rod 3 was designed to act as an internal check on the power by having a small gap 0.051 mm (0.002 in.) which assured a high gap conductance. Rod 4 was designed to study the anomalous thermal

behavior of xenon-filled rods and the effects of gap eccentricity. The lower thermal conductivity of the xenon provided a low gap conductance.

It should be noted from Figure 23 that the gap Δt uncertainty is approximately $\pm 55^\circ\text{C}$ for Rods 1 through 5 and $\pm 85^\circ\text{C}$ for Rod 4.

Verification of Fuel Centerline Thermocouple Readings Through Response to Linear Power Decreases

As indicated earlier, part of the IFA-431 irradiation test involved power decreases from approximately 10 kW/ft to 8 kW/ft (peak power) in 20-30 sec. Several of the power decreases were found to be sufficiently linear to permit straightforward analysis of the time-dependent fuel centerline thermocouple response. This study on the relationship between time-varying and steady-state fuel temperature measurements from IFA-431 was presented in a paper⁽⁴⁴⁾ in November 1976 and described in a paper⁽⁸⁾ in March 1977. A more complete exposition of the IFA-431 data and its error analysis is presented in a report⁽⁷⁾ that was just completed.

A major purpose of the IFA-431 test (Table 1) was to reduce the present uncertainty in stored energy calculations by gaining more precise measurements of fuel-cladding gap conductance. That meant that uncertainty on factors that affect gap conductance (such as rod power, fuel thermal conductivity, gap size, and fill gas composition and pressure) had to be minimized. Careful preparation and precharacterization of the fuel was part of this effort; another part was the development of cross-checks on the power and fuel temperature data.

In particular, this study shows that time-varying fuel thermocouple and assembly power measurements provide a cross-check on the accuracy of indicated steady-state fuel temperatures. Good reasons exist for seeking such a cross-check. There is continuous uncertainty in thermocouple performance, due to the possibility of shunting along the length, axial temperature gradient effects, and response change due to radiation effects.

The assembly carried 12 fuel thermocouples and 6 vanadium neutron detectors, in two coplanar sets, as shown in Figure 4. The arrangement of neutron detectors facilitated axial and radial definition of the thermal neutron flux in the assembly.

The assembly also carried a cobalt neutron detector, which has a response time to flux changes on the order of milliseconds and was capable of following and recording rapid relative changes in power. During special power changes, the cobalt detector output was recorded, along with that from all 12 thermocouples, on a fast-scan system that records a complete data set every 3 sec. This was the feature that enabled the cross-check on centerline temperature.

The power changes used to create time-temperature data consisted of power decreases of 20% in approximately 30 sec. These power decreases were accomplished by control rod movement far from IFA-431. The whole reactor appeared to change power quite uniformly, based on the output of several widely-spaced cobalt neutron detectors. A set of three such power drops was run after each operating month. The numbering system used by Halden to designate these power drops, and the datas on which they occurred, are shown in Table 12.

TABLE 12. IFA-431 Power Drops^(a)

<u>Halden Run Number</u>	<u>Date</u>	<u>Approximate Burnup (MWd/MTU)</u>
1	6/22/75	0
3*	6/22/75	
5	6/22/75	
7	6/26/75	0
9	6/26/75	
11*	6/26/75	
13*	8/8/75	1000
15	8/8/75	
17*	8/8/75	
19	9/10/75	2000
21*	9/10/75	
23	9/10/75	

(a) An asterisk (*) marks runs which were sufficiently linear for this study. Six power drops also occurred in December 1975; however, none were sufficiently linear for analysis in this study.

Analytical Approach. The basic assumptions made in our analysis of fuel temperature response to the power decreases are:

- (1) That the power decreases are truly linear.
- (2) That the fuel rod responds as a linear dynamic system to an acceptable approximation.

Both assumptions can be relaxed if a computer code is used to analyze the transients. But then thorough understanding of the results would require thorough understanding of the code and the myriad assumptions implicit within it. Furthermore, estimation of meaningful limits of error for the results would be very complicated. Let us retain Assumptions (1) and (2) and see where they lead us.

Assumption (1) limits the number of acceptable power drops, as indicated in Table 12.[†] Assumption (2) is bound to lead to some bias in the analysis. We will return to this aspect later. First, let us test Assumption (2). We define $T_r = T_o - T_\infty$ as the "output" of our system, where T_o is the measured centerline temperature and T_∞ is the coolant temperature.^{††} The input we take is "q," the linear heat rating at a given thermocouple plane.

During any transient, we write $T_r(t)$ and $q(t)$ as functions of time "t:"

$$T_r(t) = T_{r_i} + \Delta T_r(t) \quad (1a)$$

$$q(t) = q_i + \Delta q(t) \quad (1b)$$

with initial condition $\Delta T_r = \Delta q = 0$ at $t = 0$

where T_{r_i} and q_i are known initial values, $\Delta q(t)$ is a known variation of power, and $\Delta T_r(t)$ is the time variation of T_r (to be determined). The transfer function for the system we write as a function of Laplace variable "s"

$$G(s) = \frac{\Delta T_r(s)^*}{\Delta q(s)^*}$$

[†]The other drops are useful, but only for verification of computer codes.

^{††}We take $T_\infty = 240^\circ\text{C}$ for all cases. Since all transients considered began at or near full reactor power the error involved in this approximation is negligible.

i.e., the ratio of Laplace transforms of output and input. Assumption (2) indicates $G(s)$ is the same at all power levels of interest. A way to test a limiting value of G (as we shall see, the limiting value of interest) is to consider a step increase in power from $q = 0$ to $q = Q$. In terms of Equations (1a) and (1b), $T_{r_i} = q_i = 0$, $\Delta q(t) = Q$. Hence $\Delta q^* = Q/s$, and we find $\Delta T_r(t)$ from the relation

$$\Delta T_r^* = G \Delta q^* = \frac{GQ}{s}$$

and

$$\Delta T_r(t) = L^{-1}(\Delta T_r^*) = L^{-1} \frac{G(s)Q}{s} = Q L^{-1} \left(\frac{G(s)}{s} \right)$$

Since $T_{r_i} = 0$, the limiting value of $\Delta T_r(t)$ as $t \rightarrow \infty$ is the steady-state value of T_r at power level Q :

$$\lim_{t \rightarrow \infty} \Delta T_r = T_r^Q = Q \lim_{t \rightarrow \infty} \left[L^{-1} \left(\frac{G(s)}{s} \right) \right] = Q G_{lim} \quad (2)$$

Thus if Assumption (2) is acceptable, a plot of T_r versus q should be linear, with slope G_{lim} .

Figure 37 is a plot of T_r versus q for Thermocouple No. TF2 near the beginning of life, and is typical of all the IFA-431 thermocouple data. Notice that the plot is quite linear over a broad range of power, including the range of the power drops. This fact is a little deceptive as will be discussed later when we return to the question of bias in the analysis. For the moment let us take Figure 37 as adequate verification of Assumption (2) and proceed.

Now let us consider the fuel thermocouple response to a linear decrease in power. Actual data from Run 3 are presented in Figure 38, where we show the cobalt detector output (normalized to its initial value) and the concurrent temperature signal from TF2, Rod 1. Notice that the temperature assumes a linear decrease with time, after transient terms have died away. We can arrive at an expected value for the slope of this linear response: we let $q(t) = q_i - bt$, where b is a constant with units of W/m/sec. Thus $\Delta q(t) = -bt$, in terms of Equations (1a) and (1b). Then

FIGURE 37. T_r Versus q for TF2, Rod 1 (near beginning of life)

FIGURE 38. Power and Temperature Data for TF2, Rod 1, Run 3

$$\Delta T_r^*(s) = G(s) L(\Delta Q) = -G L(bt) = \frac{-bG}{s}$$

Now the Laplace transform of the derivative of $\Delta T_r(t)$ is just

$$L\left(\frac{d\Delta T_r}{dt}\right) = s\Delta T_r^* - \Delta T(0) = s\Delta T_r^*.$$

Hence in terms of $G(s)$,

$$L\left(\frac{d\Delta T_r}{dt}\right) = s\left(\frac{-bG}{s^2}\right) = \frac{-bG}{s}$$

The limiting value of $\frac{d\Delta T_r}{dt}$ is

$$\lim_{t \rightarrow \infty} L^{-1}\left(\frac{dT}{dr}\right) = \lim_{t \rightarrow \infty} \left[L^{-1}\left(\frac{-bG}{s}\right) \right] = -b G_{lim} \quad (3)$$

where G_{lim} is defined as before. But from Equation (2) we know that

$$T_{r_i} = q_i G_{lim} \quad (4)$$

One is tempted to divide (3) by (4), eliminating G_{lim} and obtaining

$$\frac{1}{T_{r_i}} \frac{d\Delta T_r}{dt} = \frac{-b}{q_i} \quad (5)$$

Recognizing

$$\frac{d\Delta T_r}{dt} = \frac{dT_r}{dt}$$

and

$$-b = \frac{d\Delta q}{dt} = \frac{dq}{dt}$$

we have

$$\frac{1}{T_{r_i}} \frac{dT_r}{dt} = \frac{1}{q_i} \frac{dq}{dt}$$

or

$$\frac{dT_N}{dt} = \frac{dq_N}{dt} \quad (6)$$

where T_N and q_N are T_r and q normalized to their initial values. Figure 39 presents the data from Figure 38 replotted in normalized form. One can see rather good conformance to Equation (6). If data plotted as in Figure 39 do not result in parallel lines, we conclude the initial temperature reading, T_{0i} , is in error. Furthermore the amount by which T_{0i} must be adjusted to bring the curves parallel is an estimate of the error in T_0 ; i.e., T_0 reading + estimated error in T_0 = estimated true value for T_0 . Furthermore, a limit of error on the estimated true value of T_0 can be obtained from the limit of error on the difference in slope between the least squares fit lines for normalized power and temperature. The process for finding that limit of error is very straightforward.

Now let us return to the question of bias in the analysis. As we said, Figure 37 is a little deceptive. It indicates that the quantity $G_{lim} = \frac{T_r}{q}$ (i.e., the slope of the plot) is 2.65×10^{-2} ($^{\circ}\text{C}/\text{W}/\text{m}$) over a large range. This quantity may be viewed as the total thermal resistance between fuel centerline and coolant. As such, it is the sum of the resistances between fuel centerline and fuel surface, plus resistance from fuel surface to coolant. That is,

FIGURE 39. Normalized Data for TF2, Run 3

$$R_{TOT} = \frac{T_0 - T_\infty}{q} = \frac{T_0 - T_{FS}}{q} + \frac{T_{FS} - T_\infty}{q} = R_F + R_S$$

where T_{FS} is the fuel surface temperature. We have used a small computer code to evaluate T_{FS} from the relation

$$\int_{T_{FS}}^{T_0} K(T) dT = \frac{q}{4\pi} \left[\frac{\int_{r_0}^{r_{FS}} \frac{dr}{r} \int_{r_0}^r Q(r') r' dr'}{1/2 \int_{r_0}^{r_{FS}} Q(r) r dr} \right]$$

where $K(T)$ is the fuel thermal conductivity (measured at PNL) and $Q(r)$ is calculated from the BRT code (BNWL-1434).⁽⁴⁵⁾ Variables r_0 and r_{FS} refer to the inner and outer fuel radii, respectively.

We then calculated and plotted R_{TOT} , R_F , and R_S as a function of q from the data in Figure 37. The results are presented in Figure 40. As one can

FIGURE 40. Thermal Resistance Versus Linear Heat Rating (q) for Rod 1 of IFA-431 at the TF2 Plane

see, changes in R_F and R_S are such as to cancel and produce a deceptively constant R_{TOT} . Over the range of the power drop, R_S changes by -14% relative and R_F changed by +9%. For the conditions at the beginning of Run 3 for TF2, we have simulated a perfectly linear power drop, and find a bias in temperature slope of -6% (cf. Figure 41).

The next section provides summary information on data and analysis for the five linear power drops marked in Table 12. Examples of the estimated bias are given.

FIGURE 41. Plot of Comparative Computer Runs for Rod 1 of IFA-431 at the TF2 Plane

Data and Discussion. As stated earlier, an estimate of the true value of the initial centerline temperatures can be obtained from the slope of the normalized temperature plot. As an example, consider the data from TF2, Run 3, presented in the previous section. The normalized power slope for Run 3 was -0.938%/sec and the normalized temperature slope for TF2 was -0.870%/sec. We noted previously that $\frac{dT_N}{dt}$ should equal dq_N/dt , i.e., the slopes should be equal.

Since

$$dT_N/dt = \frac{1}{T_{r_i}} \frac{dT_{r_i}}{dt} = \left(\frac{1}{T_0 - T_\infty} \right) \frac{dT_r}{dt},$$

we conclude that the quantity $(T_0 - T_\infty)$ must be in error, and in fact must be decreased by the factor $\frac{0.870}{0.938}$. The original value of T_{0_i} for this case is 1079°C. The difference between T_{0_i} and its estimated "true" value is

$$(1079 - 240) \left(1 - \frac{0.87}{0.938} \right) = 61^\circ\text{C}.$$

Table 13 presents these differences for 8 of the 12 thermocouples in the assembly for all five linear runs. Data from the four thermocouples in Rods 2 and 4 are excluded because the thermal inertia of these rods prevented them from attaining quasi-steady-state slopes during most of the runs.

TABLE 13. Estimated True Centerline Temperature
Minus Initial Thermocouple Reading (°C)

IFA-431 Thermocouple Number	Run 3	Run 11	Run 13	Run 17	Run 21
TF 1	-16	+65	+14	-43	-34
TF 2	-61	+36	+2	-74	-87
TF 5	+26	+103	+12	-9	-1
TF 6	+71	+167	+34	-9	-55
TF 9	-24	+54	+26	-10	-8
TF 10	-42	+161	+94	+18	+68
TF 11	-4	+78	+33	-28	-14
TF 12	-62	+139	+76	-8	-66

As an example of the possible bias in this data, we consider the results of Runs 3 and 11, which occurred on the 22nd and 26th of June, 1975. Computer simulations of the initial conditions plus a perfectly linear power decrease yield the bias estimates listed in Table 14. A version of the GAPCON-3 code⁽⁴⁹⁾ was employed for these calculations.

One can correct the temperature slopes from Runs 3 and 11 for this estimated bias and recalculate the difference between true and actual initial thermocouple readings. Again using TF2 from Run 3 as an example, we correct the temperature slope from -0.870 to $-0.870(1.06) = 0.922$, since the bias is -6%. Then the difference between true and actual reading is $(1079-240)$ times $\left(1 - \frac{0.922}{0.938}\right) = 14^\circ\text{C}$. Table 15 presents all the corrected temperature slopes for Runs 3 and 11, and the corrected temperature differences.

Now the corrected temperature slopes plus the power slope for each run constitute a set of nine independent estimates of the true power slope for that run. Their average is probably the best estimate. (In the case of Run 11, the power slope is so low as to be excluded from the average.) Table 16 presents the temperature differences recalculated using the average slopes rather than the power slopes. Similar analysis applied to the other three runs results in even better agreement between the thermocouple readings and their estimated true values. Run 11 was definitely the worst case.

TABLE 14. Estimated Bias in Normalized Temperature Slope for IFA-431 in June 1975

<u>Rod Number</u>	<u>Thermocouple Number</u>	<u>Estimated Relative Bias in Slope (%)</u>
1	TF 1	-1
	TF 2	-6
3	TF 5	-1
	TF 6	-2
5	TF 9	-3
	TF 10	-10
6	TF 11	-2
	TF 12	-9

TABLE 15. Corrected Slopes and Temperature Differences for Runs 3 and 11

IFA-431 Thermocouple Number	Normalized Temperature Slope (%/sec)		Temperature Difference (°C)	
	Run 3	Run 11	Run 3	Run 11
TF1	0.923	0.649	-10	+71
TF2	0.922	0.643	-14	+91
TF5	0.961	0.720	+11	+87
TF6	0.990	0.737	+36	+124
TF9	0.930	0.651	-5	+74
TF10	0.983	0.751	+43	+270
TF11	0.951	0.665	+9	+92
TF12	0.952	0.729	+13	+236
Power Slope	0.938	0.583		
Average Slope	0.950	0.693		

TABLE 16. Recorrected Temperature Differences

IFA-431 Thermocouple Number	Temperature Difference (°C)	
	Run 3	Run 11
TF1	-18	-40
TF2	-25	-64
TF5	+5	+18
TF6	+27	+44
TF9	-13	-39
TF10	+31	+78
TF11	+1	-26
TF12	+2	+49

Conclusions. An independent estimate of the fuel centerline temperature can be obtained from the thermocouple response to a linear power change. This estimate is independent of the absolute initial values of temperature or power. It is simultaneously independent of any prior knowledge of gap conductance or fuel thermal conductivity. Thus it provides a truly independent cross-check on the thermocouple reading.

This independent cross-check is, however, subject to possible bias, from at least two sources:

- 1) The assumption that fuel rod responds as a linear system, with a constant transfer function determined by the effective values of gap conductance, fuel thermal conductivity, etc., applicable to the beginning of the transient.
- 2) A bias in the estimate of the power slope.

