JUN 7 1982

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**PATHFINDER ATOMIC POWER PLANT
TECHNICAL PROGRESS REPORT**

OCTOBER 1961 - DECEMBER 1961

Submitted to
U. S. ATOMIC ENERGY COMMISSION
NORTHERN STATES POWER COMPANY
and
CENTRAL UTILITIES ATOMIC POWER ASSOCIATES
by

**ALLIS-CHALMERS MANUFACTURING COMPANY
ATOMIC ENERGY DIVISION
Milwaukee 1, Wisconsin**

Ref: AEC Contract No. AT(11-1)-589

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ALLIS-CHALMERS MANUFACTURING COMPANY

Under
Agreement dated 2nd Day of May 1957, as Amended
between
Allis-Chalmers Mfg. Co. & Northern States Power Co.
under
AEC Contract No. AT(11-1)-589

April 20, 1962

Classification - UNCLASSIFIED

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ACNP-62005

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TECHNICAL PROGRESS REPORT
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CONTENTS

	Page Number
Distribution	5
List of Figures	9
Foreword	13
Design Data	14

PART A - PRECONSTRUCTION RESEARCH AND DEVELOPMENT

1. Fuel Element Research and Development	
1.1 Fuel Material Cladding, Bonding, and Irradiation Testing	17
1.2 Heat Transfer and Fluid Flow	18
1.4 Fuel Element Manufacturing Research and Development	36
1.5 Nuclear Handling Tools	38
1.6 Low-Enrichment Superheater Fuel Element	39
2. Reactor Mechanical Studies	
2.4 Control Rods, Guide Tubes, and Control Rod Drives	77
3. Nuclear Analysis	
3.1 Reactor Physics (Statics)	81
3.2 Reactor and System Dynamics	84
3.3 Critical Experiments	93

PART B - POST CONSTRUCTION RESEARCH AND DEVELOPMENT

1.0 Initial Stability and Performance Test	101
4.0 Mechanical Tests	108

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WAS INTENTIONALLY
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FIGURES

Number	Title	Page Number
1.1	Two-phase frictional pressure-drop multipliers for 81-pin test section at 600 psia	20
1.2	Permissible reactor power as a function of inlet-cooling-water flow rate and temperature	25
1.3	Reynolds Number (flow regime) of steam in inner channel as a function of steam flow rate and temperature	25
1.4	Turbulent-to-laminar heat-transfer-coefficient ratio as a function of Reynolds Number	27
1.5	Total superheater-core heat loss as a function of mean bulk steam temperature at exit of superheater fueled region with steam flow rate (percent of full flow) and pressure as parameters	27
1.6	Total heat loss from non-fueled superheater region as a function of mean bulk steam temperature at exit of fueled region with steam flow rate and pressure as parameters	28
1.7	Superheater moderator (n , γ) heat generation as a function of mean bulk steam temperature at exit of fueled region with steam flow rate and pressure as parameters	30
1.8	Superheater bulk steam temperature during reactor startup	31
1.9	Superheater bulk steam temperature as a function of superheater full-power days (low-enrichment element)	42
1.10	Dysprosium poison effectiveness as a function of thermal-flux time (low-enrichment element)	44
1.11	Superheater power fraction as a function of fraction of full-power days (low-enrichment element)	45
1.12	Low-enrichment superheater fuel test assembly	46
1.13	Test section for measurement of epi-cadmium U-238 absorptions	49

Number	Title	Page Number
1.14	Components of cadmium covered pellet stack with U-238 test foil	49
1.15	Components of aluminum-covered pellet stack with U-238 test foil	49
1.16	Low-enrichment superheater fuel bundle (compact lattice) with cadmium covered pellet stacks	50
1.17	Low-enrichment superheater fuel bundle (expanded lattice) with cadmium covered pellet stacks and spacer collars	50
1.18	Relative U-238 activations in the 7-rod flooded and voided 3.5-per-cent enriched lattices	53
1.19	Properties of swaged stainless steel UO ₂ fuel rods	57
1.20	Swaged fuel-rod cladding	58
1.21	Typical samples of draw-bonded rods at reductions in area of 2.0 to 6.5 per cent	64
1.22	Density of draw-bonded rods as a function of reduction in area	64
1.23	Stretch-bonding fixture with fuel rod installed	66
1.24	Fuel rod with brazed ferrule gripping fixture	66
1.25	X-rays showing pellet separation as a function of reduction	66
1.26	Typical vacuum brazed tee-joint interface with Coast Metal 60 alloy, 60 min. at temperature	66
1.27	Typical welds with recessed cup-cap	71
1.28	Typical weld recessed cup-cap	71
1.29	Reduced shoulder cap	73
1.30	Typical weld with reduced shoulder cap	73
3.1	Safety valve chattering test	85
3.2	Reactor response to a step change in P ₂ set point P ₂ + T ₂ control	87

Number	Title	Page Number
3.3	Ion-chamber support and grid structure	90
3.4	In-core ion chamber instrumentation block diagram and ion chamber	92
3.5	Core layout for rodged temperature coefficient measurement	94
3.6	Critical facility full-core temperature coefficient- rodged case	95
3.7	Core layout for unrodged temperature coefficient measurement	96
3.8	Critical facility full-core temperature coefficient- unrodged case	97
1.01	Oscillator rod and drive	102, 103, 104

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FOREWORD

This is one of a series of reports covering technical progress on the research and development program being performed in connection with the design of the Pathfinder Atomic Power plant. This plant will be located at a site near Sioux Falls, South Dakota and is scheduled for operation in June 1962. Owners and operators of the plant will be the Northern States Power Company of Minneapolis, Minnesota.

The U. S. Atomic Energy Commission, through Contract No. AT(11-1)-589, with Northern States Power Company, and Central Utilities Atomic Power Associates* (CUAPA), are sponsors of the research and development program.

Allis-Chalmers Manufacturing Company of Milwaukee, Wisconsin, under contract with Northern States Power Company, is performing the research, development, and design; and will construct the plant including the reactor, which is designated the Controlled Recirculation Boiling Reactor (CRBR) with Nuclear Superheater. Pioneer Service and Engineering Company of Chicago, Illinois is providing the architect-engineer services to Allis-Chalmers. Portions of the R & D program, particularly in connection with fuel development, have been subcontracted by Allis-Chalmers.

*CUAPA MEMBER COMPANIES:

Central Electric and Gas Company
Interstate Power Company
Iowa Power and Light Company
Iowa Southern Utilities Company
Madison Gas and Electric Company

Northern States Power Company
Northwestern Public Service Co.
Otter-Tail Power Company
St. Joseph Light and Power Co.
Wisconsin Public Service Corp.

DESIGN DATA

CRBR WITH NUCLEAR SUPERHEATER

Plant

Power, boiler region.....157,200 kw
Power, superheater region.....42,400 kw
Steam flow at rated power.....616,125 lbs/hr
Total core power.....199,600 kw
Gross electrical capability.....66,000 kw
Net electrical output.....62,000 kw
Net efficiency.....31.0 per cent
Steam outlet pressure (reactor).....535 psig
Reactor operating pressure.....600 psig
Temperature, boiler region.....489 F
Outlet temperature, superheater region.....825 F
Gross heat rate.....10,199 Btu/kw-hr
Reactor building size.....50 ft dia x 120 ft

Reactor

Vessel size (overall).....11 ft 6 in o.d. 36 ft: 1 in
Total core dimensions.....6 ft x 6 ft
Dimensions of superheater region.....6 ft x 30 in
Fuel, boiler (Zr-2 clad).....approx. 2.2 per cent enriched UO₂
Fuel, superheater (S.S. clad).....approx. 93 per cent enriched UO₂
Fuel, loading, boiler (U-235).....145.6 kg
Fuel, loading, superheater (U-235).....42 kg
Power density (boiler core coolant).....87 kw/liter
Average heat flux, boiler region.....128,000 Btu/hr-ft²
Average heat flux, superheater region.....77,800 Btu/hr-ft²
Maximum heat flux, boiler region.....462,000 Btu/hr-ft²
Maximum heat flux, superheater region.....245,000 Btu/hr-ft²
Recirculation rate.....65,000 gpm
Recirculation pump power.....823 kw
Neutron flux.....approx. 5×10^{13} n/cm² sec

1. FUEL ELEMENT RESEARCH AND DEVELOPMENT

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1.1 FUEL MATERIAL CLADDING, BONDING, AND IRRADIATION TESTING

The objectives of this project are as follows: 1) to perform laboratory work on the material and thermal properties of boiler fuel elements in the following two general areas: (a) fuel cladding testing and investigations as required, and (b) complete aluminum capsule irradiation proof-testing together with pre- and post-irradiation examinations; 2) to develop a high-enrichment uranium dioxide stainless steel cermet fuel element clad with stainless steel or a suitable alternate material, which includes development of means of maintaining alignment and spacing of fuel bearing tubes and double-walled insulating tubes, development of conceptual designs for the steam entry and exit section supporting the element, evaluation of assembly techniques, and fabrication of a steam corrosion-erosion test loop and testing of superheater fuel cladding material; and 3) to develop means of inserting burnable poison in the reactor core, as required, and to perform studies and research and development on in-core instrumentation for the boiler region flux and coolant flow distribution and for superheater region temperature, flux, and coolant flow.