The method is so straightforward, and leads to such a simple error analysis, as to warrant further experimental work to reduce the bias from source (2) and quantify the bias from source (1). The report on this study, BNWL-2189,⁽⁷⁾ is to be published soon and it provides a more complete exposition of the IFA-431 data and its error analysis.

Recommendations for Future Work. BNWL-2189⁽⁷⁾ points out the kind of experimental work that could be done to improve the method and those recommendations are described below.

There are several ways the results of this study could be improved or extended in future experiments. We will discuss only two aspects here: 1) ways to narrow the confidence limits on centerline temperature and 2) ways to derive estimates of the gap conductance.

- 1) Narrowing the Confidence Limits. From the experience so far it is apparent that the overall rate of 0.67%/sec is nearly ideal for a power decrease; steeper rates produce too few data points in the linear region, and shallower rates produce power histories that are decidedly nonlinear. It is also apparent that increasing the number of data points taken per run will not decrease the residual standard deviation,

which determines the confidence limits. Thus the only avenue open for narrowing the confidence limits is to make the power decreases more truly linear, which means altering the method by which they are achieved. This could probably be done in the helium-3 rig in the HBWR. It could also be done by surrounding the assembly by a rotatable shroud (similar to the shroud on IFA-429). The shroud could be tapered or graded in composition around its circumference, such that as it rotates it casts a linearly increasing shadow to neutron radiation across a rod site. Juxtaposition of a cobalt detector near the rod site would adequately determine the variation of the flux at the rod site since it is the slope of the power that is important, not its instantaneous absolute value.

- 2) Deriving Gap Conductance Values. The data in this report indicate that the limiting value of the power-to-centerline-temperature transfer function does not vary typically more than ~5% over a power range of 20% and its variation is typically less than 3% over a power range of 10%. This is not a new or startling result. But it does point to a way to independently estimate the gap conductance. If a rapid but accurately calibrated change in power were made (say 10% in less than 5 sec) then the centerline temperature behavior about 20 sec after that change would be exponential with the time constant directly related to the ratio of the fuel-to-coolant conductance divided by the fuel conductivity. This independent check on both conductivity and conductance could be achieved by the same rotatable shroud described above, by simply making a rapid twist. Again, the fact that the neutron detector recording the power change is not exactly at the rod site will not affect the results, since it is the ultimate relative value of the power change that is important, not the instantaneous absolute value of the power.

Note that the shroud could be graded axially rather than circumferentially, and lowered past a thermocouple elevation at constant speed to produce linear and rapid power variations.

Effects of Pellet Eccentricity and Fill Gas Composition on Gap Conductance of UO₂ Fuel Rods

Two of the many aspects of the IFA-431 and IFA-432 tests, the effects of 1) fill gas composition and 2) eccentric fuel-cladding gaps on gap conductance and its uncertainty, are under study. During the January-March 1977 reporting period, results from this study for beginning-of-life conditions for IFA-431 were presented in a paper⁽⁹⁾ and a summary⁽¹³⁾ and are described below.

Previous experiments have indicated that gap conductances are not as sensitive to different fill gas compositions as some models would predict. As stated earlier, some investigators^(24,27,28,30) have reported that large fractions of xenon must be added to helium-filled rods before significant changes in gap conductance can be detected. Another uncertainty exists pertaining to the effects of asymmetric pellet-cladding gap geometries on fuel temperatures and gap conductances.

One rod in each fuel assembly (Rod 4, Figure 20) was designed to evaluate the effects of xenon and pellet eccentricity. The rod was backfilled with xenon at room temperature and one atmosphere. Instrumented portions of the rod were mechanically constrained with oversized pellets and molybdenum rods to ensure a concentric geometry in the upper portion of the rod and an eccentric geometry in the lower portion. Flats were ground on the oversized pellets to allow axial communication of the fill gas within the fuel rod.

Analysis. IFA-431 experienced its first rise to power on June 8, 1975. The temperature history of Rods 1, 3, and 4 is shown in Figure 42.

As the assembly rose to power, higher-than-usual fuel temperatures occurred in Rod 4 due to the xenon fill gas. The radial thermal expansion of the oversized pellets in Rod 4 caused them to contact the cladding at relatively low power. With further increases in power, the smaller-diameter pellets in Rod 4 expanded axially against the oversized pellets. The cladding deformed, resulting in an operating radial hot gap nearly half that calculated from free thermal expansion of the smaller pellets alone. This can be seen in the lower half of the Rod 4 curves in Figure 42.

FIGURE 42. Startup Temperature History for IFA-431

When the axial thermal expansion forces overcame the friction forces between the oversized pellets and cladding in Rod 4, the fuel column slipped axially with respect to the cladding. The sudden increase in the gap size resulted in an accompanying increase in fuel centerline temperature, also shown in Figure 42.

The axial forces quickly reached equilibrium and the cladding began deforming again (Figure 43). The nearly vertical high-temperature sections of the Rod 4 curves in Figure 42 represent a power plateau lasting approximately 12 hr. During this time, axial slipping, cladding recovery, and plastic flow of the fuel allowed the gap to widen and the temperature to rise.

Because the fuel at this time was in its most intact operating condition, the data from Rod 4 can serve as a useful boundary condition for fuel relocation and pellet-cladding interaction (PCI) studies. Fuel temperatures are

FIGURE 43. Longitudinal Cross Section of Rod 4 Operating at a Linear Heat Rating of 200 W/cm

very sensitive to changes in gap dimensions in a rod backfilled with a gas of such low thermal conductivity. In order to complete the original Rod 4 objectives and provide a basis for further study, it was necessary to further quantify the operating gap size.

Determination of Gap Size for the Concentric Case. Figure 44 is a flow chart of the analysis procedure. All available pre-irradiation characterization data were input for the analysis. The general approach was to build a tool to quantify the gap sizes.

FIGURE 44. Flow Chart of Analysis Procedure

The operating gap conductances and associated uncertainties were calculated with HGAP, a code written for the uncertainty analysis⁽³⁵⁾ of these tests. This code uses the $\int K(T)dT$ method to solve for the fuel surface temperature, calculates the gap conductance, then applies these in a first order Taylor series propagation of errors routine.

Two codes were used to establish a basis for concentric conditions with no PCI: GAPCON-THERMAL-2⁽⁴⁾ and TRUMP.⁽¹¹⁾ TRUMP is a finite difference heat transfer code that can accommodate complex three-dimensional geometries and temperature-dependent material properties.

The development of GAPCON-3⁽¹⁰⁾ proceeded simultaneously. Rod 4 was used to test the thermomechanical models of this code. Although GAPCON contained a first approximation fuel creep package and no fuel-cladding axial slippage routine at the time of these calculations, the temperature and geometric results were quite encouraging. The code verified the cladding deformation for Rod 4 and closely simulated the mechanical response of Rod 3.

Determination of Gap Size for the Eccentric Case. The concentric solution was used as a first estimate of the gap size for the eccentric case, but it was necessary to determine the amount of contact between fuel and cladding that occurred in this case. Three more variables were added to the gap size variable of the concentric case:

- the azimuthal angle of contact between fuel and cladding,
- the pressure of contact and resulting solid-solid contact conductance, and
- the effective distance for conduction through the gas in the area of contact.

The basic problem of the eccentric case is the lateral force equilibrium of an asymmetrically loaded, statically indeterminate column with thermally dependent geometry⁽⁴⁷⁾ (see Figure 45). A three-dimensional finite element or finite difference model would be very large and still include uncertainties in the mechanical properties and surface profiles of the Zircaloy cladding and UO_2 fuel pellets. At the heart of the problem is the interface between

mechanical and thermal models: the lateral force between cladding and fuel is pressure times area. In the concentric case the contact area of the pellet and the pressure may be found easily. The contact area is difficult to define in the eccentric case, thus, there is an unknown pressure term in the solid-solid contact conductance model and in the equation for the effective distance for conduction through the gas.

FIGURE 45. Cutaway View of the Eccentric Portion of Rod 4

The eccentric problem was solved using the steady-state points on the power plateau of the Rod 4 curves in Figure 42. The constant temperatures at the ends of the curves indicated that the gap sizes had approached stable value for these thermal conditions. GAPCON-3 calculations using a complete axial model of Rod 4 predicted an effective 0.030-mm (0.0012-in.) radial hot gap for the eccentric case during the power plateau at 145 W/cm. TRUMP was used to establish the eccentric contact conditions shown in Table 17. There was good agreement between data for June 8 and calculations, establishing a basis for further calculations.

By assuming that the cladding deformed in a similar manner over both the eccentric and concentric pellet sections, iterations were then made for

TABLE 17. Solutions for Eccentric Portion of Fuel Column^(a)

Power, W/cm	Data		Calculations						
	Date in 1975	Fuel $\bar{Q}$ Temp, °C	Fuel $\bar{Q}$ Temp, °C	H_{solid} W/cm ² -°C	Contact Conditions				
					Included Angle, Deg.	Pressure		Radial Hot Gap	
						psi	kg/cm ²	mils	mm
145	June 8	1150	1152	0.0712	100	705	49.5	1.20	0.030
200	June 20	1285	1255	0.0084	168	83	5.8	0.73	0.019
200	June 20	1285	1277	0.1381	100	1314	92.4	0.80	0.020
200	June 20	1285	1336	0.0712	120	705	49.5	0.90	0.023

(a) $\bar{Q}$ = Centerline, H_{solid} = Pellet-Cladding Contact Conductance

the eccentric case. The target was the observed 1285°C eccentric fuel centerline temperature at 200 W/cm during the second power ramp of IFA-431. This ramp occurred on June 20, 1975, after an 11-day shutdown. Using a three-dimensional TRUMP model, a range of solutions was found varying from low contact pressure and high contact area to high pressure and low area (see Table 17). The high pressure was assumed to be limited by the maximum calculated value of contact pressure for the oversized pellets (approximately 1400 psi).

Results. Figures 46 and 47 show the fuel centerline temperatures and gap conductances, respectively, versus gap size for the concentric and

FIGURE 46. Fuel Centerline Temperature Versus Hot Gap as a Function of Gap Geometry for a Xenon-Filled Rod. Rod 4 and Rod 3 Operated at 200 W/cm.

FIGURE 47. Gap Conductance Versus Hot Gap as a Function of Fill Gas for a Xenon-Filled Rod. Rod 4 and Rod 3 Operated at 200 W/cm.

eccentric cases of xenon-filled rod. These curves represent beginning-of-life (BOL) conditions and were calculated with a continuous fuel pellet model.

The dashed lines in Figures 46 and 47 show the effects of eliminating the fuel and cladding roundnesses from the calculations, but still using the roughnesses. The nearly vertical portions of the solid curves between 0.25 and 0.35 mils (0.006 to 0.009 mm) show the expected effects of increasing pellet-cladding contact pressure.

Figure 47 shows that there is an order-of-magnitude difference between the gap conductances of xenon- and helium-filled rods with comparable operating hot gap sizes. Pellet-cladding eccentricity in the xenon-filled case (Rod 4) increases the average azimuthal gap conductance by 25 to 50% in the range of these data. When azimuthal heat flow was ignored for a particular fuel centerline temperature, the gap conductance was decreased by 7-24%, but the fuel volumetric averaged temperature remained nearly constant due to the temperature distribution (Table 18). Thus, the thermal time constant of the rod, $\alpha = HA/\rho VC_p$, may be underestimated when based on averaged thermal properties.

TABLE 18. Effects of Calculational Method
in the Eccentric Xenon Case

Case	Fuel Centerline Temperature (°C) for Operation at 200 W/cm	Pellet-Cladding Gap Conductance, (W/cm ² -°C)		Volumetric Average Fuel Temperature (°C)	
		Symmetric	Asymmetric	Symmetric	Asymmetric
1	1255	0.1070	0.1146	1046	1053
2	1277	0.1040	0.1293	1065	1088
3	1336	0.0970	0.1162	1114	1128

Uncertainties in Experimental Geometry. Uncertainties in the experimental geometry were investigated under the following classifications:

- axial heat conduction due to the oversized pellets,
- effects of fuel and cladding roundness and roughness on small gap sizes,
- boundary cases representing the effects of circumferential cool-down cracks in the pellets.

The code TRUMP was used for these calculations. The results are summarized in Table 19. All are compared to concentric results at the proper gap sizes. The axial model used is shown in Figure 43. The roundness check used a stepwise model of pellet and cladding surface waviness with an amplitude of ± 0.00015 in. (0.0038 mm), azimuthal increments of 24° , and a mean gap of 0.00065 in. (0.017 mm). The circumferential crack models consisted of moving the shown percentages of the hot gap to 75% of the fuel radius to simulate cracks formed during cool down.

TABLE 19. Uncertainties in Experimental Geometry^(a)

Case	Radial Hot Gap		T_{CL} °C	H_{gap} W/cm ² -°C	% Change from Concentric Case	
	mils	mm			T_{CL}	H_{gap}
Axial	0.85	0.022	1556	0.0738	+ 1.7	- 5.4
Roundness	0.65	0.017	1328	0.0958	- 3.8	+ 3.0
13% Crack	0.70	0.018	1455	0.0906	+ 2.5	- 3.0
50% Crack	0.40	0.010	1349	0.1311	+ 20.5	- 0.1

(a) T_{CL} = Centerline Temperature, H_{gap} = Gap Conductance

The results generally compare well with the temperature and gap conductance uncertainties of $\pm 3\%$ and $\pm 11\%$, respectively. The axial case showed only small deviations. The temperature change in the roundness case was slightly higher due to the small gap size investigated. Roughness was not a significant factor until pellet-cladding contact occurred (Figures 46 and 47). The 13% and 50% circumferentially cracked pellet cases, representing cool-down

cracks, showed varying results (Table 19). The small crack (0.0025 mm wide) showed little change from the reference concentric case, but the temperature change of the 50% crack (0.01 mm wide) case was large. The small change in gap conductance for the 50% crack case was due to the fuel surface temperature remaining nearly unchanged from the reference concentric case. The simulated crack acted as a thermal barrier affecting only the interior temperature distribution. According to M. D. Freshley in February 1977, this is contrary to the results of other investigators, who found no significant thermal changes due to a fabricated annular gap within a duplex pellet assembly. Note that the emissivity of the fuel is not well characterized at the interior fuel temperatures of the xenon-filled rod, where radiation heat transfer should dominate.

When fuel-cladding slippage is eliminated from the analysis, the change in the fuel centerline temperature for the concentric xenon case from June 8 to June 20 was -20°C at 212 W/cm. Considering the sensitivity of fuel temperatures to gap size, only small amounts of fuel relocation could have occurred.

Uncertainties in Calculations. Uncertainties in the radial hot gap sizes can be found by projecting the fuel centerline temperature plus the uncertainty onto the desired curve and then moving from those points down to the abscissa. Thus a temperature of $1490^{\circ}\text{C} \pm 3\%$ corresponds to a 0.020 ± 0.0015 mm (0.0008 ± 0.00006 in.) radial hot gap. The uncertainties in radial hot gap sizes deduced by this method are approximately $\pm 8\%$ for the range studied. Uncertainties in the deduced gap conductances for the helium and both xenon cases were calculated at $\pm 50\%$ and $\pm 11\%$, respectively.⁽³⁵⁾ These data are consistent with the reported thermal conductivity values for helium and xenon.

Conclusions. The following conclusions can be drawn from this analysis:

- Quantification of hot gap size is necessary to assess the effects of fill gas composition and eccentricity on gap conductance.
- The helium-filled concentric versus xenon-filled concentric cases show that the ratio of their respective gap conductances is proportional to

the ratio of the thermal conductivities of the associated gases, indicating that the gas properties presently used in the GAPCON-THERMAL codes are correct.

- Eccentricity in the xenon case increases the gap conductance by 25 to 50% at 200 W/cm when radial hot gap sizes are less than 0.001 in. (0.025 mm). This is thermally equivalent to a helium-filled rod with a radial hot gap of approximately 0.012 in. (0.305 mm).

Fuel Rod Elongation Measurements from IFA-431 and IFA-432 at Low Burnup

The evaluation of the elongation behavior of helium-filled fuel rods in IFA-431 and IFA-432 is presented in a preliminary report⁽¹²⁾ completed in March 1977. That evaluation is also described below.

The cladding elongation behavior over the 0-173 GJ/kgM (0-2000 MWd/MTM) burnup range of five rods in each assembly, all of which were backfilled with helium at room temperature and one atmosphere pressure, was analyzed. The sixth rod in each assembly which was designed to study the effects of xenon fill gas and gap eccentricity on irradiation behavior was discussed in the previous section.

Description of Irradiation Tests

a) Test Assemblies. Design information on IFA-431 and IFA-432 is listed in Table 1. As indicated earlier, IFA-431 began operation in June of 1975 and was discharged after reaching an average burnup of 372 GJ/kgM (4300 MWd/MTM) in February of 1976. IFA-432 was charged into the reactor in December 1975 and will run until late in 1977.

The spatial arrangement of the temperature sensors, fuel and neutron detectors relative to the reference design axial neutron flux distribution are shown in Figure 48. The location of each test assembly in the reactor core and fuel rod orientation are shown in Figure 49.

The test assemblies are designed so that irradiated fuel rods can be replaced with noninstrumented fresh rods. It is planned to remove three rods from IFA-432 at a burnup of approximately 345 GJ/kgM (4000 MWd/MTM) and replace them with fresh noninstrumented rods which will be irradiated to a goal burnup of 1380 GJ/kgM (16,000 MWd/MTM).