1.1.1 STEAM-CORROSION-EROSION TESTS ON STAINLESS STEELS

The preliminary test in the corrosion-erosion loop was completed. Evaluation of samples indicated a corrosion penetration of 0.01 to 0.12 mils/year. Operation of the loop was satisfactory.

The first comprehensive test of 304, 304L, 316, and 316L stainless steel samples was begun. Samples will be removed every three weeks for evaluation, as described in the last quarterly report. Test conditions in the superheated steam section are 670 psi, 720 F, and 250 fps. In addition, 30-ppm oxygen, and moisture are introduced in the saturated section, as described in the last quarterly report (ACNP-6123).

1.2 HEAT TRANSFER AND FLUID FLOW INVESTIGATIONS

Objectives of this project are to operate an experimental apparatus (heat transfer loop) to measure heat transfer coefficients and two-phase pressure drops for a full-sized coolant channel. Installed power capacity of the test loop shall be such that it will allow testing of 1) one fuel coolant channel at a heat flux of one million Btu/hr-ft², or 2) one-half of a fuel channel at a heat flux of two million Btu/hr-ft². The maximum design pressure and temperature shall be 1500 psi and 596 F.

Tests will be performed to determine 1) dimensional stability of fuel elements by thermal cycling, 2) pressure drop and vibration tendencies at various flow rates, 3) heat transfer coefficients under simulated Pathfinder operating conditions, and 4) the ability of a superheater element to cool itself by radiation and convection under conditions equivalent to fission product heating with no forced steam flow and with no steam flow.

1.2.1 HEAT TRANSFER LOOP

The electrical bus work was changed to allow use of a smaller M-G set. This set will be used for superheater tests.

An oscillographic recorder with six channels has been added to the loop instrumentation. This recorder will be used to monitor transient temperatures and pressures in the loop.

1.2.2 BOILER FUEL ELEMENTS

1.2.2.1 16-Pin Tests. The 16-pin test section was rebuilt and will be used to complete void determinations after current superheater tests are completed.

1.2.2.2 81-Pin Pressure-Drop and Void-Distribution Tests. The two-phase pressure-drop data obtained from the 81-pin test section were reduced to friction factor multipliers (Φ_{LO}^2). Pressure drops were measured for each 9-in length of the 6-ft long test section both

isothermally and with boiling, and the ratio of the two was taken to give the desired multipliers. A plot of ϕ_{LO}^2 as a function of local steam quality is shown in Figure 1.1. These tests were run at mass velocities of 1.5×10^6 to 2.0×10^6 lb/hr-ft². Exit steam qualities ranged from 0 to 3.5 per cent, and heat fluxes ranged as high as 125,000 Btu/hr-ft².

Also shown in Figure 1.1 are curves representing the modified Martinelli relationships. The agreement is only fair, and the data exhibit an excessive amount of scatter (± 60 per cent). On examination of the test section following the test runs, some metal burrs were found on the pressure taps. These burrs may have been responsible for the large scatter in the data.

Two-phase multipliers measured on the 16-pin test section, which were reported in Allis-Chalmers Report ACNP-6119, were in good agreement with the modified Martinelli values, and had much less scatter in the data points (± 25 per cent). Since the major difference between the two sets of data was the amount of scatter present and not the magnitude, it was concluded that the variation in array size had little effect on ϕ_{LO}^2 .

Two void fraction measurements were obtained just downstream from the center spacer. These values, which were averaged over the array cross-section, are shown in Table 1.1. The void fractions fall on either side of the modified Martinelli correlation used in the design calculations, and are considered to agree within the limits of experimental accuracy. Additional measurements at higher steam qualities will be obtained in the 16-pin test section measurements.

Figure 1.1 Two-phase frictional pressure drop multipliers for 81-pin test section at 600 psia. Dotted curves denote modified Martinelli correlation. (A-C Dwg. 43-024-941)

TABLE 1.1 VOID FRACTION IN 81-PIN TEST SECTION

Experimental Value	Local Steam Quality	Modified Martinelli Value
0.27	1.26	0.306
0.28	0.95	0.255

1.2.3.2 Superheater Heat Transfer Tests. The superheater and isothermal test sections were completed and delivered on schedule, and the testing program began in November. The test section was operated at mass velocities of 0.25×10^6 to 0.75×10^6 lb/hr-ft² with heat fluxes as high as 0.75×10^5 Btu/hr-ft².

After approximately 40 hr of operation, both test sections were removed and dismantled. Both were in very good condition. The test sections were assembled. The final test run is planned for early in January. The heat-transfer and pressure-drop data already obtained are being reduced.

1.2.3.3 Surface Temperatures of High-Enrichment Superheater during

Normal Startup. Summary. Normal reactor startup procedures were examined to ensure that allowable superheater-fuel surface temperatures are not exceeded. It has been established that the maximum clad temperatures never exceed 1270 F, the maximum full-power temperature for 725-F outlet steam. An ample margin of safety with respect to tolerable fuel temperatures, conservatively considering associated steam conditions, is provided during all phases of the startup procedure.

The reactor startup procedure consists of five distinct phases: Phase I - Nuclear Warmup; Phase II - Superheater Draining; Phase III - Approach to 20% Steam Flow; Phase IV - Reactor Pressurization; and Phase V - Conversion of Rod Configuration.

Completion of the above procedure takes the reactor from a zero superheater coolant flow to a 20-per-cent steam flow (100 per cent

equals 616,125 lb/hr) and from the decay or zero power level to 20-per-cent reactor power level (100 per cent equals 188.9 mwt).

Phase I - Nuclear Warmup. Procedure. During nuclear warmup, water is circulated through both the boiler and superheater coolant channels with the recirculation system operating at full flow. The cooling water circulated through the superheater coolant channels consists of 300-gpm recirculation water (420 F) and 40-gpm from the seal water booster pumps (100 F) giving a mixed inlet temperature of 380 F. The system pressure is held by nitrogen at 550 psia (478 F saturation) throughout Phase I.

All of the superheater control rods and the eight boiler control rods nearest the superheater are fully inserted. The power fraction in the superheater during this phase, as evaluated by physics considerations, is 8.4 per cent of total core power. The calculated radial power factor in the superheater is 2.02 with a 10-per-cent uncertainty.

Thermal Analysis. The thermal analysis of the superheater during nuclear warmup included the evaluation of both laminar and turbulent flow conditions. Tolerable clad surface temperatures are obtained even if laminar flow should occur in two of the three annular coolant channels of each superheater fuel element.

The maximum superheater surface temperature permitted during this phase is 453 F (25 F less than the saturation temperature). Adequate margin to prevent bulk or local boiling in the superheater at inlet water temperatures of 380 F is provided by not allowing the reactor power to exceed 7.25 mwt.

Other superheater inlet-coolant-water flow rates and temperatures were examined with respect to safe superheater power levels during the nuclear warmup period. As Figure 1.2 indicates, a power up to 10 mwt is permissible during nuclear warmup with slight adjustment in inlet water conditions.

Phase II - Superheater Draining. Procedure. Upon reaching the desired 550-psia and 420-F water temperature in the first phase, the reactor is shutdown and the draining operation is begun. The pressure falls rapidly to 315 psia, and continued draining permits flashing at the top of the reactor vessel. The draining terminates when the desired water level is attained by blowdown, and the reactor is brought to criticality using the outer eight boiler rods. Additional reactivity is inserted until a power level of 10 kwt is reached assuming no decay heat. With decay heating, this step is eliminated. (It should be noted that the 10-kwt power value is considerably below the thermal capabilities of either the superheater or the boiler at zero-flow conditions.)

Thermal Analysis. The draining operation introduces no excessive thermal condition in either the superheater or boiler. The ability of the superheater fuel element to safely dissipate the heat generated at reactor powers up to 2.6 mwt under conditions of zero steam flow with moderator temperature (sink temperature for radiation and conduction heat dissipation) of 489 F, 615 psia has been demonstrated experimentally.

Phase III - Approach to 20% Steam Flow. Procedure. At the beginning of Phase III, a steam-flow equivalent of 1 per cent of full flow is established through the superheater, and the reactor power is then brought up to the level where 1 per cent of steam flow is generated in the boiler (about 1 per cent of full power, i.e., ~2 mwt). The radial factor increases to 2.18 ± 10 per cent, and the superheater power fraction increases to 6.96 per cent. Any drop in the reactor pressure resulting from the independent control of steam flow and power is expected to be small. All three recirculation pumps are in operation. The feedwater inlet temperature is maintained at a temperature not lower than 100 F.

After establishing a steady-state condition, the steam flow is increased to 2.5 per cent of full flow, and the power is increased to about 2.5 per cent of full power. Similar increases in steam flow and reactor power are made until a steady-state condition of 20 per cent of full steam flow is attained (about 20 per cent reactor power). Reactor pressure is maintained at 315 psia during the entire phase.