FIGURE 48. Arrangement of Temperature Sensors, Neutron Detectors, and Fuel Relative to Reference Axial Thermal Flux Profile, IFA-431 and 432

HBWR IV CORE LOADING NO. 19 DATE NOV - 75

- CONTROL ROD (CS 19)
- INSTRUMENTED FUEL ASSEMBLY (IFA-431)
- STANDARD THIRD CHARGE ASSEMBLY

FIGURE 49. Location of Test Assemblies in the Reactor and Fuel Rod Orientation

b) Fuel Rod Assembly. The fuel pellets were fabricated and characterized at Battelle, Pacific Northwest Laboratories, Richland, WA. The cladding was purchased from Sandvik Special Metals Corporation, Kennewick, WA. The pellets and cladding were shipped separately to Halden, Norway, where the instrumented fuel rods were assembled and irradiated.

c) Fuel Rods. Each fuel rod was instrumented with two temperature sensors to measure the central fuel temperature and with one cladding elongation sensor to detect the time and extent of pellet-cladding interaction. All of the temperature sensors were thermocouples except for one in IFA-432, which was an ultrasonic thermometer (UT). Three rods in each assembly were each equipped with a pressure transducer to monitor the internal rod pressure. All rods contained UO_2 pellets enriched to 10 wt% ^{235}U . The pellets had flat ends and were 12.7 mm (0.5 in.) long. A schematic arrangement of the fuel rods is shown in Figure 50.

d) Fuel Pellets. Fuel pellet characterization is described in BNWL-1988⁽¹⁾ and includes analytical and density measurements as well as microstructural and thermal studies.

The analytical measurements included weight percent uranium, oxygen-to-uranium ratio, isotopic composition, off-gas and ppm carbon, nitrogen, fluorine, chlorine, H_2O , and trace impurities.

Density measurements were made using both geometric and liquid immersion techniques. Geometric measurements were based on diameters measured at three locations on each pellet, on lengths at two locations, and on one weight measurement. The liquid immersion technique using water as the suspension medium was used to determine bulk density on a selected number of pellets.

Microstructural characterization studies included determining grain size, pore distribution, and the surface condition for each type of fuel. Both optical and scanning electron microscopy (SEM) techniques were used.

Thermal studies included measuring the thermal diffusivity of the fuel and performing resintering tests to determine the propensity of the fuel types for densification and to confirm the correlation between irradiation and

FIGURE 50. Schematic Arrangement of Instrumented and Noninstrumented Fuel Rods Irradiated in IFA-431 and IFA-432 (all dimensions in mm)

thermally induced densification. The thermal diffusivity was determined using a laser-pulse technique from 100°C to 1600°C. The thermal conductivity was calculated from the measured thermal diffusivity and from known heat capacity.

The fuel pellets were outgassed at Halden prior to irradiation by drying in air at 125°C for 20 hr followed by an 8- to 10-hr room temperature hold period in vacuum after the loaded pins were placed in the welding chamber.

Postulated Pellet-Cladding Interaction Mechanism. The assembled fuel rods start their journey with the fuel pellets in contact with the cladding. The pellets are loaded into the cladding, the compression springs installed, and the closure welds made while the rods are in the horizontal position. By the time the fuel rods reach the reactor and are loaded, most pellets probably are still in contact with the cladding.

During the first ramp to power, those pellets in contact with the cladding will remain so because of the axial spring load on the stack. Also, because of the axial spring load, the contact force between pellets and cladding will increase as the pellets radially expand. This will produce an asymmetric temperature distribution across the pellet. The pellet surface temperature on the large gap side will be higher than the temperature at the small gap interface.

As the linear heat rating is increased during the first ramp to power, pellet cracking and relocation of the pellet fragments could occur. A pellet fragment cracked from a pellet could become lodged in the gap before full power is achieved. This situation is depicted in Figure 51. As the linear heat rating (LHR) is further increased, radial thermal expansion of the pellet and fragment increases the frictional force between the contact areas until the axial thermal expansion of the pellet stack causes elongation of the cladding. In this situation, three effects are required to cause mechanical elongation of the cladding: pellet cracking, strategic relocation of the cracked fragment, and fuel thermal expansion.

FIGURE 51. Fuel Stack with Displaced Pellet Fragment

As the irradiation proceeds, and at fixed linear heat ratings, the PCI-induced cladding elongations decrease. According to E. Rolstad of Halden in 1974, this phenomenon has been observed in other tests performed in the Halden Reactor. The decrease in cladding length was attributed to the formation of axial gaps between pellets as well as stress relaxation in fuel and cladding. Hence, pellet cracking, strategic relocation of pellet fragments, and fuel thermal expansion cause an increase in cladding length while the formation of axial gaps between pellets or stress relaxation causes the cladding elongation resulting from PCI to decrease. The net cladding elongation at anytime during irradiation results from a combination of these factors.

If rods are fabricated with small cold gaps, at some point radial thermal expansion of the pellets alone will be sufficient to cause PCI and cladding elongation.

Discussion of Results. The parameters in this test included pellet-cladding gap size and eccentricity, fill gas composition, fuel density and stability, linear heat rating, and burnup. A parameter which was not included but was discovered to be an influential factor in causing PCI in several of

the fuel rods was the initial ramp rate. Also, the boiling power in the test channel, which was changed during assembly calibration, affected the outputs of the elongation detectors but did not cause PCI. These two parameters are discussed first. This is followed by an analysis of the effect of fuel density, stability, and gap size on cladding elongation and the effect of pellet-cladding contact pressure on fuel centerline temperature. Finally, the results of pre- and post-irradiation measurements are presented and discussed.

a) Reactor Ramp Rate. Except for one rod in each assembly which was designed with a small gap so that thermal expansion of the pellets alone would cause PCI, three rods in IFA-432 and one rod in IFA-431 experienced PCI during the initial ramp to power. A summary of observed pellet cladding interactions is given in Table 20.

TABLE 20. Summary of Observed Pellet-Cladding Interaction in IFA-431 and IFA-432

ROD NO.	IFA-431 - PEAK POWER = 328 W/cm (10 kw/ft)			IFA-432 - PEAK POWER = 492 W/cm (15 kw/ft)				
	PCI OBSERVED	ASSEMBLY AVERAGE BURNUP GJ/kgM (MWd / MTM)	NO. OF POWER CYCLES	PCI OBSERVED	ASSEMBLY AVERAGE BURNUP GJ/kgM (MWd / MTM)	NO. OF POWER CYCLES	FUEL TYPE*	COLD DIAMETRICAL GAP mm (mils)
1	NO	-	-	YES	STARTUP	-	95% TD-S	0.229 (9)
2	NO	-	-	YES	130 (1510)	12	95% TD-S	0.331 (13)
3	YES	STARTUP	-	YES	STARTUP	-	95% TD-S	0.051 (2)** 0.076 (3) [†]
5	YES	STARTUP	-	YES	STARTUP	-	92% TD-S	0.229 (9)
6	NO	-	-	YES	STARTUP	-	92% TD-U	0.229 (9)

* TD - THEORETICAL DENSITY

S - STABLE

U - UNSTABLE

** IFA-431

[†] IFA-432

The two assemblies were essentially identical in construction, fuel loading, and fuel rod design. They were irradiated in different channels at the same reactor elevation so that the axial power shape was essentially the same in the two assemblies. The fact that PCI was observed during the initial ramp to power in three rods in one assembly and one rod in the other

assembly cannot be attributed to assembly design differences or differences in axial power shape, but must be attributed to some reactor operating parameter. The operating parameter during the first ramp to power which substantially differentiated IFA-432 from IFA-431 was the initial average ramp rate. The initial average ramp rate for IFA-432 was three times that for IFA-431, the values being 0.85 W/cm-min (0.0259 kW/ft-min) and 0.28 W/cm-min (0.0085 kW/ft-min).

The initial and subsequent ramp rates, and hence the fuel temperature rate of increase, apparently was a critical parameter in causing PCI in this test. A summary of ramp data for the two assemblies is presented in Table 21.

TABLE 21. IFA-431 and IFA-432 Ramp Data

ASSEMBLY	RAMP	ASSEMBLY AVERAGE BURNUP GJ/kgM (MWd/MTM)	DATE	NUMBER OF POWER CYCLES BETWEEN RAMPS	AVERAGE RAMP RATE * W/cm-min (kW/ft-min)
IFA-431	A	0	JUNE 6, 1975	0	0.28 (0.0085)
	B	2 (25)	JUNE 20, 1975	7	0.65 (0.0198)
	C	22 (250)	JULY 7, 1975	6	0.77 (0.0235)
	D	164 (1900)	AUG 30, 1975		0.59 (0.0180)
					0.57 (0.0175) = AVE
IFA-432	A	0	DEC 8, 1975	0	0.85 (0.0259)
	B	2 (25)	DEC 14, 1975	12	2.00 (0.0610)
	C	130 (1510)	JAN 24, 1975	2	2.33 (0.0710)
	D	172 (1990)	FEB 5, 1976		1.67 (0.0509)
					1.71 (0.0522) = AVE

*ALL RAMPS ARE UP RAMPS

a) Boiling Power. The cladding elongation results from Ramp B for IFA-431 differ significantly from those of Ramp A. Results are shown in Figure 52. This difference was caused by the procedure which was used to calibrate IFA-431 during Ramp B.

During Ramp A, the mode of heat transfer to the coolant was forced circulation. During Ramp B, the heat transfer was by natural circulation in which the subcooling was changed in order to calibrate the fuel assembly power. The test channel power is determined by varying the subcooling to the channel while maintaining a constant assembly power (labeled as CR1 and CR2 on Figure 52). The boiling power is calculated and is plotted as a function of subcooled power. The plot is very close to a straight line and by extrapolating to the abscissa the minimum subcooled power required to suppress boiling is determined. This subcooled power is equal to the total channel power. The moderator heating power is calculated and subtracted from the total channel power to give the total assembly power.

The boiling power was changed over a wide range (0-40 kW) during the calibration procedure. An analysis of the calibration data taken during Ramp B, indicated that changes in boiling power affected the output of the elongation detectors. As can be seen from Figure 52, the upramp elongation detector output as a function of average linear heat rating (ALHR) for Ramp B differs

FIGURE 52. A Comparison of Elongation Detector Outputs During Ramp A and B for Rod 1 in IFA-431

significantly from that for Ramp A and also from that calculated for the cladding axial thermal expansion for natural circulation. The reason for these significant differences is associated with the changes in boiling power during Ramp B.

At the essentially constant assembly power during the Ramp B calibration runs (CR1 and CR2), the transition from nonboiling to boiling surface heat transfer coefficient causes a reduction in average cladding temperature, thus resulting in a decrease in cladding length. Also, at low boiling powers mixing in the test channel is poor.⁽⁴⁸⁾ Poor mixing results in coolant temperatures which are higher near the fuel rods than those near the test channel. As the boiling power increases, the temperature distribution across the channel becomes less nonuniform and the coolant temperatures near the fuel rods decrease and those near the channel increase. This results in an additional reduction in cladding temperatures and also an increase in stay rod temperatures and in stay rod length. A diagram showing the relationship of the stay rods, elongation detectors, and fuel rods is shown in Figure 53. An increase in stay rod temperatures causes a decrease in elongation detector output.

The effect on elongation detector output at 0 boiling power (Ramp A) and with increasing boiling power (Ramp B) is shown in Figure 54. This figure is a plot of data taken from Ramp A at 160 W/cm (4.9 kW/ft) and an elongation detector output of 0.078 mm (3 mils), and from Ramp B, Calibration Run 1, in which the fuel rod average linear heat rating was maintained between 155 (4.7) and 158 W/cm (4.8 kW/ft). This figure shows that at a fuel rod average linear heat rating of 155-160 W/cm (4.7-4.9 kW/ft) the elongation detection outputs decreased from 0.078 mm (3 mils) (Ramp A, 0 boiling power) to 0.018 mm (0.7 mil) (Ramp B, 35 kW boiling power). Calculations predict that an increase of stay rod average temperature of 15°C relative to the cladding average temperature would produce the observed change in elongation detector output. A relative temperature change of 15°C under the circumstances is not unreasonable.

FIGURE 53. Schematic Arrangement of Fuel Rod Elongation Measurement System

FIGURE 54. Elongation Detector Output as a Function of Boiling Power at Constant Fuel Rod Linear Heat Rating

The same type of analysis could not be performed on IFA-432 data because a failed calibration valve prohibited a similar calibration of the assembly. (IFA-432 was using other instrumentation combined with data from the IFA-431 calibration.)

In evaluating the initiation of PCI in fuel rods in which cooling was by natural circulation, comparison is made to the slope of Rod 2 in IFA-431 in which PCI was not observed. This slope, rather than a calculated slope, was judged to be a better measure of cladding free axial thermal expansion.

c) Fuel Density and Stability. Fuel Rods 1, 5, and 6 were designed to obtain information concerning the density and stability of fuel on fuel rod behavior. These rods all had 0.229 mm (9 mil) gaps. Rod 1 contained 95% theoretical density (TD) fuel. Rods 5 and 6 each contained 92% TD fuel; however, the fuel in Rod 5 was stable while that in Rod 6 was unstable and thus susceptible to densification.

The measured cladding elongations for these rods in IFA-432 are plotted as a function of fuel rod average linear heat rating (ALHR) for the initial ramp to power in Figure 55 and for a ramp which occurred after 14 full power cycles at a burnup of 172 GJ/kgM (1990 MWd/MTM) in Figure 56. All rods showed PCI during the first ramp to power. The magnitude of the cladding elongation at a fixed ALHR decreased during subsequent exposure to that shown in Figure 56 for Cycle D. During the initial ramp to power, Rod 1, which contained stable and the higher density fuel, showed the least cladding elongation. Rod 5 which contained 92% TD stable fuel showed the greatest elongation with Rod 6 which contained 92% TD unstable fuel falling in between. These different elongations can be related to the thermal conductivity of the fuel types which was measured on nonirradiated fuel (Figure 57). The fuel type in Rod 5 had a fuel thermal conductivity approximately 17% less than that in Rod 1 while the thermal conductivity of the fuel type in Rod 6 was approximately 10% less than that in Rod 1. The lower fuel thermal conductivities cause higher rates of fuel temperature increase at the same ramp rate and higher fuel temperatures at the same ALHR. The latter causes greater radial expansion of the pellet and thus a greater pellet cladding contact force. The former could cause more severe pellet cracking than in fuels with higher thermal conductivities.

During Ramp D, [14 full power cycles, 172 GJ/kgM (1990 MWd/MTM)], Figure 56, Rod 6 attained the greatest elongation. An analysis of the fuel temperatures which occurred during this ramp showed that the fuel in Rod 6 attained higher temperatures than the fuel in Rods 1 and 5. Presumably this occurred because the fuel in Rod 6 was unstable and susceptible to densification.

Another example of lower thermal conductivity causing PCI occurred in Rods 1 and 5 in IFA-431. These rods were loaded with the same type of fuel as those in IFA-432. On the initial ramp to power, Rod 5 showed PCI but Rod 1 did not. The results are presented in Figure 58. On subsequent power ramps the elongation decreased until there no longer was any measurable elongation beyond that of free thermal expansion in either rod.

FIGURE 55. A Comparison of Fuel Rod Elongations for Rods Containing Stable and Unstable Fuels During Initial Ramp to Power

FIGURE 56. A Comparison of Fuel Rod Elongations for Rods Containing Stable and Unstable Fuel at Low Exposures

FIGURE 57. Measured Fuel Thermal Conductivities for Nonirradiated Fuel Types Used in IFA-431 and IFA-432

FIGURE 58. Comparison of Fuel Rods Containing Fuel of Different Thermal Conductivities

d) Fuel Gap. Fuel Rods 2 and 3 each contained 95% TD stable fuel. Rod 2 was designed with a large gap (0.381 mm, 15 mils) to simulate instantaneous densification. Rod 3 was designed primarily as an independent check of rod linear heat ratings and to accomplish this was designed with a small gap (0.076 mm, 3 mils).

A comparison of fuel rod elongations which occurred during Ramps A and C for Rods 2 and 3 is presented in Figure 59. Rod 3 exhibited strong PCI during the first ramp to power (Ramp A), whereas PCI in Rod 2 was first observed during Ramp C. A comparison of the elongations for the two rods during Ramp C shows a marked similarity in behavior up to 375 W/cm (11.4 kW/ft), the highest ALHR attained by Rod 2 during this ramp. Beyond 375 W/cm (11.4 kW/ft), Rod 3 experienced increased PCI.

During Ramp D Rod 3 showed no PCI, whereas Rod 2 showed a slight elongation beyond that of thermal expansion. The effect of PCI in the large gap rod on gap conductance could not be determined because the temperature monitor, an ultrasonic thermometer, at the peak power position in this rod failed to operate.