Thermal Analysis. Operation of the superheater at the low steam-flow rates required for startup introduces two major areas of design uncertainty: 1) non-turbulent flow conditions, and 2) superheater heat losses, which make calculation of surface temperature and determination of correlations between surface temperature and bulk steam temperature difficult.

Figure 1.2 Permissible reactor power as a function of inlet-cooling-water flow rate and temperature. (A-C Dwg. 43-024-942)

Figure 1.3 Reynolds Number (flow regime) of steam in inner channel as a function of steam flow rate and temperature. (A-C Dwg. 43-024-940)

During startup, laminar, transition, or turbulent flow through the superheater fuel elements may occur. As Figure 1.3 shows, the steam flow rates during reactor startup cover all three flow regimes.

The heat transfer film coefficient, h , and the friction factor, f , are significantly affected in the region of non-turbulent flow by such physical factors as wire spacers, uneven heating in the annuli, variable fluid properties, etc., which affect the velocity and temperature profiles. Without experimental data for the conditions of interest, an exact prediction of surface temperature and bulk steam temperature is not possible.

However, these temperatures may be calculated using a turbulent-flow model, and an estimate of the maximum error resulting from its use in the laminar and transitional flow region is possible.

Figure 1.4 shows a plot of the ratio of turbulent heat transfer coefficient to the laminar heat transfer coefficient as a function of Reynolds Number. As expected, the largest ratio, 3, occurs at the highest Reynolds Number, 10,000. For Reynolds Numbers from 10,000 to 350, the agreement between the laminar and turbulent heat transfer coefficients becomes better. They are equal at Reynolds Number of 350. For Reynolds Numbers below 350, use of the turbulent flow model results in an increasingly conservative error.

Contrary to the indications of Figure 1.4, however, the turbulent model is accurate at Reynolds number of 10,000, as experimental data reported by McAdams have shown. Therefore, the error in the transition

Figure 1.4 Turbulent-to-laminar heat-transfer-coefficient ratio as a function of Reynolds Number. (A-C Dwg. 43-024-939)

Figure 1.5 Total superheater-core heat loss as a function of mean bulk steam temperature at exit of superheater fueled region with steam flow rate (percent of full flow) and pressure as parameters. (A-C Dwg. 43-024-979)

TOTAL HEAT LOSSES FROM SUPERHEATED STEAM BETWEEN
FUELED CHANNEL OUTLET AND CONSOLE METER THERMOCOUPLE

Figure 1.6 Total heat loss from non-fueled superheater region as a function of mean bulk steam temperature at exit of fueled region with steam flow rate and pressure as parameters. (A-C Dwg. 43-202-589)

range is much less than a factor of three, and cautious application of the turbulent flow model for all flow regimes can be considered with appropriate uncertainty factors.

In calculating the bulk steam temperature rise during Phase III, the steam flow rate is virtually unaffected by the selection of friction factor correlation, because of the parallel pressure-drop coupling among hot and average channels. Therefore, the bulk temperature rise prediction, using the turbulent model, is accurate for the laminar and transitional flow range. An important factor in making the calculation, however, is heat loss from the reactor to the surroundings and from the superheater to the boiler. At the low steam flow rates, the sensitivity of the bulk steam temperature to heat loss increases markedly.

Figure 1.5 and 1.6 show the heat losses from the active superheater fueled region and non-fueled region, respectively. Figure 1.7 presents the (n, Y) heat generated in the superheater moderator, which in turn leaks into the boiler region. The influences of the steam flow rate (convection film resistance) and system pressure (moderator or sink temperature) on the heat losses are shown.

At no time during Phase III operation does the fuel surface temperature in the superheater exceed the maximum 1270-F temperature expected at full power. The maximum surface temperature calculated for the superheater fuel during the approach to 20% steam flow is 950 F. This reduction in fuel surface temperature at these low powers from the full-power and flow value is due primarily to the lower superheater power fraction. The control rod pattern in the reactor shields

Figure 1.7 Superheater moderator (n, γ) heat generation as a function of mean bulk steam temperature at exit of fueled region with steam flow rate and pressure as parameters.
(A-C Dwg. 43-024-943)

the superheater reducing its power fraction from the full power 0.1668 value to 0.0696 during this low-power period.

The bulk outlet steam temperatures (measured at the steam-line thermocouple) from the superheater were calculated over the flow rate range from 5 to 20 per cent. Curve A of Figure 1.8 is this expected temperature trace as the reactor is brought up to 20% steam flow. The maximum bulk outlet temperature is 535 F, which occurs at the 20 per cent of full-flow condition.

The steam temperatures and flow rates which could give rise to the 1270-F fuel surface temperature for Phase III operation appear as Curve B of Figure 1.8. As Curve B indicates, the steam temperature

Figure 1.8 Superheater bulk steam temperature during reactor startup. Curve A is the expected temperature during Phase II. Curve B is the permissible steam-line-thermocouple temperature with all rods in at 315 psia. Curve C is the expected temperature during Phase IV. Curve D is the permissible steam-line-thermocouple temperature with all rods in at 615 psia. Curve E is the expected temperature during Phase V. Curve F is the expected temperature as steam temperature approaches reference. Curve G is the permissible steam-line-thermocouple temperature with all rods out at 615 psia. (A-C Dwg. 43-202-590)

could rise to 583 F at the 20 per cent flow point before the 1270-F fuel surface temperature is attained. Therefore, a considerable margin of safety is provided. A power increase of 40 per cent without any increase in steam flow would be required to attain the 1270-F limiting surface temperature. Furthermore, any conceivable reduction in the inlet feedwater temperature will not produce intolerable steam conditions.

Phase IV - Reactor Pressurization. Procedure. Pressurization of the system from 315 psia to 615 psia (reference) begins after steady-state reactor operation at about 20 per cent of full steam flow is attained. The regulating dump valve is closed slightly, which results in a step reduction in steam flow rate, and an immediate step increase in the pressure drop across the steam line. The reactor power is held constant at approximately 20 per cent, and the reactor dome pressure consequently increases. The bulk steam temperature at the superheater exit also increases.

The reactor dome pressure continues to increase until its magnitude is sufficient to drive the total amount of steam generated in the boiler through the dump valve. The system, then, is again at the steady-state with approximately the same steam flow rate and bulk steam temperature as existed before closing the dump valve. The system pressure, however, is slightly higher.

Reductions in steam flow are made in steps of less than 10 per cent of the steady-state, 20-percent flow rate. The control rod configuration remains as in Phase III with an associated radial power

factor and superheater power fraction of 2.18 ± 10 percent and 6.96 percent, respectively. Feedwater temperatures are increased gradually during this phase until a temperature of 350 F at a pressure of 615 psia is reached.

Thermal Analysis. During pressurization of the system from 315 to 615 psia, the steady-state maximum fuel surface temperature is 920 to 945 F. The stepwise steam flow reductions cause the surface temperature to increase briefly by 90 F. However, this temperature drops within 50 sec to the steady-state temperature.

The bulk steam temperature from the superheater varies from 535 F at 315 psia to 563 F at 615 psia during this period. Again a brief period of slightly higher bulk steam temperatures occurs immediately after the movement of the control valve. This rise is damped effectively reaching only 590 F at its maximum and returning to the steady-state temperature after 50 sec.

Curve C of Figure 1.8 shows that the expected bulk temperatures as system pressure is increased to 615 psia are well below the limiting values for 315 psia. A superheater power increase of 44 per cent at 615 psia without any increase in steam flow is required to attain a maximum fuel surface temperature of 1270 F. The inlet feedwater temperature would have to be reduced from 350 F to about 100 F before the excessive steam temperature conditions are reached.

Curve D of Figure 1.8, which represents the limiting (1270-F surface temperature) steam conditions for 615 psia system pressure show that a bulk steam temperature of 622 F is required before the 1270-F surface temperature at this 20-percent steam flow is exceeded.

Phase V - Conversion to Rod Configuration. Procedure. With the reactor system pressure established at 615 psia and operating at 20 per cent of full power and flow at steady-state, adjustment of the control rods begins. The outer eight control rods in the boiler are inserted, and the eight inner control rods in the boiler are withdrawn to hold the steam flow and power at about the 20 per cent level.

During the adjustment of the boiler control rods, the superheater control rods remain fully inserted. An increase in superheater fractional power to 0.0923 has been calculated for this step. The radial power factor in the superheater remains at 2.18 ± 10 per cent.

Thermal Analysis. The higher superheater power fraction causes the bulk steam temperature from the superheater to increase as Curve E of Figure 1.8 indicates, the bulk steam temperature increases from 563 F to 592 F during control rod adjustment. These steam temperatures are still below the limiting 622-F temperature value as shown by line D, Figure 1.8.

During rod manipulation, the maximum superheater surface temperature is 1050 F. To attain the maximum permissible 1270-F value, an increase of 22 percent in the superheater power is required without any increase in steam flow. An inlet feedwater temperature reduction from 350 F to 100 F, a remote possibility, is required before the superheater temperature becomes excessive.

Conclusion. Following Phase V, the superheater control rods are withdrawn to increase the superheater power fraction while providing reactivity compensation with the boiler rods. The superheater power

fraction is increased until the reference 725 F outlet steam temperature condition is reached (Curve F, Figure 1.8). The limiting temperatures at 615 psia with all superheater control rods withdrawn which is shown as Curve G, Figure 1.8, is well above 725-F.