PCI in Rod 2 could be caused by pellet fragment relocation to the cladding during previous cycles. During Ramp C, thermal expansion locked a fractured pellet or pellets to the cladding resulting in cladding elongation. In Rod 3, PCI occurred immediately during Ramp A as a result of thermal expansion only of the pellets. During subsequent cycles axial gaps formed between pellets which reduced the extent of PCI.

e) Cladding Elongation and Fuel Centerline Temperature. The small gap rods, Rod 3 in each assembly, showed PCI near the beginning of the initial power ramp for each assembly. A plot of fuel centerline temperature at the upper thermocouple location and the cladding elongation as a function of fuel rod average linear heat rating is shown in Figure 60 for IFA-432.

The upramp for the cladding elongation shows an increase in slope at approximately 235 W/cm (7.2 kW/ft) and 340 W/cm (10.4 kW/ft). There is also a slight increase in slope in the fuel centerline temperature at these ALHR.

FIGURE 59. A Comparison of Fuel Rod Elongations for Rods Having Small and Large Gaps

FIGURE 60. Relationship of Fuel Centerline Temperature to Cladding Axial Elongation in Small Gap Rod

These slope increases in the fuel centerline temperatures could be associated with changes in the fuel thermal conductivity brought about by the increase in contact force between pellets and cladding. It is conjectured that in this rod, which had a small initial cold gap, because the gap conductance was already high any increase in it caused by an increase in contact force would not significantly affect the fuel centerline temperature; however, the

increase in fuel temperature could have caused additional pellet cracking which could reduce the effective thermal conductivity of the pellet. In their experiments, Stora, et al.⁽⁵⁰⁾ observed a decrease in the effective thermal conductivity of UO_2 pellets which they attributed to pellet cracking. The decrease in effective pellet thermal conductivity would result in higher fuel temperatures.

No correlation between fuel centerline temperature and cladding shortening was observed during the 51-hr period in which the ALHR was fairly constant.

b) Pre- and Post-Irradiation Measurements. IFA-431 pre- and post-irradiation measurements were made at the Kjeller Hot Laboratory in Norway. The pre-irradiation measurements were made during May 1975, the post-irradiation measurements during August and September 1976. Results for IFA-431 are given in this report because IFA-432 is still under irradiation at this writing. The results of measured changes in cladding outside diameter and length and deduced residual gap widths are as follows:

- Measured Diameter and Length Changes

The results of measured changes in cladding outside diameter and length are shown in Table 22.

TABLE 22. Pre- and Post-Irradiation Measurements for Helium Filled Rods - IFA-431

Rod No.	Diameter Change mm(mils)	Pre-Irradiation Cladding Outside Diameter mm(in.)	Length Change mm(mils)	Pre-Irradiation Cladding Length mm(in.)	Evidence of Pellet Cladding Interaction, (PCI)
1	0	12.779(0.5031)	0.24(9.4)	645.68(25.420)	No
2	0	12.779(0.5031)	0.17(6.7)	645.36(25.409)	No
3	0.015(0.59)	12.781(0.5032)	0.25(9.8)	645.58(25.417)	Yes
5	0.001(0.04)	12.781(0.5032)	0.18(7.1)	645.62(25.418)	Yes
6	0.005(0.20)	12.776(0.5030)	0.16(6.3)	645.60(25.417)	No

Three of the rods show a small permanent diameter strain. The greatest residual diameter change being 0.015 mm (0.59 mils) for Rod 3 which probably was the result of the strong PCI occurring in this rod.

The residual length elongations are more uniform than the diameter changes with no apparent correlation between rod PCI behavior and residual elongations. Rod 1, in which no PCI was observed, had essentially the same residual elongation as that of Rod 3 which showed strong PCI. The residual elongations are probably the result of radiation growth and/or stress relaxation.

- Deduced Residual Gaps

The Kjeller Hot Laboratory has devised a means to obtain an indication of post-irradiation gap width. A lever applies a line force to a fuel rod mounted on a flat support to ovalize the rod. An interaction curve is produced from which an indication of residual gap width can be deduced. The gap width cannot be adequately deduced from the first displacement curve because of the rounded shape of the curve so this interaction is considered as a conditioning of the fuel rod. The indicators of gap width are taken from subsequent displacement curves. The results of these procedures are presented in Table 23.

TABLE 23. Results of Deduced Gap Widths for Helium Filled Rods - IFA-431

<u>Rod No.</u>	<u>Post-Irradiation Average Deduced Gap Width^(a) mm(mils)</u>	<u>Pre-Irradiation Nominal Gap Width mm(mils)</u>	<u>Fuel Type^(b)</u>	<u>Evidence of Pellet-Cladding Interaction, PCI</u>
1	0.177(7)	0.229(9)	95% TD-S	No
2	0.275(11)	0.381(15)	95% TD-S	No
3	0.021(1)	0.051(2)	95% TD-S	Yes
5	0.225(9)	0.229(9)	92% TD-S	Yes
6	0.282(11)	0.229(9)	92% TD-U	No

(a) Average of results from four axial positions on each fuel rod taken at the location of solid pellets.

(b) TD = theoretical density, S = stable fuel, U = unstable fuel.

These results cannot be correlated with fuel rod PCI behavior. Rods 3 and 5 showed PCI and the above results show little change from the pre-irradiation nominal gap widths. The results show that Rods 1 and 2, in which no PCI was observed, have smaller gaps after irradiation while Rod 6, also no PCI, has a larger gap. The latter could be due to densification in the unstable fuel in Rod 6.

Conclusions. The conclusions derived from this experiment relevant to the fuel rods operating under the same conditions are:

- The initiation of PCI is related to power ramp rates. Power ramps induce thermal strain rates in fuel pellets which cause pellet cracking. The cracks could act as thermal barriers and cause a reduction in the effective thermal conductivity of the pellets. The pellet fragments can also relocate into the pellet cladding gap during the power ramp and cause PCI. It is concluded that the higher ramp rates in IFA-432 compared to those in IFA-431 caused more extensive pellet cracking and fragment relocation and a greater reduction in the effective thermal conductivity of the fuel pellets which resulted in significantly more PCI in IFA-432 than in IFA-431.
- The magnitude of cladding elongation is inversely related to fuel thermal conductivity. Fuel rods containing fuel pellets with higher thermal conductivities elongate less than those containing fuel pellets with lower thermal conductivities.
- The magnitude of cladding elongation decreases at a slower rate in fuel rods containing unstable fuel than in rods containing stable fuels.
- Small gap rods are more susceptible to PCI than large gap rods.
- Cladding elongations resulting from PCI in small and large gap rods both decreased to low values by the time a burnup of 173 GJ/kgM (2000 MWd/MTM) was reached.
- Increased pellet-cladding contact force in the small gap rod (0.076 mm, 3 mils), evidenced by an abrupt increase in cladding upramp slope, produced an increase in fuel centerline temperature. A decrease in

effective pellet thermal conductivity was more effective in influencing the fuel centerline temperature than the increase in gap conductance caused by the increase in pellet-cladding contact pressure.

- The boiling power, which is changed over a wide range during assembly calibration, causes a change in cladding surface heat transfer coefficient and a less nonuniform radial temperature distribution in the channel. This causes a change in the output of the elongation detectors but does not cause PCI.

TASK B: EX-REACTOR DETERMINATION OF GAP AND CONTACT CONDUCTANCE

The objective of this task is to determine (ex-reactor) the gap and contact conductance of UO_2 and Zircaloy interfaces as a function of the following seven variables: temperature, gas pressure, gas composition, interfacial contact pressure, gap separation, surface characteristics and time. The information obtained will be coupled with in-reactor testing and be used to further refine GAPCON-THERMAL, FRAP-S, and other related computer programs for oxide fuel elements.

Background for Task B

Although the principles which govern heat transfer across the fuel-cladding gap are fairly well understood, the process within an actual fuel element is complex and difficult to predict. Quantitatively, the heat transfer can occur by fluid convection, conduction, and radiation. These modes are interdependent and difficult to predict precisely because of their dependence upon a number of variables which are not easily defined in a fuel element. In a fuel pin, contact and noncontact between the fuel and cladding may occur separately or simultaneously. The total thermal interface conductance, H_T , can be considered to consist of contributions arising from the contact conductance, H_C (fuel and cladding are in contact), and the gap conductance, H_g (fuel and cladding are separated).

Studies have been conducted which have shown that H_T is dependent on the gap size, gas composition in the gap, temperature in the gap and on its boundaries, the extent and pressure of fuel-cladding interaction, fabrication parameters, and operating history of the fuel rod. In-reactor experiments designed to measure the dependence of H_T on these parameters are complicated because of the difficulties and uncertainties in controlling and determining the variables which affect the heat transfer across the interface. For example, the eccentricity or non-uniform surface contact between the fuel and cladding adds a very unpredictable dimension which increases the uncertainty in calculating the stored heat in the fuel rod. Ex-reactor experiments, on the other hand, afford the opportunity to study the dependence of H_T on a variety of parameters in a well-defined and controlled manner. Determination of the sensitivity of the interface conductance to changes in these parameters

would allow a further refinement of computer programs such as GAPCON-THERMAL,^(4,14) and FRAP-S⁽¹⁵⁾ which predict the effects of gap and contact conductance in oxide fuel elements.

Previous Work

Ex-reactor experiments by various investigators⁽⁵¹⁻⁵³⁾ have determined the sensitivity of the thermal interface conductance between UO_2 and Zircaloy over a limited range of contact pressure, temperature, surface roughness, and atmosphere. However, of current interest to modeling groups is the desire to extend these studies to gap widths, higher pressure, temperatures, different surface morphologies, and atmosphere. Table 24 summarizes the parameters and the range of conditions of interest.

A review of experimental apparatus employed by these investigators⁽⁵¹⁻⁵³⁾ was made. It was concluded that reliable measurements could not be made at high gas pressures and elevated temperatures because of experimental difficulties associated in making the measurements. An assessment of a variety of measurement techniques (comparator, radial, axial, guarded hot plate, thermal diffusivity) for measuring the interfacial conductance concluded that although experimental apparatus have been devised to measure the contact conductance between materials at low and intermediate temperatures and at pressures ≤ 1 atm,

TABLE 24. Parameters and Range of Interest

Interface Temperature	- Room temperature to 600°C or higher
Gas Pressure	- Up to 1.38×10^7 Pa (2000 psia)
Gas Composition	- Pure He, He-Xe-Kr and other gas mixtures representative of the gas atmosphere in fuel pins
Gap Width	- 1.2×10^{-2} cm (0.005 in.) to contact
Interfacial Contact Pressure	- 0 to 3.45×10^7 Pa (5000 psi)
Surface Morphology	- Typical of contemporary fuel element designs
Time	- Up to several hours

there is no generally acceptable technique(s) or apparatus available for measuring gap and contact conductances at elevated temperatures and high gas pressures.

However, based on encouraging results of an experiment designed to evaluate the thermal diffusivity technique for measuring both gap and contact conductance, it was recommended that the thermal pulse and axial (longitudinal) techniques are the best experimental means for investigating the sensitivity of gap and contact conductance to changes in the parameters of interest. Preliminary designs of a proposed Modified Pulse Design (MPD) and a Modified Longitudinal Design (MLD) apparatus were proposed. In addition, it was recommended that joint MPD and MLD measurements of gap and contact conductance be made so as to allow for cross-checks on system performance and to increase the informational reliability of the data so obtained.

Program Development

An experimental program was then developed and initiated to coordinate both the design, construction, testing, and data acquisition of the MPD and MLD experiments. A flow chart of this program is illustrated in Figure 61. The program is designed to:

- Develop the MPD and MLD techniques as experimental tools for measuring interface conductance.
- Determine the sensitivity of H_c and H_g to variations in the following seven factors: gap size, temperature, contact pressure, gas composition, gas pressure, interface surface morphology, and time.
- Compare theoretical and experimental sensitivities of H_c and H_g to variations in the factors of interest.

Because of the experimental difficulties associated with measurements between vacuum and high gas pressures, the experimental phases have been divided in terms of measurement technique and atmospheric ranges as listed in Table 25.

FIGURE 61. Ex-GAPCON Program Progression

TABLE 25. Stages of H_g and H_c Experimentation

Techniques		Stage	Classification	Range
MPD	MLD			
		I	Vacuum	1.33 Pa (10^{-3} Torr) to 1.01 x 10^5 Pa (760 Torr)
*	*		Low Pressure	1.10 x 10^5 Pa to 1.72 x 10^5 Pa (25 psia)
*	*	II	Intermediate Pressure	1.72 x 10^5 Pa to 2.8 x 10^6 Pa (400 psia)
*		III	High Pressure	2.8 x 10^6 Pa to 1.38 x 10^7 Pa (2000 psia)

Progress Prior to January 1977

A. MPD Progress.

1. MPD System. An experimental system has been designed, constructed, and tested that will allow the measurement of both H_g and H_c as a function of the parameters as listed in Table 24. The current experimental limitations are that only measurements under low gas pressures and contact pressures up to 2.7×10^6 Pa are possible. A schematic of the MPD apparatus and system components is shown in Figure 62. The main feature in this system is the use of a graphic computing system with a transient waveform recorder for data capture, calculation, analysis, and storage. The complete data acquisition system is expected to be fully functional upon receipt of the interface required to communicate between the two devices.

The MPD vacuum and low pressure vessel is illustrated in Figure 63. A laser is used to supply a heat pulse to the front face of a UO_2 sample. The resulting temperature-time (T-t) curve may be measured by either an infra-red optical detector focused on the rear of the Zircaloy sample or a fast response thermocouple. A furnace controls the ambient temperature and a hollow column load cylinder and hollow column load cell are used to supply and measure the contact force between the samples. For gap conductance measurements spacers are used to maintain a fixed separation distance between the UO_2 and Zircaloy samples.

FIGURE 62. Schematic of MPD Vessel1 (vacuum and low gas pressures) and System Components

In this quarter, calibration of components and testing of the overall system have been performed. Reliability of the overall system was checked by performing measurements on a standard sample of ARMCO iron and comparing the results with that reported by other investigators. As seen in Figure 64, these results are in good agreement with measurements made with similar material on other experimental systems.

2. MPD Data Analysis. Heat diffusion equations have been developed^(54,55) for three-layer, two-layer, and homogeneous systems with appropriate boundary conditions. Effort in this quarter has been directed towards an assessment of the analysis by Stewart,⁽⁵⁴⁾ Taylor and Lee,⁽⁵⁵⁾ and PNL as applied to measurement of thermal contact resistance between UO_2 and Zircaloy.

FIGURE 63. MPD Vacuum and Low Pressure Apparatus

FIGURE 64. Thermal Diffusivity of ARMCO Iron from 200 to 820°C as Measured on MPD Apparatus Compared with Reported Thermal Diffusivity Data

a. Stewart Analysis.⁽⁵⁴⁾ The Stewart formulation is considered the most restricted of the three derivations examined. The basic assumptions made by Stewart are: (1) the heat transfer through the two specimens is one-dimensional; (2) there is no convection heat transfer at the exterior surfaces; (3) there are no radiation effects; (4) the temperature history is strongly dependent upon the thermal interface resistance; and (5) that a solution for the temperature history can be solved using a separation of variables method. Inherent in Assumption (5) is that the temperature at any point can be expressed as products of a strictly spatial function and a temporal function. The solution takes the form:

$$T(x,t) = (A \cos \lambda x + B \sin \lambda x)e^{-\alpha \lambda^2 t} + C \quad (1)$$

which is the sum of the transient and steady-state parts. Applying the boundary conditions and assuming only one eigenfunction (λ_1) is significant yields:

$$R = \frac{\cot \lambda_1 x_1}{k_1 \lambda_1} + \frac{1}{k_2 \lambda_1} \cdot \left(\frac{\alpha_2}{\alpha_1} \right)^{1/2} \cdot \cot \left[\lambda_1 \left(\frac{\alpha_1}{\alpha_2} \right)^{1/2} \cdot x_2 \right] \quad (2)$$

where the eigenfunction, λ_1 , is the only unknown.

Assuming the temperature rise on the back specimen surface is an exponential of the form

$$T_2(x_2,t) = -T_F e^{-\alpha_1 \lambda_1^2 t} + T_F \quad (3)$$

the eigenfunction, λ_1 , is determined from two points on the experimental T-t curve related by

$$\Delta T_2'' = T_F - T_2'' = \frac{T_F - T_2'}{A} ; T_2'', t_2' \quad (4)$$

where T_F is the maximum, T_2'', t_2'' is arbitrarily picked, T_2', t_2' determined by Equation (4) and A, a constant, is chosen for convenience. Since

$T_F - T_2'' = T_F e^{-\alpha_1 \lambda_1^2 t_2''}$, λ can be determined from Equation (4) as being

$$\lambda_1 = \left[\frac{\ln A}{\alpha_1 (t_2'' - t_2')} \right]^{1/2} \quad (5)$$

Stewart performed his analysis on metal pairs of high conductivity separated by an interface of relatively high thermal resistance. Figure 65, for example, shows a plot of R versus λ for an aluminum-aluminum pair. The essence of Stewart's analysis is in obtaining λ_1 's which fall in the relatively flat plateau area where R varies only slightly with variations in λ_1 . However, even for the aluminum pair, R varied by as much as 20% depending on the location chosen on the experimental T - t curve from which λ_1 was calculated. This problem becomes aggravated even further when sample pairs of greatly different conductivities and diffusivities are used, as shown in Figure 66 for a UO_2 /Zircaloy-4 sample pair. Here R is a strong function of λ_1 , and in several regions takes on physically un-interpretable negative values. These problems may in part be due to Stewart's consideration of only one eigenvalue in the derivation. A more fundamental fault, however, lies in the basic assumption that the experimental T - t history can be expressed in terms of products of two functions, one containing only spatial terms and the other containing only temporal terms.

For these reasons it is concluded that the Stewart analysis is not applicable or amendable for use in the determination of thermal resistances between sample pairs of widely differing thermal properties such as UO_2 and Zr.

b. Taylor and Lee Analysis.⁽⁵⁵⁾ In the flash technique, the front surface of the sample is subjected to a short radiant energy pulse and the resulting temperature history of the rear face is recorded. From this temperature history, the thermal interface resistance can be determined. To solve the heat diffusion equation with the appropriate boundary conditions, the following assumptions are made:

- One dimensional heat flow,
- No heat loss from the sample surfaces,
- Each layer is homogeneous,
- Heat pulse is uniformly absorbed on the front surface,
- Only the thermal interface conductance is unknown.

FIGURE 65. Thermal Contact Resistance, R , Versus Eigenfunction, λ , (Equation 5) for an Aluminum-Aluminum Sample Pair

FIGURE 66. Thermal Contact Resistance, R , Versus Eigenfunction, λ , (Equation 5) for a UO_2 /Zircaloy-4 Sample Pair

The boundary conditions for describing heat diffusion problems with thermal contact resistance between layers are

$$-k_1 \frac{\partial \theta_1(-\ell_1, t)}{\partial z} = \omega(t) = \text{laser pulse} \quad (6)$$

$$-k_1 \frac{\partial \theta_1(0, t)}{\partial z} = -k_2 \frac{\partial \theta_2(0, t)}{\partial z} = h_{cyc} [\theta_1(0, t) - \theta_2(0, t)] \quad (7)$$

$$-k_3 \frac{\partial \theta_3(\ell_2, t)}{\partial z} = 0 = \text{heat loss at back surface} \quad (8)$$

and the initial conditions are

$$\theta_1(z, 0) = \theta_2(z, 0) = 0. \quad (9)$$

From the initial and boundary conditions, the diffusion equations are solved using Laplace transforms and complex contour integration.

The solved for normalized temperature T of the rear face solved can be represented as

$$T = 1 + 2 \sum_{k=1}^{\infty} \frac{(X_1 \omega_1 + 2\omega_2) Q(\gamma_k, \eta_2, t)}{\omega_1 X_1 \cos \omega_1 \gamma_k + \omega_2 X_2 \cos \omega_2 \gamma_k + R [\cos \omega_1 \gamma_k - \cos \omega_2 \gamma_k] - \gamma_k R [\omega_1 \sin \omega_1 \gamma_k - \omega_2 \sin \omega_2 \gamma_k]} \quad (10)$$

where

$$R = \rho_1 c_1 \ell_1 / (h_{cyc} \eta_1 \eta_2) \quad (11)$$

h_{cyc} is the thermal contact resistance coefficient ($\text{Wcm}^{-2}\text{K}^{-1}$).

The following terms in Equations (10) and (11) are defined as:

$$X_1 = H_{1/2} \eta_2 / \eta_1 + 1$$

$$X_2 = H_{1/2} \eta_2 / \eta_1 - 1$$

$$\omega_1 = \eta_1 / 2 + 1$$

$$\omega_2 = \eta_1 / 2 - 1$$

$\eta_2 = (\ell_2^2/\alpha_2)^{1/2}$ Square root of heat diffusion time of j^{th} layer

$H_j = \rho_j C_j \ell_j$ Heat capacity of j^{th} layer

$H_{1/2} = H_1/H_2$ Relative heat capacity

$\eta_{1/2} = \eta_1/\eta_2$ Square root of relative heat diffusion time

α_j = Thermal diffusivity of j^{th} layer (cm^2/sec)

C_j = Specific heat of j^{th} layer at constant pressure (J/gmK)

a = Cross sectional area of sample (cm^2)

ℓ_j = Thickness of j^{th} layer (cm)

t = Time variable (sec)

ρ_j = Density of j^{th} layer (gm/cm^3)

$Q(\gamma_K, \eta_2, t) = \exp(-\gamma_K^2 t/\eta_2^2)$ instantaneous heat pulse

The γ_K is the K^{th} positive root of

$$X_1 \sin \omega_1 \gamma + X_2 \sin \omega_2 \gamma + \gamma R [\cos \omega_1 \gamma - \cos \omega_2 \gamma] = 0. \quad (12)$$

or

$$\eta_2 / \eta_1 H_{1/2} \cot \eta_{1/2} \gamma + \cot \gamma - \gamma R = 0 \quad (13)$$

The solution for h_{CYC} is determined by an iterative, computer based technique which involves choosing a h_{CYC} value; calculation of R from Equation (10); determining the roots γ_K of Equation (12) and calculation of the temperature-time history from Equation (10); determining the roots γ_K of Equation (12) and calculation of the temperature-time history from Equation (10). Iteration of h_{CYC} is made until the theoretical and experimental T - t curves are in agreement as shown in Figure 67 for a 316 stainless steel-brass pair.

The limitations of this analysis technique are that convective and radiative heat losses are not considered. These terms are expected to make significant contributions at elevated temperatures and high gas pressures.

c. PNL Analysis. An analysis of the heat diffusion equation was initiated during the October-December 1976 quarter. The following assumptions were made:

FIGURE 67. Normalized Temperature Versus Time for a 316 Stainless Steel-Brass Sample Pair Comparing Equation (10) with the Experimental Data

- (1) The heat pulse is uniformly absorbed on the front surface.
- (2) Radiative and convective heat losses from both the front and back surfaces are considered.
- (3) Heat flow is one dimensional.
- (4) Each layer is homogeneous.

The temperature on the back sample surface will be given by an expression of the form:

$$T_b = T(H_F, h_I, H_b, t, \dots) = \sum_{N=0}^{\infty} A_N e^{+\alpha_N t} \quad (14)$$

where H_F and H_b are the surface heat transfer coefficients at the front and back samples, respectively, and h_I is the unknown thermal interface conductance.

The terms A_N and α_N in Equation (14) contain terms dependent upon the thermal diffusivities, conductivities, heat capacities, densities, and surface heat transfer coefficients of the front and back samples, in addition to h_I . H_F , H_b , and h_I are determined by a computer iteration technique based on the given experimentally determined temperature history at the back face.

Completion of this analysis was expected during the January-March 1977 quarter. It was anticipated that inclusion of heat loss terms would allow the determination of reliable data from experiments conducted in regions of high heat loss (e.g., elevated temperatures and high gas pressures).

3. High Pressure MPD System. The design and construction of a special high-pressure autoclave and support stand were completed during the October-December 1976 quarter. Delivery of the high-pressure autoclave occurred in mid-October. The autoclave allows the extension of measurements in gas pressures from 25 psia to 2000 psia.

B. MLD Progress

1. MLD System. An experimental system has been modified* to allow use of the MLD sample geometry for limited measurements of both gap and contact conductances as a function of the parameters listed in Table 24. A schematic drawing of the MLD apparatus and sample geometry is shown in Figure 68. The UO_2 and Zircaloy sample geometry is designed such that a lateral sample guard, fabricated from the same material as the sample, surrounds the sample. The lateral sample guard surrounding each sample has four functions: (1) it significantly reduces any alteration of the steady-state temperature gradients in each sample arising from heat shunting through the mechanical spacers and alignment pins; (2) reduces the radial guard heater-sample temperature distribution mismatch; (3) provides location of alignment pins for accurate remating of sample interfaces in replication measurements; and (4) provides location for mechanical spacers for gap conductance measurements. Since heat shunting in gas environments (especially at high gas pressures) is expected to adversely affect the linear temperature gradient in both samples, ceramic rings, having thermal conductivities that are $\leq UO_2$ (or Zircaloy), are inserted in the space

* Model TCFM Series, Dynatech, Incorporated, Cambridge, Massachusetts.

FIGURE 68. Modified Longitudinal Design (MLD) Apparatus

separating the sample and lateral sample guard. The UO_2 and Zircaloy samples are positioned such that each sample represents a mirror image of the other.

The main axial heater is located on the back face of the UO_2 sample and is surrounded by guard heaters. The Zircaloy sample has a base heater located at the back face to maintain a linear temperature gradient along the entire length of the Zircaloy sample. Radial guard heaters are located along the length of each sample to minimize radial heat losses from the sample and provide an isothermal zone for specimen thermocouple leads.

Thermocouples are placed in two-bore ceramic tubes which extend radially from the samples, lateral sample guard, and radial guard heaters in isothermal

planes. This is to prevent heat leak down the leads of the couples. The guard heater wires also extend radially in isothermal planes for similar reasons.

The samples are insulated radially with low conductivity pellet insulation. The insulation is contained between a series of solid ceramic cylinder insulators and a water-cooled stainless steel inner jacket. Radiation shields are located around the inner solid cylinder insulation and above the main guard heaters. Stainless steel radiation shields are located around the outer solid cylinder insulation. The alumina cylinders and radiation shield are of a split design to facilitate reassembly of the system after the connection of the various thermocouple and guard heater wires to terminal strips located at opposite sides of the inner water-cooled jacket.

Application of a variable compressive load along the axis of the specimens may be provided by a variable hydraulic cylinder system located above the UO_2 sample. The compressive load is transferred through a stainless steel plunger and zirconia insulator which absorbs heat from the main guard heaters but floats independently of it.

The complete MLD apparatus is contained in a pressure vessel of sufficient size capable of maintaining an internal gas pressure up to 4×10^6 Pa.

2. MLD Sample Development. Development of the MLD samples was continuing. A special cutting tool for grinding the presintered uranium dioxide pellets was fabricated.

Directions of Efforts That Were Planned for the January-March 1977 Quarter

The PNL theoretical analysis for determining thermal interfacial resistance was to be completed. A comparative study of thermal resistance data analyzed by both the Taylor and Lee and PNL formulations under gap conditions was to be completed. This study will yield a basis for MPD Stage I experiments. Stage I experiments are scheduled to be initiated under gap conditions. Installation of the MPD high pressure vessel system was to be started but at a low level of effort. The fabrication of the MLD samples was to be completed with initial MLD testing and calibration experiments scheduled for March 1977.

Current Progress on Task B

As indicated previously, the goal of this task is to determine the sensitivity of thermal gap and contact conductance between uranium dioxide and Zircaloy-4 to variations in gap width, surface morphology, temperature, gas composition, gas pressure, contact pressure and time. The two independent experiments, MPD and MLD, designed and constructed to measure interfacial thermal conductance were discussed earlier in this report. The MPD (modified pulse design--a transient technique) and MLD (modified longitudinal design--a steady-state technique) experiments afford the opportunity to study the dependence of gap conductance on a variety of parameters in a well-defined and controlled manner. As stated before, determination of the sensitivity of the interface conductance to changes in these parameters would allow a further refinement of computer programs, such as GAPCON-THERMAL,^(4,14) FRAP-S⁽¹⁵⁾ and IAMBUS⁽⁵⁶⁾ which predicts the effects of gap and contact conductance in oxide fuel elements. The program elements and status are given in Figure 69.

Details on Progress this Quarter

1. MPD System. A computer program was developed to assist in reduction and refinement of the experimental time-temperature history data acquired either by use of an optical detector or fast response thermocouple. The program is designed to enhance signal to noise ratio; curve fit the digitized time-temperature curve; store experimental code information and data on magnetic tape; and, print out selected digitized points of the time-temperature curve for use in the MPD data analysis program for determining gap conductance.

2. MPD Data Analysis. Previously, an analysis was made of heat diffusion equations^(57,58) for three-layer, two-layer, and homogeneous systems with appropriate boundary conditions. The following is a discussion of the PNL analysis developed specifically for the analytic determination of thermal gap and contact conductance between UO_2 and Zircaloy. The first section of the discussion relates to the derivation of the analytic expression for the back face temperature transient. The second deals with fitting of the unknown resistance parameters to the experimental data.

FIGURE 69. Ex-GAPCON Program Progression - March 1977

A. Mathematical Model and Analytic Expression

In this analysis, the unknown quantity sought is the thermal resistance of a gap between two materials with known thermal properties. The experimental procedure is similar to single sample experiments, with the front face of one slab sample pulsed with a laser and the temperature transient recorded on the back face of the second sample.

If the experiment is properly designed, it has been shown that lateral heat losses from the samples may be neglected.^(58,59) It is assumed that these conditions are met leaving only a one dimensional heat conduction problem to be solved. It is also assumed that the thermal properties do not vary over the temperature transient range, and that there may be some heat losses from the front and back faces of the samples. Figure 70 describes the problem geometry with the boundary conditions. The known parameters are the thermal diffusivities, α_i , the thermal conductivities, k_i , the sample thicknesses, ℓ_i , and the radiant pulse shape, $w(t)$. The unknown quantities are the front and back face heat losses, q_f , q_b , and the heat transfer across the gap, q_1 and q_2 .

FIGURE 70. Problem Geometry and Boundary Conditions

The governing equations for the temperatures of the two samples are:

$$\frac{1}{\alpha_i} \frac{\partial T_i}{\partial t} = \frac{\partial^2 T_i}{\partial x_i^2} \quad i = 1, 2 \quad (1)$$

where T_1 and T_2 are the sample temperature deviations from the ambient.

The associated boundary conditions are:

$$-k_1 \frac{\partial T_1(t, x_1)}{\partial x_1} = w(t) - q_f = w(t) - h_1 T_1(t, 0), x_1 = 0 \quad (2)$$

where h_1 is the unknown film coefficient for the front face,

$$-k_1 \frac{\partial T_1(t, x_1)}{\partial x_1} = h_2 [T_1(t, \ell_1) - T_2(t, \ell_2)], x_1 = \ell_1 \quad (3)$$

where h_2 is the unknown gap conductance and

$$-k_1 \frac{\partial T_1(t, x_1)}{\partial x_1} = k_2 \frac{\partial T_2(t, x_2)}{\partial x_2}, x_1 = \ell_1, x_2 = \ell_2 \quad (4)$$

$$k_2 \frac{\partial T_2(t, x_2)}{\partial x_2} = q_b = h_3 T_2(t, 0), x_2 = 0 \quad (5)$$

where h_3 is the unknown film coefficient for the back face.

Implicit in the boundary conditions is the assumption that the heat capacity of the gap is negligible, i.e. $q_1 = q_2$.

Since the time required for the back face temperature to reach a maximum is long compared to the laser pulse time, $w(t)$ is assumed to be of a Dirac delta form.

Applying the Laplace Transform to the governing equations and determining the solutions to the resulting partial differential equations, the analytic expression for temperature as a function of time is then found using the inversions theorem

$$T_2(t,0) = \frac{1}{2\pi i} \lim_{L \rightarrow \infty} \int_{\sigma-iL}^{\sigma+iL} e^{tp} \overline{T_2}(p,0) dp \quad (6)$$

where σ is sufficiently large to make the inversion integral exist. This can be evaluated by contour integration methods by choosing σ large enough so that all the poles of the integrand are to the left of line $x = \sigma$ in the complex plane. The inversion integral can be easily evaluated using the Cauchy residue theorem provided all the poles can be located and their order determined. The functional form of the integrand of Equation (6) is such that it is extremely difficult to determine the order of each pole. The fact that the integrand does not have any multiple poles is not clear and could quite possibly possess such poles for some choices of h_1 , h_2 and h_3 . It is assumed that if such a case should arise, a slight adjustment in one of the h 's would eliminate the higher order pole without significantly changing the shape of the back face temperature function.

The back face temperature can then be written as:

$$T_2(t,0) = \sum_{i=0}^{\infty} A_i e^{\alpha_i t} \quad (7)$$

where the α_i are the poles of the integrand of Equation (6) and

$$A_i = \left(\frac{d\overline{T_2}}{dp} \right)^{-1}, p = \alpha_i \quad (8)$$

It can be shown that all poles of the integrand lie along the negative real axis. Hence it is convenient to convert to circular form to solve for the poles. In addition, it is convenient to normalize the back face temperatures by dividing by the maximum temperature. In this way, $w(t)$ in Figure 70 appears as a constant and it is not necessary to know the magnitude of the laser pulse.

Hence the problem reduces to choosing values for h_1 , h_2 and h_3 , deriving the corresponding expression for the back face transient temperature, and

checking the analytic results against the experimental time-temperature curve. The h 's are then adjusted in a suitable manner so that the analytic temperature function may more closely fit the experimental curve. This procedure is carried on iteratively until the agreement between the analytic temperature function and the experimental data is satisfactory.

B. Determining the Unknown Resistance Parameters

There are a variety of techniques available for determining unknown parameters in a functional expression such that the resulting function best fits a given set of data. Since the expressions dealt with here are highly non-linear, a method developed by Peckham⁽⁶⁰⁾ was chosen. This particular method has been shown fairly effective in problems involving non-linear functions.⁽⁶¹⁾

Peckham's method attempts to minimize the sum of the squared errors, as do many parameter fitting schemes, but it does not require the explicit calculation of gradients. The equation to be minimized is

$$S = \sum_{k=1}^m \left[f_k(h_1, h_2, h_3) \right]^2 \quad (9)$$

where h_1 , h_2 and h_3 are the unknown parameters, and

$$f_k = T_2(t_k, 0) - T_E(t_k) \quad (10)$$

where t_k is the time of the k th data point, $T_E(t_k)$ is the experimentally determined back face temperature at time t_k and m is the number of data points.