The reactor protection system is, of course, in operation throughout the course of the startup period. Thorough analysis of possible transients during this period has shown all such transients to be less severe than similar transients initiated at higher power levels (i.e. more than 20 per cent). It should be noted that two of the steps in the startup procedure require control rod manipulation while holding power approximately constant. These steps are being examined further so that power distributions associated with intermediate rod configurations for short periods can be taken into account.

1.4 FUEL ELEMENT MANUFACTURING RESEARCH AND DEVELOPMENT

Objectives of this project are: 1) to develop or specify methods for production of fuel assemblies for the reactor core; and 2) to fabricate and examine sample boiler and high-enrichment superheater fuel assemblies (or portions thereof) to evaluate their suitability for repetitive fabrication to design specifications.

Development will include fuel fabrication, clad fabrication, fuel loading and sealing, assembly weldments, and intermediate and final inspection procedures, as required.

1.4.1 HIGH-ENRICHMENT SUPERHEATER FUEL ELEMENT

1.4.1.1 Fabrication and Assembly. The development of a hot-roll technique for reducing the sintered cermet strip to final fuel cermet thickness was completed. Some 18-in long sections were fabricated and evaluated to determine their compliance with preliminary specifications. (Previous use of a cold-roll process had produced unacceptable fuel tubes.)

The uranium dioxide powder spheroidizing process was further developed so that dense spherical powder particles are obtainable in the 54- and 88- μ size range. This study is being continued.

Destructive and non-destructive evaluation of fuel tubes made with the hot-rolled cermet and containing various weight per cents of uranium dioxide (21, 17.5, and 17.2 per cent) and employing different ranges of uranium dioxide particles (44-54, 44-88, and 54-88 μ) have shown the following results.

1) Stringering was essentially eliminated and maximum particle elongation was approximately 2-1/2-to-1 (ratio between the major to minor diameter).

2) A complete random mixing of the UO_2 powder particles and stainless steel particles was obtained.

3) The fuel tubes have been non-permeable with the exception of some of the earlier tubes which showed an occasional leak through a cermet-to-cermet seam. These seams were prepared by shearing the cermet edges. Later tubes were prepared by machining the cermet edges which eliminated this seam leakage problem.

4) The bond between the cermet and clad is acceptable.

5) Radiography of the cermet strips have shown no detectable inhomogeneities.

Preliminary hot forming of the cermet strip into tubes has shown no appreciable advantage over the cold forming.

As a back-up effort to this entire process, several fuel tubes were isostatically pressed. Preliminary information indicates that the only noticeable difference was an increase in density of approximately 1 per cent. In conclusion, the hot-roll process appears to be a satisfactory process for producing the Pathfinder superheater fuel elements. Further development work will employ the hot-rolling technique in developing complete full-length fuel element assemblies.

1.5 NUCLEAR HANDLING TOOLS

Objectives of this project are to perform conceptual engineering and experimental work for development of simplified but reliable fuel handling tools, and special tools for repairing reactor parts.

The hydraulic actuation system for the boiler core handling tools was replaced with an air system, which is simpler and less expensive.

A capsule is being designed for handling ruptured fuel elements. Such a fuel element will be loaded into the capsule in the shield pool. The fuel element will be stored and shipped in the capsule.

An underwater facility was designed for cutting the boiler and superheater control rods prior to shipping. The facility will include a commercial air-driven hacksaw.

Some equipment for testing the handling tools in the underwater handling facility was designed and is being built.

1.6 DEVELOPMENT OF LOW-ENRICHMENT SUPERHEATER FUEL ELEMENTS

The objectives of this project are to determine the economic merit of using low-enrichment superheater fuel, to determine and select a promising fuel element design, and to develop a low-enrichment superheater fuel element through applicable studies, fabrication experiments, and testing.

At such a time as a promising fuel element concept has been selected, a full-scale mock-up will be fabricated including supports and steam entry and exit section, and flow tests will be performed to determine pressure drop and dimensional stability at simulated reactor operating conditions.

Heat transfer calculations will be performed. A test element will be fabricated for insertion in the heat transfer loop to perform heat transfer experiments under simulated Pathfinder operating conditions. Physics calculations will be performed, such as the required computer programming and operation, determination of the critical mass, neutron and gamma flux and power distribution, enrichment, coefficients of reactivity, control rod effectiveness, and conversion ratio.

A partial loading of prototype low-enrichment elements will be fabricated and tested in the critical facility in conjunction with the remainder of the superheater section of the core containing high-enrichment fuel elements. Safeguards reports will be prepared for the facility as required to meet the licensing requirements of the AEC. Critical experiments will be conducted to determine the nuclear characteristics of the prototype low-enrichment superheater fuel elements.

1.6.1 NUCLEAR DESIGN

The principal effort during the past quarter was performance of a PDQ-RZ depletion study of a low-enrichment superheater core. For the study, a superheater fuel element consisting of a cluster of seven 0.234-in o.d. uranium dioxide fuel rods of 4.5 at/o U-235 enrichment was assumed. The superheater burnable poison assumed was B-10 homogeneously distributed in the center fuel pin of the element.

For purposes of depletion studies, the superheater was represented by nine regions chosen so as to minimize the flux variation within each region. In each region, the group cross sections were spatially constant changing stepwise at each time step. The stainless steel axial

spacer plate in the superheater and the baffle between the superheater and boiler were represented explicitly. The superheater control rods were completely withdrawn throughout life. Both radial and axial fuel and void distributions were represented in the boiler. However, the fuel within each boiler region was assumed fixed with superheater depletion. The average irradiation in the boiler was taken to be 5000 mwd/t, which is representative of the equilibrium boiler. Core criticality was achieved at each time step by the insertion of a homogeneous poison in the boiler.

The nuclear constants for each of the nine superheater regions at each time step were obtained using the BAD code. The BAD calculations (hence the RZ-PDQ calculations) assumed two groups with uranium, plutonium, and B-10 reaction cross sections represented explicitly in both groups. A thermal cross section, after corrections for thermal spectrum hardening and cell flux dipping, of 75 barns was assumed for the long-lived fission products. Fast and slow microscopic cross sections from MUFT and SOFOCATE, and thermal-group disadvantage factors from I_2 were computed for the beginning-of-life and for approximate end-of-life cell compositions. The beginning- and end-of-life microscopic cross sections and disadvantage factors were linearly averaged. These average values were then used in the depletion calculations. RZ calculations were performed at 0, 100, 200, 300, and 400 full-power days.

Table 1.2 lists the full-power days, superheater peak-to-average power, superheater average burnup, and superheater peak burnup predicted by this study for each time step. The peak-to-average power remains

relatively constant with depletion due to the buildup of plutonium and the relatively stable flux distribution in the superheater throughout life. The peak-to-average values in Table 1.2 are not corrected for additional peaking caused by the explicit representation of the superheater control rod channels and superheater boiler interface in XY geometry. Calculation of these total XYZ power peaks are currently in progress using the techniques described in ACNP-6123.

Figure 1.9 is a plot of the superheater bulk steam temperature as a function of superheater full-power days. The bulk steam temperature reaches a maximum of 830 F at about 6,000 mwd/t falling to 795 F at 10,000 mwd/t with an additional drop of about 10 F per 1000 mwd/t to the end of the study. For a 12,000 mwd/t lifetime, the total bulk-steam temperature swing is 55 F. Further optimization of this superheater (reduction in bulk-steam temperature swing with burnup) could be obtained by slight adjustments in the U-235 enrichment and B-10 content.

Dysprosium has been under consideration for use as a burnable poison in the low-enrichment superheater. To this end, fast- and slow-group microscopic cross sections compatible with a low-enrichment superheater flux spectrum were developed for each dysprosium isotope. For the fast group, resonance integrals were computed by means of the Adler-Hinman-Nordheim method^{1,2} using resonance parameters from the supplement to the second edition of BNL-325. The thermal cross sections were developed

1. GA-350, "The Quantitative Evaluation of Resonance Integrals", F. T. Adler, G. W. Hinman, and L. W. Nordheim
2. GA-377, "Tables for the Composition of Resonance Integrals", F. T. Adler and L. W. Nordheim

Figure 1.9 Superheater bulk steam temperature as a function of superheater full-power days (low-enrichment element). (A-C Dwg. 43-024-960)

TABLE 1.2 LOW-ENRICHMENT SUPERHEATER R-Z DEPLETION STUDY
(0.234 in. dia., fuel, 4.5% enriched, B-10 poison)

Time Step (Days)	Power (Peak-to-Average)	Depletion (mwd/t)	
		Average	Peak
0	1.487	0	0
100	1.424	2860	4070
200	1.424	6010	8560
300	1.423	9190	13080
400	1.419	12170	17270

based on published data.^{3,4} Table 1.3 summarizes the computed resonance integrals and the fast-group average cross sections appropriate to a low-enrichment superheater flux spectrum. The Dy-164 resonance integral is a measured value.⁵ Spectrum average thermal cross sections are listed in Table 1.4. The end products in Table 1.4 are included to allow for the formation of holmium and erbium. The small effect of Dy-160 was not included.