The difference function, f_k , may be linearized according to

$$f_k = C_k + \sum_{i=1}^3 g_{ki} h_i \quad (11)$$

If values for C_k and g_{ki} , $k = 1, 2, \dots, m$ are known then it is straight forward to find h_i , $i = 1, 2, 3$ to minimize Equation (9). If the functions, f_k , are non-linear h will not be the true minimum but can be used as a starting point for the next iteration.

In Peckham's method a set of guesses for the minimizing h-set is used to get a new estimate. Rather than calculating the coefficients in the linear approximation by numerical differentiation, they are chosen to minimize the m expressions

$$\sum_{\ell=1}^n w_{\ell}^2 \left(h_k + \sum_{i=1}^3 g_{ki} x_{i\ell} - f_{k\ell} \right)^2 \quad (12)$$

where n is the number of guesses being used and w_{ℓ} is a suitable weighting factor. Once the values for C_k and g_{ki} are determined they can be substituted into Equation (11) to determine the new estimated h-set to minimize Equation (9).

This technique has been shown to be quite effective for a variety of parameter fitting problems, though for this particular problem it appears that the data must be very accurate and it requires a good initial estimate for the unknown parameters.

1. MPD Data. Experiments were initiated this quarter to generate Stage I gap and contact conductance data and assess the regions where experiments may be readily conducted using the MPD technique. A comparison of MPD contact conductance data with that of other authors is shown in Figure 71. The MPD data is represented by a dot or open triangle depending on the value of Meyer's hardness used. In either case, the data trend is that the contact conductance is independent of contact pressure at low values of P_A . The MPD data is also in agreement with the data of Rapier and also with that of Ross and Stoute.

The preliminary results of gap conductance data at 400°C obtained at 0.005 and 0.01 cm separation widths is shown in Figures 72 and 73 for He and Ar gas at 1 atmosphere, respectively. Excellent agreement is seen at a gap width of 0.005 cm for both He and Ar with the gap conductance predicted by Lloyd, Kennand, and GAPCON. The lower values obtained at 0.01 cm may be attributed to the increase in radial heat losses with increased gap widths. These losses are not corrected for in the PNL analytical expression.

FIGURE 71. Comparison of MPD Contact Conductance Results with that of Rapier, Dean, and Ross and Stoute

FIGURE 72. Comparison of MPD Gap Conductance Data with that Predicted by the Lloyd, Kennard and GAPCON Models (He atmosphere)

FIGURE 73. Comparison of MPD Gap Conductance Data with that Predicted by the Lloyd, Kennard and GAPCON Models (Ar atmosphere)

2. MLD System. Fabrication of the MLD samples was completed this quarter. A series of seven UO₂ samples is currently under surface preparation for experiments scheduled to begin next quarter. These experiments are designed to provide a data base for correlation with the MPD results. Testing and calibration of the experimental system were initiated this quarter.

Directions of Effort for Next Quarter

MPD data generation under Stage I experimental conditions will continue. The experiments will involve varying gap widths, temperatures, gas atmospheres and surface morphologies. Installation of the MPD high pressure vessel (for Stage II and III experiments) will continue with testing of the laser, furnace, and chamber pressurization scheduled for the latter part of the quarter. MLD data generation is expected to be initiated this quarter with the first series of experiments designed for comparison with both the MPD gap conductance data being generated and the contact conductance data of Ross and Stoute.

FUEL SUBASSEMBLY PROCUREMENT AND IRRADIATION TEST PROGRAM

TASK A: DESIGN AND FABRICATION OF 4X TEST TRAIN

The objective of this task is to provide a 4X test train to be used in PBF tests with the goal of providing enhanced performance and cost effectiveness over that achieved with the current 4X test train.

Background for Task A

Criteria and technical bases for the design are being established, and will define such items as (a) the range of test conditions over which the test train is to be functional, (b) special material requirements, (c) compatibility requirements (between test train and PBF test loop and between test train and test fuel), (d) service lifetime, (e) thermal and mechanical loads, and (f) special handling considerations.

The existing 4X test train design will be thoroughly reviewed, and to the extent consistent with improved performance, features of the existing design will be retained. The PNL design will focus on commercialization of instruments and quick test turn around by easy replacement of failed instrumentation in the 4X test train assembly.

A preliminary design will be developed in sufficient detail to provide a basis for estimating the cost of fabrication. Product specifications will be developed for all components and instrumentation. Component fabricators and instrument suppliers will be identified, and joint efforts with instrument suppliers will be undertaken to develop instrumentation with improved reliability and with special characteristics that may be required for PBF tests. All components and instruments will be tested and evaluated prior to inclusion into the final detailed design.

The detailed design of the 4X test train will include working drawings, product and material specifications, and inspection and testing requirements to ensure that fabrication is within specifications. A full-scale assembly will be fabricated and made available for use in PBF tests.

Current Progress on Task A

The existing test trains are designed to irradiate four instrumented fuel rods simultaneously under transient conditions in the PBF facility. The test instrument leads extend from the in-reactor assembly without provisions for intermediate disconnect to the instrument consoles located adjacent to the PBF canal. The four fuel rods, each contained within a shroud, can be removed from the test train after cutting the fuel rod instrumentation leads, leaving intact a single unit of instrumented test train hardware. This single unit of hardware includes four turbine flow meters, four fuel rod elongation detectors, pressure transducers, thermocouples, and self-powered neutron detectors.

A conceptual design has been developed by PNL during this period that features a quadrant system to facilitate easy and quick replacement of instrumentation which has failed prior to nuclear testing and should assist in the timely completion of ongoing test programs by minimizing the amount of reactor downtime. The basic hardware features of this concept are illustrated in the cutaway view shown in Figure 74. Each quadrant will include an instrument package and a hanger tube which will house the lead wires that feed upward through braze plugs in the pressure head. The "L" shaped strong back will be made of Zircaloy-4 and will support both the instrument package and test shroud assembly. The strong back will be located into its proper position with guide pins, and integrally formed springs will be used to prevent excessive vibration or lateral movement between adjacent sectors.

Each shroud and fuel pin assembly can easily be attached or removed from the quadrant and will have its own penetration at the pressure head. A conceptual view of the eight-hole head, upper thermal baffle and catch screen assembly is shown in Figure 75. The protective tubes housing the instrument leads for each quadrant extend downward into the guide slots shown in Figure 74. The shroud and fuel pin assembly will be positioned by outer slots in the quadrant restraint bracket and will be free to grow axially downward against a spring-loaded flow guide piston at the bottom. Preliminary stress analysis on the eight-hole head configuration relative to the maximum anticipated pressure pulse is currently underway.

FIGURE 74. 4X Test Train Quadrant Hardware

FIGURE 75. Eight-Hole Head and Upper Assembly Hardware

If feasible, quick disconnect devices at a location immediately above the head will be employed to facilitate removal of a quadrant from the test train assembly. A back-up quadrant of instrumentation can be stored in the PBF canal thus allowing the removal of a shrouded fuel rod from a defective quadrant and the re-installation of the shroud in the back-up assembly. The reconstituted assembly can then be checked and loaded into the reactor and the test continued. The failed instrument quadrant could be transported from the PBF canal to another location for repair as shown in Figure 76.

The entire test train can be removed intact from the PBF canal to another site immediately after completion of a test thus permitting other tests to be run in the reactor. The proposed removal of the entire test train is shown in Figure 77, and would require a cask with a removable extension. The cask would provide shielding for the radioactive fuel rods, but hardware above the fuel rods would require less shielding and this could be provided by a removable extension. Cask modification to provide this additional shielding would be needed. For the case in which the activated instrument package only is removed, (Figure 76) shielding similar to that used in the cask extension would be adequate.

FIGURE 76. Radioactive Instrument Package Transported from PBF Canal

FIGURE 77. Entire Test Train Transported from PBF Canal

TASK B: ANALYSIS OF PBF GAP CONDUCTANCE TESTS

The objective of this task is to make pretest predictions for a 4X PBF gap conductance/stored energy test; these will be made with the latest available versions of the FRAP-S, FRAP-T, and GAPCON-THERMAL codes. A cooperative effort will also be pursued with the EG&G staff to include pretest predictions using the thermal oscillator technique.

Background for Task B

Data from the PBF data acquisition system is to be processed at PNL on a PDP 11/45 computer by modifying existing codes developed for NRC/PNL Halden tests. An extensive error analysis of the instrumentation and test conditions will be completed and submitted as a topical report. After completion of the raw data reduction, error analysis, and application of correction factors derived from the data analysis, a series of posttest predictions will be completed using the FRAP-S, FRAP-T, and GAPCON-THERMAL codes. The posttest analysis and results of the experiment will be included in a topical report.

Current Progress on Task B

Plans for shipping five PNL IFA-431 rods from Halden to INEL for subsequent testing in PBF have been set in motion. These plans also include the combined shipment of failed and destructively examined irradiated PuO_2 -bearing rods requiring double containment and other unfailed PuO_2 -bearing rods requiring single containment. The plutonia-bearing rods belong to EG&G and the purpose of consolidating shipments is to conserve costs. The general scheme for shipping will be as follows:

- The five PNL IFA-431 rods now at Kjeller will be combined with the single EG&G IFA-239 rod at Halden and shipped via a Halden-supplied cask to RISO. These six rods will be unloaded from the Halden cask, combined with the 12 IFA-226 rods at RISO and all 18 rods will be loaded into the BMI-1 cask at RISO and shipped to Idaho. The empty cask will be returned to Columbus, Ohio.
- The tasks required for accomplishing the shipment and the person or organization assigned responsibility for executing each task are:
 1. Determine RISO's capacity for loading and shipping the BMI-1 cask. J. A. Christensen.
 2. Ship single IFA-239 to Kjeller, combine with five IFA-431 rods, and ship to RISO in cask supplied by Halden.
 - a. Obtain necessary approvals. J. A. Christensen.
 - b. Design and fabricate holder for the six fuel rods for the Halden cask. The holder for the five IFA-431 rods to be designed to minimize possible damage to coiled thermocouple wires. J. A. Christensen.
 3. Ship BMI-1 cask from Columbus, Ohio to RISO. Edlow International.
 4. Ship Rods from RISO, Denmark to Idaho, USA.
 - a. Design, fabricate, and obtain approval for double and single containment hardware for failed and intact plutonia-bearing rods. EG&G. Approvals required by EG&G, PNL, and RISO.

- b. Design and fabricate holder for five PNL IFA-431 rods for BMI-1 cask or assure that holder used in Kjeller to RISØ shipment of PNL rods is adequate for use in BMI-1 cask. J. A. Christensen.
 - c. Determine that heat generation rates in fuel rods do not exceed BMI-1 cask limitations. EG&G.
 - d. Obtain approvals for storage of fuel rods at INEL. EG&G.
 - e. Domestic and international communications, coordination, documentation, and transportation arrangements. Edlow International.
5. Authorization for initiation of cask shipment. R. K. Marshall.
 6. Return of empty BMI-1 cask to Columbus, Ohio. EG&G.
 7. Shipment of unirradiated fuel rods from Halden to INEL. EG&G.
- The goal for completing the shipment is the end of the current fiscal year.

TASK C: DUAL PURPOSE THERMAL PERFORMANCE ASSEMBLY

The objective of this task is to design and fabricate six carefully characterized and instrumented fuel rods for irradiation in Halden followed by additional testing in PBF under postulated accident or off-normal conditions.

Background for Task C

The proposed test program should provide statistically significant data on a number of identical rods irradiated in the same facility. These data are needed to quantify the error band associated with normal operation. Additional work is also needed to extend the data base for gas composition effects. Well characterized data are becoming available for helium and xenon fill gas and data should be forthcoming from gas mixing experiments in the Halden Reactor. Data from recent NRC/PNL tests in Halden indicate xenon has a much stronger impact on fuel temperatures than previously reported experimental results suggested. Gas compositions representing second and third cycle commercial fuel will be investigated. The gas composition parameters in the dual purpose assembly would provide tie points to the gas mixing tests and provide a sound base for interpolation between helium and xenon fill gas.

The PBF program needs well characterized, irradiated, instrumented fuel rods for testing under postulated accident and off-normal conditions. Instrumented fuel rods from the dual purpose assembly will be transferred from the Halden Reactor to PBF for insertion in instrumented test trains. Currently available PBF 4X hardware can be modified to accept the irradiated, instrumented rods. Detachable thermocouple and pressure transducer lead designs are being evaluated by both Halden Project and PNL staff. These rods would also serve to tie the short term PBF tests to a larger source of well characterized steady state data.

The rods from the dual purpose assembly will be available in FY 1979 for PBF testing. The specific tests have not yet been defined but are likely to include LOCA and RIA. The first phase of the test program would provide tie points to the Halden Project portion of the test and address questions such as test bed bias or transportation-induced changes.

Current Progress on Task C

Test parameters for the proposed dual purpose assembly have been identified and are listed in Table 26. Four gas compositions are shown and should provide the needed data to evaluate the effects of gap conductivity on thermal performance. In addition to centerline thermocouples and cladding elongation sensors, the instrumentation package will include bellows transducers to monitor internal rod pressure. These transducers are currently under development by a commercial source and will hopefully be capable of operating for short periods of time under the higher temperature transient conditions as well as under steady state conditions.

A total of 502 fuel pellets have currently been fabricated from 10% U^{235} powder and are ready for shipment to Halden. Geometric densities indicate excellent uniformity with an average value of 95.06% of theoretical and a standard deviation of 0.177%. Additional precharacterization is underway and will include standard moisture, isotopic, impurity analysis, immersion density, thermal resintering behavior, grain size, pore size, microstructure, and thermal diffusivity data.

TABLE 26. Dual Purpose Thermal Performance Test Parameters

• Number of Rods	6
• Heat Rating	12-13 KW/ft peak ^(a)
• Burnup	8000-12000 MWd/MTM ^(b)
• Fuel Density	95% TD
• Enrichment	10% U ²³⁵
• Gap Size	0.009 in. (0.229 mm)
• Fil Gas	3 Rods, Helium (1 atm) 1 Rod, Helium (20-30 atm) 1 Rod, He-Xe (50% of He conductivity) 1 Rod, He-Xe (75% of He conductivity)
• Cladding	Zircaloy-2; 0.5035 in. (12.79 mm) OD
• Instrumentation	12 Fuel Centerline Thermocouples ^(c) 6 Cladding Elongation Sensors ^(c) 6 Bellows Type Transducers ^(c) 7 Neutron Sensors

(a) Lower thermocouple plane will operate at 70% of peak.

(b) Burnup will be adjusted depending on degree of pellet cladding interaction, Halden Reactor schedule, and PBF schedule.

(c) Both thermocouple and pressure transducer leads will be reconnected for PBF tests. Cladding elongations LVDT's will be modified in PBF test train and new neutron sensors will be used.

TASK D: SIXTEEN-ROD TEST ASSEMBLY

The objective of this task is to provide a 16-rod assembly for PBF tests. Criteria and technical bases for the test assembly design will be established and will define such items as (a) the range of test conditions over which the test assembly is to be functional, (b) special material requirements, (c) compatibility requirements (between test assembly and PBF test loop and between test assembly and test fuel), (d) service lifetime, (e) thermal and mechanical loads, and (f) special handling considerations.

Background for Task D

A preliminary design is to be developed first and in sufficient detail to provide a basis for estimating cost of fabricating the test assembly. Product

specifications will also be developed for all components and instrumentation. Component fabricators and instrument suppliers will be identified and joint efforts with instrument suppliers undertaken to develop instrumentation with improved reliability and with special characteristics that may be required for PBF tests. All components and instruments will be tested and evaluated prior to inclusion into the final detailed design.

A detailed design of the 16-rod assembly will be developed and will include working drawings, product and material specifications, and inspection and testing requirements to ensure fabrication is within specifications. A 16-rod test train will be fabricated, assembled, and made available for use in PBF tests.

Current Progress on Task D

No significant progress has been made on this task during the current reporting period.

PRESSURE TRANSDUCER DEVELOPMENT PROGRAM

TASK: DEVELOP PRESSURE TRANSDUCER

The objective of this task is to develop several commercial sources of compact dynamic pressure transducers suitable for use in the Fuel Behavior Research Branch and other RSR programs. To accomplish this objective, Battelle's Pacific Northwest Laboratories (PNL) is developing designs and specifications for high-temperature pressure transducers which can be used under transient conditions in LWR tests and which can be purchased from commercial sources.

Background for Task

Pressure transducers are needed for measuring steady-state and transient fuel rod plenum gas pressures in PBF and Halden tests. A suitable design is to be developed and specifications written in order to obtain prototype models for testing and evaluation. On the basis of these tests, commercial sources will be selected for qualification as potential suppliers of production models.

A search was made to identify companies who produce pressure transducers. Of those identified, a selection was made of the most likely companies who could successfully make pressure transducers or components for in-reactor use. The search revealed 12 companies who commercially produce pressure transducers. The transducers produced are of five types:

- Resistance Strain Gauge
- Linear Variable Differential Transformer (LVDT) - Bellows
- Capacitance Strain Gauge
- Potentiometric - Bellows
- Piezoelectric

All of these devices are basically motion measuring devices with electrical signal output. The basic principle for each device and the advantages and disadvantages for in-reactor use are discussed below. In addition, the resonant cavity system is also discussed.