Assuming the dysprosium to be homogeneously mixed with the center fuel pin, these cross sections were used in low-enrichment superheater depletion calculations. Figure 1.10 is a plot of the dysprosium poison effectiveness as a function of thermal flux time assuming flux spectrum and disadvantage factors characteristic of the 0.234-in/4.5-a/o-U-235 fuel element. The thermal and fast contribution to the total poison effectiveness are also shown in Figure 1.10. The substantial fast contribution is due to the large fast-to-slow flux ratio in the low-enrichment superheater. For this superheater element, a burnup of 10,000 mwd/t occurs at about 8×10^{20} nvt. From Figure 1.10, the poison residual at this burnup is 78 per cent of the initial value, a very low swing in poison effectiveness. Additional calculations have shown that the shape of the total-poison-effectiveness curve is not sensitive to reasonable variations in the values assumed for the resonance integrals. The high residual is due to the slow depletion through the dysprosium chain and the end-products cross section.

3. Transactions of the American Nuclear Society, Vol. 2, No. 2

4. Transactions of the American Nuclear Society, Vol. 3, No. 2, Paper 25-4

5. ANL Reactor Physics Constants Center Newsletter No. 1

Figure 1.10 Dysprosium poison effectiveness as a function of thermal-flux time (low-enrichment element). (A-C Dwg. 43-024-959)

TABLE 1.3 DYSPROSIUM FAST-GROUP CROSS SECTIONS

Isotope	Resonance Integral (barns)	Fast-Group Cross Sections (barns)
161	1500	53.5
162	1700	60.7
163	2100	75.0
164	450 (measured)	16.1

TABLE 1.4 DYSPROSIUM THERMAL-GROUP CROSS-SECTIONS

Isotope	Thermal-Group Cross Section (barns)
161	329
162	132
163	121
164	1240
End Products	150

The effect of the dysprosium depletion shape on the superheater power fraction is illustrated in Figure 1.11. Here, the power fraction for the 0.234-in/4.5-a/o superheater is plotted versus lifetime assuming both B-10 and Dy as the burnable poison. The poison contents were chosen to yield a beginning-of-life power fraction of 21 per cent. Dysprosium does not appear to be a promising burnable poison for the low-enrichment superheater. However, with large fuel rod sizes, hence lower enrichment and higher conversion ratios, and longer irradiation times, dysprosium would be more attractive as a burnable poison than is indicated in Figure 1.10.

Figure 1.11 Superheater power fraction as a function of fraction of full-power days (low-enrichment element). (A-C Dwg. 43-024-958)

During the coming quarter, studies to determine the more suitable type and location of burnable poison for the low-enrichment superheater will be conducted. The use of gadolinium will be examined. The results of experimental measurements of the U-238 resonance escape probability and the cell thermal disadvantage factors for a variety of low-enrichment lattices are being incorporated into the design analytical model. At the time this incorporation is completed including the latest mechanical and thermal information, a second round of low-enrichment superheater calculations will be initiated.

1.6.2 CRITICAL EXPERIMENTS

1.6.2.1 ρ^{28} Measurements. Introduction.

During the quarter, experiments were conducted to measure ρ^{28} for the seven-rod 3.5-per-cent fuel bundle (Figure 1.12). The ratio, ρ^{28} , is defined as the ratio of radiative capture in U-238 above the cadmium cutoff energy to radiative capture in U-238 below the cadmium cutoff energy. This ratio is used to

Figure 1.12 Fuel Test Assembly.
(AC Photo 215378)

experimentally determine the resonance escape probability in U-238, p^{28} , by the relation, $(1 - p^{28})/p^{28} = p^{28} f R$ ⁽⁶⁾, where, f is the thermal utilization of the lattice, and R is the ratio of radiative thermal capture in U-238 to total thermal capture in the fuel.

The thermal utilization, f , of the lattices is calculated using the region-wise flux values reported in ACNP-6123 and the appropriate volumes and cross sections. The U-238-to-total thermal-capture ratio, R , is obtained using published cross sections of the fuel ingredients averaged over the thermal spectrum by the SOFOCATE routine. The experimental determination of p^{28} , then, is dependent on the measurement of the epi- and sub-cadmium radiative capture ratio and the sub-cadmium fluxes in the fuel, moderator, and other cell components.

The following describes the experiment and gives results of the p^{28} measurements. The calculation of p^{28} for these lattices is in progress.

Experimental Methods. The radiative neutron capture in U-238 yields the following reaction:

where, the energy level of the γ emitted by U-238 in decaying to Np²³⁹ is 73.6 kev. This radiation, which is proportional to the radiative capture in U-238 is used to obtain measurements of p^{28} . The method by which this is accomplished is to irradiate cadmium-covered and uncovered

6. Nuclear Science and Engineering, 3, 403 - 429 (1958)

depleted U-238 and measure the subsequent 73.6-kev radiation. Highly depleted uranium is used also to monitor integrated exposure, which is used to normalize the data for the cadmium-covered and the uncovered runs.

Thus, two types of pellet packages are used. The cadmium-covered pellet package, which is used to measure epi-cadmium U-238 radiative capture, consists of a highly depleted uranium foil (3 to 5 ppm U-235) sandwiched between 0.174-in thick oxide pellets identical to the fuel-tube pellets. This package is surrounded by a 0.020-in thick cylinder of cadmium and is capped by 0.020-in thick cadmium. A layout and photograph of the package are shown in Figures 1.13 and 1.14.

The "uncovered" pellet package, which is used to measure the total U-238 radiative capture, is identical to the cadmium-covered pellet package except that the cadmium components are replaced by aluminum. The aluminum is necessary to displace the same amount of water as the cadmium for the flooded-lattice case, but does not act as a filter. Components of this package are shown in Figure 1.15.

In Figures 1.13 through 1.15 aluminum catcher foils are shown on each side of the depleted U-238 foil. Initially these were used to catch the fission products from the neighboring fuel pellets. Since the photographs were taken, however, it was discovered that the catcher foils were increasing the activity of the cadmium covered foils by about one per cent per mill of the aluminum foil thickness, and of the uncovered foils by about half of one per cent per mill. This increase is due to streaming of neutrons through the aluminum foil into the

Figure 1.13 Test section for measurement of epithermal U-238 absorptions. (A-C Dwg. 43-024-844)

Figure 1.14 Components of cadmium covered pellet stack with U-238 test foil. (A-C Photo 215549)

Figure 1.15 Components of aluminum-covered pellet stack with U-238 test foil. (A-C Photo 215550)

Figure 1.16 Low-enrichment superheater fuel bundle (compact lattice)
with cadmium covered pellet stacks. (A-C Photo 215377)

Figure 1.17 Low-enrichment superheater fuel bundle (expanded lattice)
with cadmium covered pellet stacks and spacer collars. (A-C Photo 215380)

depleted U-238 foil, which in effect increases the surface area of the depleted foil. This phenomena is particularly pronounced with small fuel pins where the surface to mass ratio is large. As a result the catcher foils were eliminated and correction for the fission product activity is being made. This correction was experimentally determined, and amounts to less than one per cent for the uncovered foils, and zero for the cadmium-covered foils, where no fissions take place in the pellet package.

The pellet packages are inserted in the test assembly (Figures 1.16, 1.17), which is inserted in a central location in the reactor surrounded by at least 5-in of similarly loaded fuel elements. The pellet packages are inserted such that there is continuity of fuel in the lattice except for the cover and foils. The perturbation caused by the pellet packages is therefore kept to a minimum. Foils of highly depleted uranium are placed in the lattice to monitor the integrated exposure for normalization.

Three lattices are used for the experiment, which corresponds, to lattices for which the thermal-utilization measurements were made (ACNP-6123). Two of the lattices, lattices B and D, are shown in

TABLE 1.5 FUEL PIN SPACINGS
FOR TEST LATTICES

Lattice	Radius of Fuel Rod Centers
B	0.730 cm
C	0.825 cm
D	0.953 cm

Figures 1.16 and 1.17, respectively. The spacings for the lattices are summarized in Table 1.5.

With the test section and monitors in place, the foils are irradiated at 100 watts for 45 min, are removed after shutdown, and counting is initiated 5 min after shutdown.

The 73.6-kev activities are measured with a scintillation spectrometer consisting of a 2 by 2 in. NaI(Tl) crystal mounted on a 6292 photomultiplier connected to a cathode follower, non-over-loading linear amplifier, pulse-height analyzer, gated scaler, and a decimal printer. The pulse-height analyzer is set at a base line of 70 kev with a window width of 10 kev. The equipment resolution of the U-239 73.6-kev gamma is about 15 per cent.

Timing is accomplished by using the output of a 1000-cps (± 0.002 -cps) tuning-fork oscillator in a pre-set counter that gates the counting of the scaler. Each foil counting period is 60 sec ± 0.00012 sec, and 11.2 sec is allowed for change of sample. The foils are counted for 10 cycles, which is approximately 3 half-lives for a 3-foil (plus three-monitor-foil) measurement.