Resistance Strain Gauges

The strain gauge consists of a strain sensitive filament housed within a small diameter stainless steel tube. The strain filament is insulated from the tube with highly compacted ceramic insulation, which also serves as a strain transfer medium from the housing to the strain filament.

The gauge is bonded to the test specimen and when the test specimen is put into tension or compression the stress is transmitted to the gauge sensing filament. This produces a change in the resistance of the filament which can be measured electrically.

The Canadians successfully used weldable strain gauges in-reactor under limited conditions to measure the circumferential strains of cladding on UO_2 fuel rods.⁽⁶²⁾ The gauges were attached by spot welding the stainless steel gauge flange to the Zircaloy cladding of the fuel rod. Dummy gauges whose output was not affected by the strain of the cladding were attached to each element. The authors stated that the resistance of the strain gauges changed with irradiation particularly during the initial irradiation period. Although the resistance change due to irradiation was compensated for by the use of dummy gauges, they felt that the compensation was only valid for a relatively short time interval (a few hours). The experiments therefore consisted of measuring the apparent cladding strains when the reactor and fuel power were changed in a series of steps. Because of the relatively large overall resistance change due to irradiation the cumulative cladding deformation during the experiment could not be measured and compared to that measured in the hot cell after irradiation. In view of the in-reactor experience of the Canadians, the use of strain gauges cannot be recommended for the long term in-reactor measurement of pressure buildup in fuel rods.

LVDT - Bellows

An LVDT is an electromechanical transducer that produces an electrical output proportional to the displacement of a separate movable core. In its simplest form, three coils are equally spaced on a cylindrical coil form. A rod-shaped magnetic core positioned axially inside the coil assembly provides a path for output which varies linearly with changes in core position.

The LVDT-bellows pressure transducer couples an LVDT to a bellows to sense the axial motion of the bellows caused by changes in the bellows internal pressure. LVDT's are successfully used in the Halden reactor to measure fuel stack and cladding elongation.⁽⁶³⁾ The successful use of LVDT's by the Halden Reactor Project indicates that this type of device coupled to a metal bellows could successfully be used to measure the pressure buildup in a fuel rod during reactor operation.

Capacitance Strain Gauges

The capacitance strain gauge was developed to overcome basic problems associated with the use of resistance strain gauges.⁽⁶⁴⁾ Greater use of resistance strain gauges was restricted because of the following reasons:

- The change in mechanical and electrical properties of materials with temperature.
- The mechanical strains to be measured are usually very small ($<500 \mu\epsilon$) compared to thermal expansion strains (around $13,000 \mu\epsilon$) for typical structural materials.

A cross-sectional schematic of the strain gauge is shown in Figure 78. Note that the gauge is quite small, approximately 1.125 in. long by 0.035 in. wide. Major elements of the gauge are:

FIGURE 78. Capacitive Strain Gauge - Cross Section View

1. A compensating rod, usually made of the same material as the test specimen. This rod establishes the gauge length of the gauge, and provides nominal self-temperature compensation for thermal expansion strains.
2. A pair of cylindrical excitation plates, mounted on, but electrically insulated from the compensating rod.
3. A sensing ring, coaxial with and surrounding the excitation plates.
4. Attachment ribbons for fastening the gauge to the test specimen.
5. Flexures which maintain coaxial alignment of the excitation plates and the sensing ring.

In the capacitor configuration used in the gauge, the gap (~ 0.007 in.) between the excitation and sensing rings stays constant. Changes in capacitance result because more or less area of the sensing ring overlaps the respective excitation rings. The area-changing capacitor generates a linear response to relative motion.

In normal use, the attachment ribbons are attached to the device to which a strain is applied; however, for this application the gauge would be modified and one end of the compensating rod would be attached to a bellows and the sensing ring attachment ribbon attached to a fixed location. Changes in the bellows internal pressure would cause axial movement of the compensating rod which would produce a signal proportional to the fuel rod pressure. The bellows and gauge would be contained within a capsule and could be attached by means of a capillary tube to either end of the fuel rod.

The capacitance strain gauge holds the potential for being a useful in-reactor measurement tool. However, extensive development work would be required before it could be used with confidence in-reactor. The use of air or other gas as a dielectric in measuring the change in capacitance when subjected to the reactor environment is unknown. The intense radiation would have some effect on the properties of the dielectric. How this would affect the gauge output is unknown.

Potentiometric-Bellows and Piezoelectric Transducers

The potentiometric-bellows type transducer operates on the principle of the precision potentiometer in which the variable (the movement of a bellows) is converted into the motion of a contacting brush on a precision potentiometer winding.

The sensing element in piezoelectric transducers is crystalline quartz (silicon dioxide). The movement resulting from the force applied by pressure is detected and counterbalanced by an electromagnetic force which keeps the sensing element near its null position and is a direct measure of the applied force.

We do not have any knowledge of either of these types of devices being used in-reactor; consequently a feasibility study would be required and if results were favorable, extensive development work would be required before using these devices in-reactor.

Resonant Cavity

The resonant cavity system involves an adaptation of a system which the Italians⁽⁶⁵⁾ used to measure in-reactor and the axial elongation of a fuel stack. The measuring device is based on the resonant cavity principle and is shown schematically in Figure 79. A bellows attached to the fuel rod plenum is contained within an enclosure, thus forming the measurement cavity. Another similar cavity (reference cavity) is located out-of-reactor and is attached to a micrometer. A high frequency wave, generated by a Klystron, is sent to both cavities through a wave guide. The measuring cavity back wave is analyzed by an oscilloscope which shows an absorption peak whose displacement is related to the depth of the cavity. The absorption peak of the reference cavity back wave is put in coincidence with the peak due to the measuring cavity with the micrometer. By proper calibration, obtaining the coincidence of the two absorption peaks gives the fuel rod pressure. This system shows promise; however, extensive development work would be required to place it in operation. A primary advantage of the system is that all instrumentation is located out of the reactor environment.

FIGURE 79. Resonant Cavity System for Measuring Fuel Rod Pressure

Additional Transducer Types

Additional information was obtained concerning miniature unbonded strain gage type transducers manufactured by Bell and Howell, and transducers operating on the principle of impedance variation which is caused by eddy currents induced in the target material, manufactured by Kaman Sciences Corporation.

Both these type devices are recommended by the manufacturer as being suitable for in-reactor use; however, no results of actual in-reactor use of either type transducer were found. The main difficulty in using these devices in-reactor during transient tests will be temperature compensation. These transducers are sensitive to temperature changes and during transient tests the temperature differential between the measuring and compensating components possibly could seriously affect the output of the transducer.

Current Progress on Task

The results of the background study indicated that the LVDT-Bellows type of transducer would be the most feasible device for production by a commercial vendor. Of the twelve companies surveyed, only Schaevitz Engineering produced standard LVDT's for use in a reactor environment. Schaevitz also indicated a willingness to help design and build several LVDT-Bellows prototypes. Discussions with Columbia Research Laboratories, Inc. and other potential suppliers indicated a general lack of interest in developing or fabricating transducers for reactor application.

A program to develop design specifications and to fabrication of four prototype high temperature pressure transducers was established with the Schaevitz Company of Pennsauken, New Jersey. The specifications detail the requirements for employing this type of pressure transducer in nuclear reactor environments under dynamic coolant flow conditions where high accumulated radiation dosages are anticipated.

Transducers will be designed to measure pressures over the 0-345 kPa (0-50 psi), 0-6895 kPa (0-1000 psi), and 0-13,790 kPa (0-2000 psi) pressure ranges. The transducers will operate in pressurized water up to 17,237 kPa (2500 psi) and 343°C (650°F). The transducers must survive a temperature excursion to 760°C (1400°F) for one hour. Detailed specification criteria are presented in Appendix A.

A purchase order for the fabrication of two 0-345 kPa (0-50 psi) and two 0-13,790 kPa (0-2000 psi) to the specifications in Appendix A and for one signal conditioner was issued to Schaevitz on September 24, 1976 with a delivery date of January 20, 1977. Material procurement difficulties and fabrication problems caused shipment to be delayed beyond January 20, 1977. The delays were due to procurement of Inconel for the transducer parts and in fabrication of the bellows assembly.

A schematic drawing of the high pressure transducer assembly showing the bellows, LVDT, and containment housing was prepared. A safety feature of this transducer design is double containment so that if failure of the bellows should occur during reactor operation, fission gases from the fuel rod will not diffuse up the instrument leads and into the reactor building. The bellows

assembly is made by welding a stack of Inconel washers at each washer hub and periphery. A problem was encountered in obtaining 100% weld penetration at the weld joints, but was solved by modifying the electron beam welder so that proper adjustment of the beam focus could be obtained. A schematic drawing of the low pressure transducer was also prepared.

PNL personnel visited the Schaewitz plant on March 16 to assess the current status of fabrication. All component parts were fabricated, the bellows assembly had been welded and was in heat treatment. Shipment of the completed transducers was repromised for April 1, 1977.

Qualification tests will be performed at PNL to assure that the welds are leak tight and that the transducers will operate satisfactorily at 760°C (1400°F) and in an autoclave at operating pressures and temperatures. Calibration curves will be obtained under steady-state and transient conditions. The qualification test plan is presented in Appendix B.

Despite concerns over temperature compensation problems, transducers of the unbonded strain gauge or eddy-current principle seem to have sufficient potential to warrant evaluation testing. Eddy-current transducers have also been under consideration by EG&G and they have agreed to provide one of this type along with a transducer of their own design (strain post concept) for inclusion in the PNL Qualification Test Program. An unbonded strain gauge transducer will also be obtained from a commercial source and included in the program to provide an additional basis for comparison.

REFERENCES

1. C. R. Hann, et al., Test Design, Precharacterization, and Fuel Assembly Fabrication for Instrumented Fuel Assemblies IFA-431 and IFA-432. BNWL-1988, Battelle, Pacific Northwest Laboratories, Richland, WA, July 1976. (report draft)
2. C. R. Hann, D. D. Lanning, R. K. Marshall, R. E. Williford and L. J. Parchen, "Experimental Verification of Nuclear Fuel Rod Stored Energy Calculations." Presented at the Enlarged Halden Programme Meeting on Water Reactor Fuel and Core Performance at Sanderstolen, Norway, March 8-12, 1976, and to be published in Proceedings. [This paper is marked for Halden Project Use Only.]
3. C. R. Hann, et al., "Application of Experimental Data to the Reduction of Stored Energy Uncertainty." Fuel Behavior Research Program, Fourth Water Reactor Safety Research Information Meeting at National Bureau of Standards, Gaithersburg, MD, September 30, 1976.
4. C. E. Beyer, C. R. Hann, D. D. Lanning, F. E. Panisko and L. J. Parchen, GAPCON-THERMAL-2: A Computer Program for Calculating the Thermal Behavior of an Oxide Fuel Rod, BNWL-1898, Battelle, Pacific Northwest Laboratories, Richland, WA, November 1975.
5. C. R. Hann, D. D. Lanning, R. K. Marshall, A. R. Olsen, and R. E. Williford, "A Method for Determining the Uncertainty of Gap Conductance Deduced from Measured Fuel Centerline Temperatures." BNWL-SA-6161, February 1977.
6. J. A. L. Robertson, $\int Kd\theta$ in Fuel Irradiations. CRFD-835, Chalk River, Ontario, Canada, April 1959.
7. D. D. Lanning and C. R. Hann, Verification of Fuel Centerline Thermocouple Readings Through Response to Linear Power Decreases. BNWL-2189, Battelle, Pacific Northwest Laboratories, Richland, WA 99352, April 1977.
8. D. D. Lanning and C. R. Hann, "Verification of Fuel Thermocouple Readings During IFA-431 Irradiations." BNWL-SA-2352, Presented at the Enlarged Halden Programme Meeting on Water Reactor Fuel and Core Performance at Sanderstolen, Norway, March 6-11, 1977.
9. R. E. Williford and C. R. Hann, "Effects of Pellet Eccentricity and Fill Gas Composition." (BNWL-SA-6178) Presented at the Enlarged Halden Programme Meeting on Water Reactor Fuel and Core Performance, Sanderstolen, Norway, March 6-11, 1977.
10. C. L. Mohr, D. D. Lanning and F. E. Panisko, "GAPCON-3: A Computer Code to Analyze the Path-Dependent Thermal and Mechanical Performance of Nuclear Fuel Rods." Specialists Meeting on the Behavior of Water Reactor Fuel Elements Under Accident Conditions. Spatind (Nord-Thorpa), Norway, September 13-16, 1976.

11. A. L. Edwards, TRUMP: A Computer Program for Transient and Steady State Temperature Distributions in Multidimensional Systems. UCRL-14754, Rev. 3, Lawrence Livermore Laboratory, Livermore, CA 94550, September 1, 1972.
12. C. R. Hann and R. K. Marshall, Fuel Rod Elongation Measurements at Low Burnup from IFA-431 and IFA-432 Halden Reactor Tests. Battelle, Pacific Northwest Laboratories, Richland, WA 99352, March 1977. (preliminary report)
13. R. E. Williford and C. R. Hann, "Effects of Fill Gas Composition and Eccentric Gap Geometry on Gap Conductance of UO₂ Fuel Rods." BNWL-SA-6150, January 20, 1977.
14. C. R. Hann, C. E. Beyer and L. J. Parchen, GAPCON-THERMAL-1: A Computer Program for Calculating the Gap Conductance in Oxide Fuel Pins, BNWL-1778, Battelle, Pacific Northwest Laboratories, Richland, WA 99352, September 1973.
- 15a. J. A. Dearien, G. A. Berna, M. P. Bohn, D. R. Coleman, P. E. MacDonald and J. M. Broughton, FRAP-S: A Computer Code for the Steady-State Analysis of Oxide Fuel Rods. SRD-52-76, January 1976.
- 15b. J. A. Dearien, G. A. Berna, M. P. Bohn, D. R. Coleman, P. E. McDonald and J. M. Broughton, FRAP-S: A Computer Code for the Steady State Analysis of Oxide Fuel Rods, Volume 1 FRAP-S Analytical Models and Input Manual - INEL Report I-309-13.1, January 1975.
16. C. R. Hann, et al., Fuel Behavior Research Program, First Water Reactor Safety Research Information Meeting, 1973. (paper presented)
17. C. R. Hann, et al., Fuel Behavior Research Program, Second Water Reactor Safety Research Information Meeting, 1974. (paper presented)
18. C. R. Hann, J. A. Christensen, D. D. Lanning, R. K. Marshall and R. E. Williford, "Experimental Verification of Stored Energy Calculations." Fuel Behavior Research Program, Third Water Reactor Safety Research Information Meeting, September 1975.
19. Regulatory Staff, U.S. Atomic Energy Commission, Technical Report on Densification of Light Water Reactor Fuels. WASH-136, November 1972.
20. M. D. Freshley, P. E. Hart, J. L. Daniel, D. W. Brite and T. D. Chikalla, "The Effect of Pellet Characteristics and Irradiation Conditions on UO₂ Fuel Densification." Presented at the ANS/CNA Topical Meeting on Commercial Nuclear Fuel - Current Technology, Toronto, Canada, April 1975.
21. D. W. Brite, J. L. Daniel, N. C. Davis, M. D. Freshley, P. E. Hart and R. K. Marshall, EEI/EPRI Fuel Densification Project Research Project 131. Final Report prepared for Electric Power Research Institute by Battelle, Pacific Northwest Laboratories, March 1975, Revised June 1975.

22. A. Hanevik, K. D. Knudsen and P. Årnesen, "In-Reactor Measurements of Fuel Stack Shortening," Proceedings of the British Nuclear Energy Society Conference. London, England, October 1973.
23. J. A. L. Robertson, Irradiation Effects in Nuclear Fuels. Gordon and Breach, New York, 1967.
24. P. E. Hart and J. L. Daniel, "The Role of Microstructure in Thermal and Irradiation-Induced Densification of UO₂ Fuels." Presented at 77th Annual Meeting of the American Ceramic Society, Washington, DC, May 3-8, 1975.
25. C. R. Hann, S. R. Wagoner and R. E. Williford, "A Comparison of Pretest Predictions with Preliminary Data from NRC/PNL Halden Assembly IFA-431," a paper presented at the Fuel Code Verification Workshop, Fourth Water Reactor Safety Research Information Meeting, September 28, 1976.
26. I. Cohen, B. Lustman and J. D. Eichenberg, "Measurement of the Thermal Conductivity of Metal - Clad Uranium Oxide Rods During Irradiation." J. Nuclear Materials, 3, (3), 1961.
27. G. R. Horn, "The Effects of Xenon on Fuel-to-Cladding Gap Conductivity." Trans. Am. Nucl. Soc., 12, 1969.
28. L. A. Lawrence, G. R. Horn and J. A. Christensen, "Fuel-Clad Gap Conductions in Fast Reactor Fuels." Trans. Am. Nucl. Soc., 13, 1970.
29. D. J. Bradley and C. R. Hann, An Evaluation of the Discrepancies Between Predicted and Experimental Effects of Xenon and Krypton in Nuclear Fuel Rods. BNWL-1955, Battelle, Pacific Northwest Laboratories, Richland, WA, November 1975.
30. I. Cohen, B. Lustman and J. D. Eichenberg, Measurement of the Thermal Conductivity of Metal - Clad Uranium Oxide Rods During Irradiation. WAPD-228, August 1960.
31. C. E. Beyer, E. R. Bradley, C. R. Hann, D. D. Lanning, R. K. Marshall and R. E. Williford, In-Core Pressure Measurements from Halden Test Assembly IFA-431-Interim Report, BNWL-2099, August 1977. (preliminary draft)
32. C. E. Beyer and C. R. Hann, Release of Indigenous Gases from LWR Fuel and the Reaction Kinetics with Zircaloy Cladding. BNWL-1956, Battelle, Pacific Northwest Laboratories, Richland, WA 99352, December 1975.
33. J. Belle, "Properties of UO₂." Proceedings of the Second United Nations International Conference on the Peaceful Uses of Atomic Energy, Geneva, 1958, United Nations, Geneva, 6:585, 1958.
34. W. B. Lewis, The Return of Escaped Fission Product Gas to UO₂. AECL-964 also DM-58, Chalk River, Ontario, Canada, 1960.