The data obtained from the counting is corrected for a background as measured prior to the irradiation with the same spectrometer settings. The resulting activities are then checked for half-life. (No systematic deviations from the expected 23.5 min half-life were found in any of the data counted in this manner.)

The data for each foil is fit by least squares to a 23.5-min half-life curve and corrected back to the shutdown time activity. For a given irradiation, this yields the relative activity of the U-238 in the test foils at shutdown. Normalization between irradiations is accomplished by using the corrected activities of the monitor foils which are placed in the lattice such that changes in the pellet packages do not affect their activities.

Figure 1.18 Relative U-238 activations in the 7-rod flooded and voided 3.5-per-cent enriched lattices. (A-C Dwg. 43-025-903)

TABLE 1.6 VALUES OF ρ^{28}

Lattice		Outer Fuel Rods	Center Fuel Rods
B	Voided	$2.79 \pm .06$	$2.53 \pm .06$
	Flooded	$1.62 \pm .04$	$1.56 \pm .04$
C	Voided	$2.77 \pm .06$	$2.59 \pm .06$
	Flooded	$1.65 \pm .04$	$1.72 \pm .04$
D	Voided	$2.63 \pm .06$	$2.62 \pm .06$
	Flooded	$1.62 \pm .04$	$1.65 \pm .04$

Results. The determination of p^{28} involves the measurement of the total and epi-cadmium captures. The ratio p^{28} is obtained as follows:

$$p^{28} = \frac{\text{epi-cadmium captures}}{\text{total minus epi-cadmium captures}}$$

The measured capture rates as a function of lattice spacing are plotted in Figure 1.18. The values for the outer pins were obtained by averaging the activities in the two measured outer pins and are considered to be representative for all outer pins.

The values for p^{28} calculated from the data are presented in Table 1.6. The errors indicated with the values given in Table 1.6 are estimated from counting statistics, data spread, and errors associated with the experimental apparatus. It should be noted that, since p^{28} is proportional to $(1 - p^{28})/p^{28}$, errors in $(1 - p^{28})$ are approximately proportional to errors in p^{28} . However, for a p^{28} of approximately 0.9, errors of one per cent in p^{28} result in an error of only about one-tenth per cent in p^{28} .

1.6.3 FUEL FABRICATION RESEARCH

Investigations are being conducted to develop economical methods of fabricating high-density uranium dioxide fuel rods with limited interconnected porosity for good heat transfer and retention of fission gases. Work during this quarter continued on fuel elements produced by swage-compaction, vibratory compaction, and by collapsing clad over sintered pellets.

1.6.3.1 Swage-Compacted Fuel Rods. Introduction. Preliminary work had shown that a mixture of 75 w/o arc-fused and 25 w/o ceramic grade

powders yielded swaged elements with a density of about 90 per cent of theoretical. A second group of elements produced with this mixture of powder resulted in densities of 86 per cent of theoretical. The lower densities apparently were a result, at least in part, of over-swaging. Therefore, during the past quarter, studies were conducted to determine the effect of the amount of swaging reduction and also to study effects of pre-swaged density.

Effect of Pre-Swaged Density and Amount of Reduction. Twenty fuel rods were fabricated with all processing variables kept constant except the amount of reduction, which was varied from 30 to 61 per cent. The tubing used was 316L stainless steel of 0.325 in. o.d. and with 0.010 in. thick wall. The rods were vibrated on a pneumatic air vibrator to a density of 59 to 62 per cent of theoretical density. The rods were swaged at a rotational speed of 35 rpm while being fed into the swager at 2.7 in/min.

The density of fuel in the completed rods was determined by geometric measurements as follows. Three 1-in sections were cut from each rod, at each end and in the center. The ends of the sections were ground parallel, and the lengths of the sections were measured to 0.0001 in. and weighed. The UO_2 was leached from the sections with nitric acid, and the remaining stainless steel was weighed. The difference in weight was used to determine the mass of the UO_2 . The total volume was calculated from the measurements. The volume of the stainless steel was calculated by assuming a density of 7.98 gm/cm^3 for stainless steel. The difference between total volume of the section

and calculated stainless steel volume was used as the UO_2 volume. The density of each section was then calculated from these mass and volume figures. (Some measurements made with a pycnometer indicated a density 1 per cent higher, which indicates that the densities given in this study may be slightly higher.)

Wall thickness measurements were made of the top, middle, and bottom sections of cladding from which the UO_2 had been removed by nitric acid. Photomicrographs of the inside surface of the cladding were made.

Results of the study are given in Table 1.7 and Figure 1.19. Photomicrographs of the inside surface of the cladding are shown in Figure 1.20.

In all cases but three, preswaged density of 59 to 62 per cent of theoretical could be obtained with 5 min of vibration. Rod No. 1, which required the longest time to obtain the specified density, also resulted in the greatest deviation in properties after swaging. This deviation in the vibration characteristics of the powder may more generally indicate the subsequent quality of the swaged rod, and the vibrating characteristic may be advantageously used as a quality - control criterion. If the powder were removed from the tube and salvaged at this stage, production costs and quality would be improved considerably.

The curves of test results shown in Figure 1.19 indicate very little change in elongation with reductions in area of 40 to 43 per cent. Fabrication of more rods at these reductions but with different powders

WALL THICKNESS
INCHES

-57-

AV. DENSITY % T.D.

Figure 1.19 Properties of swaged stainless steel UO₂ fuel rods.

30

41

52

61

Cross Section - Magn: 100X

Reduction
(Per Cent)

Inner Surface - Magn: 70X

Figure 1.20 Swaged fuel-rod cladding.

is necessary to verify this conclusion. If the conclusion is generally valid, a wider range in parameters would be permitted in obtaining fuel elements of uniform length.

The data clearly shows the optimum reduction of the 75-25-w/o mixture to be 41 to 46 per cent. Reductions below and above these values yield under or overswaging. The density variation along the rod appears to be more than desired. Some of this may be due to variation in powder particle size distribution and the loosening of powder at the top end of the tube after vibrating but before swaging.

Table 1.7 and Figure 1.19 show the wall thickness and variation in wall thickness of the completed rods. The data shows the decrease in wall thickness with increases in reduction. The photomicrographs of Figure 1.20 clearly show the increase in wall damage with reductions about 40 per cent. Thus, from the standpoint of tube wall damage, the lowest reduction possible is desirable.

Work is in progress to study the effects of the particle size reduction during vibration on pre-swaged density. Another series of fuel rods will be produced to establish the optimum parameters using arc-fused powders to obtain a higher swaged density.

1.6.3.2 Vibratory Compaction of Fuel Rods. Due to the attractiveness of producing fuel rods by vibratory compaction from the standpoint of economy and simplicity, studies of this method were initiated. The same type and size of stainless steel tubing used in the previous study was used. Initially arc-fused powders of the same particle size-distribution used by other investigators was used for the study.

TABLE 1.7

PROPERTIES OF VIBRATORY COMPACTED AND SWAGED UO₂ FUEL RODS

Rod No.	Vibr. Time (min.)	Vibrated Density (% TD)	Swage Red. (%)	Swaged Density (% TD)	Variation in Rod Density (%)	Elog. (%)	Wall Ave. (in.)	Thickness Variation (in.)
1	9	59.58	30.36	81.47	8.60	27.86	12.4	0.0024
2	5	61.11	30.42	81.57	3.14	8.80	12.2	0.0006
3	4	59.78	33.06	86.25	1.15	11.69	11.6	0.0020
4	5	60.94	60.90	86.36	0.67	93.09	10.3	0.036
5	5	59.67	32.58	86.56	2.48	12.25	12.5	0.0010
6	5	60.93	32.98	87.62	1.66	13.19	12.0	0.0012
7	5	60.07	40.80	87.02	1.17	31.10	11.4	0.0024
8	5	59.77	40.57	86.70	1.16	32.39	10.7	0.0018
9	5	61.20	41.37	88.72	0.74	28.97	12.1	0.0032
10	5	62.55	41.32	88.08	3.23	29.55	11.8	0.0014
11	5	59.18	42.79	88.49	1.94	29.48	11.9	0.0020
12	5	59.35	42.76	88.14	2.90	30.00	11.9	0.0014
13	5	60.21	45.63	88.63	1.54	38.82	11.5	0.0028
14	5	60.66	45.70	88.47	1.27	39.74	11.0	0.0012
15	6	62.23	47.25	86.88	3.85	49.20	10.5	0.0022
16	5	61.74	47.18	88.88	2.27	46.16	10.9	0.0018
17	5	61.19	49.70	87.31	1.37	53.20	10.9	0.0023
18	5	60.64	49.72	85.92	2.40	52.12	10.5	0.0022
19	5	60.61	52.45	85.85	1.10	58.68	10.3	0.0032
20	5	62.37	52.09	86.52	3.27	62.22	10.0	0.0014

NOTES:

1-1/4 in. piston vibrator was operated at 80 psi.

Swager was operated with rotational speed of 35 rpm and feed speed of 2.7 in./min.

Mesh size - 75 w/o Spencer arc fused, as received, and 25 w/o Davidson 64 hr. ball milled ceramic grade powder.

Surface appearance of all rods was very good.

TABLE 1.8 PROPERTIES OF VIBRATORY COMPACTED UO₂ RODS

Mix No.	Particle Size (1)		Vibrating Time (Min.)	Density % TD
	Before Vibrating	After Vibrating		
	Mesh Fraction	%		
1	-6 + 8	50	10	83.1
	-8 + 12	12	20	83.1
	-12 + 20	20	30	83.2
	-20 + 200	--	40	83.7
	-200	18		
2	-8 + 20	68	10	78.4
	-20 + 100	12	20	81.0
	-100 + 170	--	30	83.2
	-170 + 400	20	40	83.2
	-400	--		
3	-10 + 16	60	10	85.8
	-16 + 70	--	20	86.0
	-70 + 100	15	30	86.2
	-100 + 200	--	40	86.4
	-200	25		
4	-3 + 16	60	10	83.3
	-16 + 70	20	20	83.8
	-70 + 200	--	30	83.8
	-200	20	40	84.0

(1) U. S. Standard Sieve Size

Fuel rods were vibrated at various conditions. Results are given in Table 1.8. In no case could densities as high as those reported in the literature for this process be obtained with the smaller super-heater rods. The maximum density attained was 86 per cent of theoretical density with a 1-1/4-in piston vibrator. Almost all rods vibrated to the maximum density in approximately 10 min. Considerable reduction in particle size was obtained during vibration.

1.6.3.3 Clad Bonding Studies. Clad bonding studies are being conducted towards development of a 0.220 in. o.d. low-enrichment super-heater fuel element loaded with sintered ceramic pellets. During the last period, series of rods were draw-bonded and stretch-bonded. Results indicate that both processes can be used to produce a good fuel-to-clad bond. Additional work is required, however, to optimize parameters and fixturing and to determine collapsing properties of clad and fuel in full 6-ft length fuel rods.

Draw-Bonding. Initial attempts to simulate fuel pellet stacks with steel mandrels for this study were not satisfactory due to seizing and galling on the mandrel as soon as slight contact was made. Consequently, the studies were completed using stacks of fuel pellets.

The first experiment using draw-bonding on fuel pellets was performed to study effects of the process on pellet and clad integrity. Four rods were drawn to obtain reductions in area of 2.0, 3.5, 5.0, and 6.5 per cent. Evaluation showed that fuel-clad

contact ranged from partial contact with the low reduction to severe pellet damage with the highest reduction (Figure 1.21). Pellet separation was not excessive and was not detected by x-ray even in rods at the highest reduction. Rod density, however, was a maximum at 3.5 per cent reduction in area (Figure 1.22). This is probably due to a redistribution of fuel that occurs with severe cracking at high reductions. In no case did noticeable wall damage occur.

The next phase of the drawing program will involve studying the effects of draw lubricant and draw speed on rod straightness, pellet damage, and rod integrity.

Stretch-Bonding. The stretch-bonding process differs from draw-bonding in that rod diameter is reduced by elongation only, without such compression forces as are exerted by a die in draw-bonding. By using a mandrel, it is possible to maintain a compression load on the pellet stack during stretching and thus maintain intimate contact between pellets.

A fixture was built and installed on the draw bench (Figure 1.23). This unit has a mandrel to maintain pressure on the pellet stacks. Brazed external ferrules on the ends of the clad tube were used to grip the rods during stretching (Figure 1.24).

A series of rods were drawn. The rods were loaded with both steel pellets and fuel pellets. Experiments showed that stress concentration around the brazed gripping ferrules was sufficiently high to cause failure in the cladding in the draw end before fuel-clad contact was made on the entire length. The brazed ferrule grip on the draw end was replaced

Figure 1.21 Typical samples of draw-bonded rods at reductions in area of 2.0 to 6.5 per cent.

Figure 1.22 Density of draw-bonded rods as a function of reduction in area. Values were calculated assuming a theoretical fuel-plus-stainless-steel density of 10.54 g/cc. (A-C Dwg. 43-024-962)

with a welded end cap, and the fixture was modified to pull the end-cap by means of a pin. This arrangement consistently provided the strength required to complete the cycle. With the end-cap, mandrel pressure on the pellets could be imposed on one end only, but this was not detrimental to initial program objectives.

Using the above method, a series of four rods were prepared and stretched to elongations of $3/4$, $7/8$, and 1 in. in 29 in. Results indicated that contact with pellets was obtained at $3/4$ in. elongation. Higher elongations resulted in increasingly greater pellet separation with no apparent gain in rod density (Figure 1.25).

The experiment showed that stretching is a feasible means of producing contact between fuel and clad. Work during the next quarter will be directed toward refining the gripping fixture to permit use of double mandrels and establishing process parameters on short lengths of rods. Trial runs will be conducted on full 6-ft length fuel rods.

1.6.4 FUEL-BUNDLE ASSEMBLY FABRICATION DEVELOPMENT

Investigations are being conducted to develop methods for producing accurately spaced high-integrity seven-rod bundle assemblies for Pathfinder. A critical design review of feasible spacer concepts was made. Ferrule spacers were tentatively selected as the reference design. Experiments were initiated to study brazing characteristics and corrosion resistance of various alloys that might be used to attach the ferrules. An assembly with mechanically fastened ferrules was made for flow studies. Fabrication of a number of model assemblies for test and evaluation was initiated. High-frequency weld studies

Figure 1.23 Stretch-bonding fixture with fuel rod installed.

Figure 1.25 X-rays showing pellet separation as a function of reduction. Elongation (top to bottom) $3/4$, $7/8$, 1, and 1 in.

Figure 1.24 Fuel rod with brazed ferrule and gripping fixture.

Figure 1.26 Typical vacuum brazed tee-joint interface with Coast Metal 60 alloy, 60 min. at temperature. (Magn: 50X)

were expanded to determine if this method may be used to attach spiral wire-wrap, straight wires, and thermocouple wires on tubes of various sizes.

1.6.4.1 Spacer Concepts. As reported previously, the two most practical spacers considered for the low-enrichment assemblies are clips and ferrules. A review of flow and heat transfer characteristics indicated that the ferrule-spaced assembly would provide the more optimum fuel element, and an assembly so spaced was subsequently selected for additional development. A clip-spaced assembly will be studied as an alternate.

1.6.4.2 Material Studies. Brazed tee-joint samples using Microbraz 30, GE 81, and Coast Metal 60 (Nickel-base—chrome—silicon alloys) were prepared by vacuum brazing for 30 min, and 60 min at 2150 F. Preliminary evaluation indicates that Coast Metal 60 has consistently higher peel strength as indicated by base-metal removal. Microstructure of all samples shows excellent grain growth across the bonding interface (Figure 1.26). Results from steam-corrosion tests for one week at 1000 F indicate no significant attack on any of the alloys. These tests are continuing.

During the next period, assembly mockups, and sample joints, will be prepared to determine optimum brazing conditions and alloys. Results of this work will provide a basis for selecting reference materials and for determining final reference process parameters.

1.6.4.3 High-Frequency Welding. The high-frequency welding process is being studied extensively to determine the feasibility of using the process for attaching straight wire spacers to small diameter stainless steel tubing. In addition, studies are being made to determine the feasibility of attaching wires to larger tubes and to investigate the feasibility of continuously welding spiral wire spacers to small-diameter thin-walled tubing.

Attachment of Straight Wires to 0.220-in Dia. Tubing. Work continued on the attachment of straight 0.054 in. dia. spacer wires and 0.062 in. dia. thermocouple wire sheaths to 0.220 in. dia. tubing. Weld speeds were increased to a maximum of 5 ft/min (compared to 0.83 ft/min previously) and the length of the ground shoe was decreased by 50 per cent. This resulted in a more axially and radially concentrated heat pattern. As a result, the heat affected zone on the tube was only 1/32-in wide, and the rest of the tube did not discolor. The effects of the reduced conduction obtained with increased weld speed was also apparent in the wire. The surface heat (2550 F at the weld point) does not conduct to the wire core, and the wire is barely discolored.

The success of the above experiment prompted another attempt to attach 0.036 in. dia. spacer wires to the fuel tubes. In previous attempts, the wire overheated and welds were unsatisfactory. With the modified fixture, successful welds were made at 4.5 ft/min with a 2-lb weld force.

Attachment of Spiral Wire Spacer to 0.220-in Dia. Tubing. A feasibility run was made to determine whether a continuous weld could be made between 0.220-in dia. tubing and a 0.036-in dia. spiral wound wire spacer. The weld was made at a weld speed of 4.5 ft/min with a 2-lb weld force, and a 6-in spiral lead. The weld quality was satisfactory but considerable radial distortion of the tube occurred. A second run will be made using fuel bearing elements. This is expected to reduce the radial distortion due to the high compressive strength of the fuel.

Attachment of Thermocouple and Spacer Wires to 0.875 in. Dia. Tubing.

The feasibility of attaching spacer wires and thermocouple wires to 0.875-in dia. tubing with wall thicknesses to 0.076 in. was demonstrated. A new ground shoe and changes in the electrical tank circuit were made to obtain the required frequency and power level for this operation.