35. C. R. Hann, D. D. Lanning, A. R. Olsen, R. K. Marshall and R. E. Williford, Error Analysis of the Gap Conductance for Instrumented Fuel Assembly IFA-431 Irradiated in the Halden Boiling Water Reactor. BNWL-2091, Battelle, Pacific Northwest Laboratories, Richland, WA 99352, August 1976.
36. R. K. Marshall, C. R. Hann, D. D. Lanning, A. R. Olsen, and R. E. Williford, "Gap-Conductance Uncertainties Deduced from Measured Fuel Centerline Temperatures," Trans. Am. Nucl. Soc. 24, (155-156), November 1976.
37. B. Aarset, Calibration Techniques and Data Handling Equipment Instrumented Fuel Assemblies in HBWR. HPR79, OECD Halden Reactor Project, Halden, Norway, December 1967.
38. A. J. Walter, "Thermocouple Calibration: Irradiation and Transmutation Effects." CONF-741226-1, Summary of information presented at the International Colloquium on High Temperature In-Pile Thermometry, Petten, Netherlands, December 1974.
39. F. Mathieu, et al., "Thermoelectrically Generated Noise in Sheathed Thermocouples and in Other Low Level Instrumentation Cables." Annals of Nuclear Energy. 2, (127-140), 1975.
40. M. F. Lyons, et al., UO₂ Powder and Pellet Thermal Conductivity During Irradiation, GEAP-5100-1, General Electric Company, Pleasanton, CA, 1966.
41. C. H. Westcott, Effective Cross-Section Values for Well-Moderated Thermal Reactor Spectra. CRRP-787, Chalk River, Ontario, Canada, August 1958.
42. R. Isnard, "Heat Transfer Coefficient and Some Other Reactor Parameters Determined from Measurements of Fuel Temperature and Neutron Flux Fluctuations." Annals of Nuclear Energy. 2, (193-195), 1975.
43. J. Jaech, Statistical Methods in Nuclear Material Control. TID-26298. Available from Technical Information Center, Oak Ridge, TN 37830.
44. D. D. Lanning and C. R. Hann, "Verification of Fuel Thermocouple Readings by Analysis of Transient Response," Trans. Am. Nucl. Soc., 24, (156-157), November, 1976.
45. C. L. Bennett and W. L. Purcell, BRT-I: Battelle-Revised Thermos. BNWL-1434, Battelle, Pacific Northwest Laboratories, Richland, WA 99352, June 1970.
46. C. L. Mohr, D. D. Lanning, F. E. Panisko, et al., "GAPCON-3: A Computer Code to Analyze the Path-Dependent Thermal and Mechanical Performance of Nuclear Fuel Rods." BNWL-SA-5962, Battelle, Pacific Northwest Laboratories, Richland, WA 99352, August 1976.
47. S. P. Timoshenko, Theory of Elastic Stability, 2nd Edition. McGraw Hill, New York, NY, pp. 1-45, 1961.

48. B. Aarset, In-Core Measurements and Accuracies at HBWR, HPR-111 (July 1969). Halden Reactor Project, Halden, Norway.
49. C. R. Hann and R. K. Marshall, An Analysis of Fuel Centerline Temperature at Low Burnup from IFA-431 and IFA-436 Halden Reactor Tests. (report to be completed in September 1977)
50. J. P. Stora, B. deBernardy deSigoyer, R. Demas, P. Deschamps, B. Lavaud, and C. Ringot, Conductibilite Thermique de l'Oxyde d'Uranium Frite dans les Conditions D'Utilization en Pile, CEA-R-2586, Commissariat a l'Energie Atomique, 1964.
51. R. G. Wheeler, Thermal Conductance of Fuel Element Materials, USAEC Report HW-60343, April 1959.
52. R. A. Dean, Thermal Contact Conductance Between UO₂ and Zircaloy-2, CVNA-127, May 1962.
53. A. M. Ross and R. L. Stoute, Heat Transfer Coefficient Between UO₂ and Zircaloy-2, CRFD-1075 and AECL-T552, 1962.
54. Stewart, et al., "Determination of Thermal Contact Resistance Using a Pulse Technique," Proc. of the 6th Symposium on Thermophysical Properties, P. E. Coley (ed), ASME, New York, 1973.
55. R. E. Taylor and H. J. Lee, Proc. of the 14th Thermal Properties Conference, Thermal Properties Research Council, 1975.
56. J. Wordsworth, "IAMBUS-1: A Digital Computer Code for the Design, In-pile Performance Prediction and Postirradiation Analysis of Arbitrary Fuel Rods," Nucl. Sci. Engr., 31, (309), 1974.
57. R. E. Taylor and H. J. Lee, Proc. of the 14th Thermal Properties Conference, Thermal Properties Research Council, 1975.
58. J. A. Cope and G. W. Lehman, J. Appl. Phys., 34, (1909), 1963.
59. K. B. Larson and Karl Koyama, J. Appl. Phys., 38, (465), 1967.
60. G. Peckham, The Computer Journal, 13, No. 4, November 1970.
61. D. M. Curry and S. D. Williams, "Non-Linear Least Squares--An Aid to Thermal Property Determination," MSC-07048, NASA TM X-58092, June 1972.
62. M. J. F. Notley, M. J. Pettigrew and H. Vidal, Measurements of the Circumferential Strains of the Sheathing of UO₂ Fuel Elements During Reactor Operation. AECL-4072, Chalk River, Ontario, Canada, January 1972.

63. K. Schenk, Development of Differential Transformers for Measurement of In-Core Fuel Elongation. HPR70, OECD Halden Reactor Project, Halden, Norway, January 1967.
64. D. R. Harting, "A System for Measuring Static Strains to 1500°F." Presented at the ISA Meeting, Philadelphia, PA, May 1975.
65. Calza Bini, A. G. Cosoli, G. Filacchioni, M. Lanchi, A. Nobili, V. Rocca, and P. L. Ratoloni, "In-Pile Direct Measurements of Axial Variation in Fast Reactor Pins." Presented at the ANS International Meeting on Fast Reactor Fuel and Fuel Elements, Karlsruhe, Germany, September 1970.

APPENDIX A

SPECIFICATION FOR LVDT-BELLOWS TYPE
PRESSURE TRANSDUCERS OPERATING IN
A NUCLEAR ENVIRONMENT

APPENDIX A

SPECIFICATION FOR LVDT-BELLOWS TYPE PRESSURE TRANSDUCERS OPERATING IN A NUCLEAR ENVIRONMENT

SCOPE

This specification presents the requirements for pressure transducers of the Linear Variable Differential Transformer (LVDT) - bellows type for measuring absolute pressures. In this type, the bellows is directly attached to the transformer core of the LVDT. The LVDT senses the motion of the bellows caused by a change in pressure and provides an electrical output signal linearly proportional to the change in pressure.

This specification details the requirements for employing this type of pressure transducer in nuclear reactor environments under dynamic coolant flow conditions where high accumulated radiation dosages are anticipated.

A summary of the design requirements is shown in Table A.1.

ENVIRONMENTAL CONDITIONS

The transducers will be subjected to flowing pressurized water up to 2500 psig and 650°F and must be designed to withstand a total integrated neutron flux of 3×10^{20} n/cm² without detrimental effects. At the end of some tests the transducers will be subjected to temperatures as high as 1400°F for a maximum time of 1 hr.

OPERATING CONDITIONS

A pressure transducer will be attached to an experimental fuel rod and operated in a test reactor to measure the internal pressure of the fuel rod during reactor operation. The attachment of the transducer to the fuel rod will be by means of an extension tube attached to one end of the transducer.

The pressure transducers will be designed to measure low, intermediate, and high pressures. The low pressure transducers will measure pressures

TABLE A.1. Summary of Requirements for LVDT-Bellows Type Pressure Transducers Operating in a Nuclear Environment

Type

LVDT-Bellows for measuring absolute pressure

Environmental Conditions

Water: Pressurized to 2500 psia at 650°F, maximum flow velocity-20 ft/sec.
Temperature excursion to 1400°F for 1 hr. at end of testing period.

Thermal neutron flux - Total integrated flux of 3×10^6 n/cm²

Shock and vibration - Those induced by pressurized water flowing at 20 ft/sec shall be a design objective

Operating Conditions

Pressure measurement range	0-50 psi
(Range to be specified in purchase order)	0-1000 psi
	0-2000 psi
Frequency response - design objective	<1 millisecond

Construction

Materials	All inorganic
Case	All welded and hermetically sealed. Extension cables welded to case. Internally evacuated to provide absolute pressure measurements
Maximum volume of transducer measuring volume	0.070 in ³ - design objective
Pressure Port	Tube: 1/4 in. OD x 0.095 in. wall x 6 in. long
Maximum external dimensions (exclusive of pressure port and extension lead attachment)	
OD	0.75 inches design objective but not to exceed 1.0 inches
Length	3 inches design objective but not to exceed 5 inches
Length of extension cable	As specified in purchase order

between 0-50 psi, the intermediate between 0-1000 psi, and the high pressure transducers between 0-2000 psi. The overall arrangement of the test system is shown in Figure A.1.

The system will operate in the following manner. At the start of each test cycle, the pressure in the transducer will increase over a period of 1 or 2 days as a result of an increase in environment temperature to a value within the range of the transducer. As the test cycle proceeds, the pressure is expected to increase then to decrease and then increase again. During this time, a number of cycles (<100) will occur in which the pressure will decrease from the peak value to less than one half of the peak value and then will return to the peak value for a continued change in pressure. This is the normal mode of operation in which the pressure changes are gradual. Special tests will be run in which, as a result of reactor operation, the pressure in the transducer drops 20% in 1 min and then increases 20% in 1 min. As many as 10 of these tests could be performed during the lifetime of the transducer. At the end of some tests the transducers will be subjected to temperatures as high as 1400°F for a maximum time of 1 hr.

FIGURE A.1. Schematic of Fuel Rod Pressure Measuring System

The gas in the fuel rod and transducer is helium. At the beginning of the test it is pure, but as the test progresses it gradually becomes contaminated with fission product gases.

The expected in-reactor operating time for the transducers is 1 yr.

The high temperature and high pressure water in which the transducers operate is turbulent and moving at high velocities; consequently, flow induced vibrations and shocks can be expected.

CONSTRUCTION DETAILS

The transducer assembly consists of the transducer attached to extension cables which extend to the junctions as shown in Figure A.1. The materials of construction in both transducers and cables must be entirely inorganic.

Transducer Construction Details

The internal components of the transducers must be enclosed in an all welded hermetically sealed stainless steel case to withstand the environmental conditions. The transducer must be evacuated and sealed in such a manner to provide absolute pressure measurements. The bellows can be internally or externally pressurized. The volume within the transducer occupied by the gas whose pressure is being measured should be less than 0.070 in.³ (design objective). The selection of materials and construction must be such as to minimize temperature related null shifts.

The transducers will be used in an in-reactor experimental assembly in which space is at a premium. The envelope dimensions for the transducers are 1 in. OD or less and up to 5 in. long exclusive of extension cable attachment and pressure port.

Extension Cable Construction Details

The extension cables must also minimize space, be flexible enough to extend from the transducer to a location outside the pressure vessel, and must withstand the same environmental conditions as the transducer. Single, five-conductor, 1/8-in. dia stainless steel sheathed cable 50 ft long would be appropriate for each transducer.

The extension cables must be welded and not silver-brazed to the transducer.

TESTS

Each transducer shall be autoclaved at 2500 psi and 650°F for 24 hr. The loop and insulation resistance of the coils shall be measured while the transducer is at pressure and temperature. Two transducers shall be tested at 1400°F for 1 hr in air at atmospheric pressure.

All seal welds shall be tested with a helium mass spectrometer leak detector. A leak rate in excess of 2×10^{-8} standard cm^3/sec of helium shall be used as a basis for rejecting a weld.

The loop resistance, insulation resistance, and integrity of each extension cable shall be determined before attaching to the transducer. The sheath integrity can be determined as follows. Weld both ends of the sheath or otherwise seal. Clean the sheath and externally pressurize to approximately 1000 psi for 5-10 min. Insert within 2 hr into a test chamber, evacuate and test for the presence of helium using a helium mass spectrometer leak detector. An indication of helium shall be taken as evidence of a leak and the extension cable shall be rejected.

CALIBRATION

The effect of thermal expansion of the components on the output of the LVDT shall be determined for each transducer. Each transducer shall be heated from room temperature to 650°F with the pressure port open to atmospheric pressure. One transducer shall be heated to 1400°F. The transducer output shall be plotted versus temperature to provide a temperature effect curve.

Each pressure transducer shall be calibrated with an accurately known source of gas over the full range of the pressure transducer. A calibration curve of transducer pressure versus transducer output shall be provided for each transducer.

INSTRUMENTATION

This specification does not specify the requirements for the instrumentation. However, the transducer design must account for the influence of the long cable runs and connection between the in-reactor transducer and the input and output instrumentation as shown in Figure A.1.

APPENDIX B

QUALIFICATION PLAN FOR LVDT-BELLOWS
TYPE TRANSDUCERS

APPENDIX B

QUALIFICATION PLAN FOR LVDT BELLOWS TYPE TRANSDUCERS

Reference: Specifications for LVDT - Bellows Type Pressure Transducers
Operating in a Nuclear Environment

The qualifications of the pressure transducers will be in accordance with the reference specification.

A. CONSTRUCTION

1. Make dimensional measurements to assure that transducer meets maximum envelope requirements.
2. Evacuate transducer and test seal welds with a helium leak detector. Leak rate shall be less than 2×10^{-8} standard cm^3/sec of helium.
3. Measure the internal volume of the transducer using a calibrated gas manifold. Design objective is 0.070 in^3 .

B. TESTS

1. Transducer

Autoclave transducer at 2500 psi and 650°F for 24 hr. The loop and insulator resistance of the coils shall be measured while at pressure and temperature. The insulation resistance shall be >5000 megohms. The loop resistance shall show no loss of continuity. Heat transducer to 1400°F and hold for 1 hr and retest as above.

2. Extension Leads

Seal transducer port and extension lead. Clean sheath and externally pressurize to approximately 1000 psi for 5-10 min. Insert within 2 hr into a test chamber, evacuate and test for the presence of helium using a helium mass spectrometer leak detector. An indication of helium shall be taken as evidence of a leak.

C. CALIBRATION

1. Determine the effect of thermal expansion by heating the transducer from room temperature to 1400°F with pressure port open. Plot LVDT output versus temperatures.
2. Generate the following calibration curves:
 - a. Heat transducer in autoclave at 2500 psi and 650°F. Apply internal pressure over specified range and plot pressure versus LVDT output.
 - b. Heat transducer to 1100°F and 1400°F at atmospheric external pressure. Apply internal pressure over specified range for each temperature level and plot pressure versus LVDT output.

DISTRIBUTION

<u>No. of Copies</u>		<u>No. of Copies</u>	
<u>OFFSITE</u>		<u>ONSITE</u>	
1	A. A. Churm ERDA Chicago Patent Group 9800 S. Cass Avenue Argonne, Illinois 60439	1	<u>ERDA Richland Operations Office</u> Program Division H. E. Ransom
245	Basic Distribution Under NRC-1	34	<u>Battelle-Northwest</u> W. J. Bailey J. O. Barner S. Begej D. W. Brite T. D. Chikalla J. A. Christensen E. L. Courtright M. D. Freshley J. E. Garnier S. Goldsmith C. R. Hann (5) R. S. Kemper L. L. King D. D. Lanning R. K. Marshall R. P. Marshall C. L. Mohr F. E. Panisko P. J. Pankaskie A. M. Sutey D. S. Trent S. R. Wagoner R. E. Williford C. L. Wilson Technical Information Files (5) Technical Publications
3	M. Jinks ERDA Technical Information Chief Mail & Files US NRC Central Files Washington, DC 20555		
61	Supplementary NRC-3 Distribution List		
2	Nuclear Regulatory Commission Division of Reactor Safety Research Chief, Fuel Behavior Research Branch Washington, DC 20555		