Three 0.054-in dia. symmetrically located spacer wires were welded to a 5-ft long 0.875-in dia. tube with 0.035-in thick wall. Total axial distortion was 1/16 in. There was no radial distortion, and dimensional variations were less than 0.001 in. Weld penetration in the tube and wire was 0.002 in.

One problem was encountered when welding at the higher power level necessary for the larger tubing. This involved localized fusion spots on the surface of the wire. The depth of the localized spots did not exceed 0.002 in., and had no effect on the electric continuity in the thermocouple wires. Preliminary investigation indicates that excessive current density is causing periodic overheating where the wheel contacts the wire surface. The frequency and input power are being studied to determine corrective measures.

Future Work. A new, more versatile weld fixture, which has been built, will be installed and evaluated during the next quarter. The new fixture will provide increased control over alignment and weld parameters.

1.6.4.4 End-Cap Welding. A review of all data obtained from the end-cap welding program showed that successful welds could be made on the 0.010-in thick walled tubes using the straight shoulder cap. However, very slight variations in joint fit, cleanliness, arc location, and amperage often resulted in peel-back. Consequently, a program was initiated to study different end-cap designs that would improve heat balance and reduce the hypersensitivity to the welding variables.

Two types of caps were studied, a recessed cup-cap and a reduced-shoulder cap. Results showed that high quality welds could be produced with both caps much more easily and consistently than with the straight shoulder cap. Studies thus showed that modifications in end cap design can greatly reduce sensitivity to weld variables.

Recessed Cup-Cap. A series of welds were made to determine optimum amperage and welding characteristics with a recessed cup-cap. Welds (Figure 1.27) were made using 6 to 9 amp at 14 in/min. Optimum amperage from the standpoint of appearance and penetration was 7 amp.

Metallographic examination showed that weld penetration at 7 amp equaled the wall thickness. Burst tests on representative samples showed failure in the cladding, and not in the weld in all cases tested.

A second type of recessed cup-cap investigated was similar to the one previously described except for a center projection that is provided

Figure 1.27 Typical welds with recessed cup-cap.
Welds (left to right): 9, 8, 7, 6 amp.

Figure 1.28 Typical welded recessed
cup-cap with projection.

for subsequent assembly of the element. Welding had to be performed at an angle of 30° to the tube axis in order to clear the projection. Results of the experimental welds were very satisfactory (Figure 1.28). Amperage to achieve sealing ranged from 8 to 11 amp with optimum amperage of 9. Minimum penetration in all acceptable welds was at least the thickness of the wall.

The tests thus showed that the recessed cup-cap is a practical design for Pathfinder low-enrichment fuel rods. The insensitivity of the design to weld variables makes it particularly well suited for production.

Reduced Shoulder Caps. Since many fuel element designs may prohibit the use of the recessed cup-cap, work continued on a cap with a reduced shoulder section (Figure 1.29). A series of welds was made with this end cap. Results showed that this cap is much better than the conventional straight shoulder cap previously used. Weld quality was consistently sound over an amperage range of 17-1/2 to 27-1/2 amp. Optimum amperage in the runs was 25 amp. Typical weld is shown in Figure 1.30.

Future Work. During the next quarter, extensive runs with each of the modified caps will be made. Additional work will be done on the straight shoulder caps. Both empty and fuel-bearing rods will be used. Results of this work is expected to lead to selection of a reference end-cap design for the Pathfinder low-enrichment superheater fuel rod.

Figure 1.29 Reduced shoulder cap.

Figure 1.30 Typical weld with reduced shoulder cap.

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2. REACTOR MECHANICAL STUDIES

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2.4 CONTROL RODS, GUIDE TUBES, AND CONTROL ROD DRIVES

The objectives of this project are to fabricate and test a full-scale prototype control rod drive unit to experimentally determine the rate of movement, mechanical reliability, accuracy of position indicators, and scram operation under simulated Pathfinder operating conditions. Incident to these objectives are the development of a shaft seal to minimize leakage of the water or steam from the drive housing, fabrication and testing of a rack and pinion to operate in steam or water, fabrication and testing of a dashpot capable of repeated operating under the impact of the control rod under simulated reactor conditions, fabrication and testing of a submersible drive mechanism to drive the rack and pinion, and testing the mechanical operation of the superheater control rod.

2.4.1 CONTROL ROD DRIVE

A "Gearmaster" gearmotor and extra slow speed gear box were ordered for testing. The gear box will be used for the 8 inner boiler drives, which will be driven at about 26 in/min.

A new rack was machined straight within 0.0025 in. with all teeth parallel within 0.002 in. After heat treating at 1100 F for 4 hr, the rack was straight within 0.0065 in.

The prototype drive is being reassembled with the new rack, pinion, and backstop clutch, and tests are planned for the next quarter.

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3. NUCLEAR ANALYSIS

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3.1 REACTOR PHYSICS (STATICS)

The objective of this project is to perform physics calculations such as computer programming and operation, and to determine the critical mass, neutron and gamma flux and power distribution, enrichment, coefficients of reactivity, control-rod effectiveness, and conversion ratio with respect to a Pathfinder core with an integral high-enrichment superheater region. An additional objective is to determine shielding requirements for the Pathfinder Plant.

3.1.1 CONTROL ROD PROGRAM DURING REACTOR STARTUP

The effect of boiler control rod configuration on radial power distribution and core reactivity for the operating voided condition was discussed in Allis-Chalmers Report ACNP-6117. During the past quarter, an analysis was begun to determine the effect of boiler control rod configurations on superheater operating characteristics during startup and low-power operation.

Startup procedure requires removal of the eight outer boiler control rods before withdrawing any of the eight inner boiler or four superheater control rods. This is required to reduce the reactivity addition for a single control rod and to suppress the power production from the superheater during conditions of low steam flow.

During full power operation, the excess reactivity is controlled by rod configurations consisting of groups of eight outer boiler rods only. This minimizes the peak-to-average power in the boiler region and still allows sufficient reactivity control for the hot operating condition. The transition from operation with the outer boiler rods withdrawn to operation with them inserted is made at low reactor power.

The effect of the transition, from control using the eight inner rods to the eight outer rods, on the superheater power distribution

was analyzed using PDQ-XY calculations. Assumed was a reactor at operating temperature and pressure ($T_{\text{mod}} = 489 \text{ F}$) with no equilibrium poisons, no voids in the boiler, and all superheater steam passages voided. Control rods within the boiler and superheater were represented explicitly using rod constants discussed in previous quarterly technical progress reports. Calculations were made for the following three control-rod configurations:

- 1) All control rods are fully inserted except eight outer boiler rods which are fully withdrawn.
- 2) All rods are fully inserted except four inner boiler control rods which are fully withdrawn.
- 3) All rods are fully inserted, except 8 fully withdrawn inner boiler rods.

Results of these calculations are shown in Table 3.1. There is a significant increase in superheater power fraction and peak-to-average power after the eight outer control rods have been inserted. However, for that case where all of the inner boiler rods are withdrawn, the peak-to-average power in the superheater does not increase.

Table 3.1 EFFECT ON CONTROL ROD PROGRAM FOR THE BOILER CORE ON SUPERHEATER PERFORMANCE DURING REACTOR STARTUP

Control Rod Configuration	K_{eff}	Fraction of Core Power	Peak-to-Average Power
1	.999	.0696	2.18
2	1.013	.0954	2.48
3	1.056	.0923	2.18

Throughout this analysis, the superheater power fraction was suppressed by keeping the superheater control rods fully inserted. These will not be removed until sufficient steam flow has become available. Configuration 3 represents a significant difference in reactivity control from the previous configurations, but it does represent the distribution that would exist in the lower core below the partially withdrawn four inner control rods.

Analysis of partially inserted control rod configurations will be made during the next quarter. Withdrawal of the superheater rods will also be examined during the next quarter.

3.2 REACTOR AND SYSTEM DYNAMIC ANALYSIS

The objectives of this project are to perform reactor transient analysis under normal operation and after credible or hypothetical accidents involving equipment failure or reactor misoperation, as well as a system stability analysis using an analog simulator or digital computer.

3.2.1 PATHFINDER TRANSIENT SIMULATOR

The following experiments were performed on the Pathfinder transient simulator during the quarter.

Safety Valve Chattering. In order to determine the effects of blow-down settings⁷ for the safety valves, an experiment was performed on the simulator. The pressure control system was placed on manual control, and reactivity was added at the rate of 5¢/sec in ramp fashion until a total of 50¢ was inserted. The initial power load was chosen as 77 per cent, so that the safety valve would initially relieve at approximately 100 per cent power. A 0.2-sec simple lag was included in the safety-valve circuit to simulate the valve-opening time lag.

Since the turbine inlet valves (on manual) remain in the same position during the transient, the line pressure rose steadily to the relief point of the first safety valve, 608 psia. Only the first safety valve opened (and chattered) in this experiment. Steam flow rates for three different blowdown settings are shown in Figure 3.1. It can be seen that chattering will occur with each blowdown setting for this particular transient, but the frequency of chattering is inversely proportional

7. A blowdown setting is defined as the pressure difference between safety-valve relief pressure and safety-valve reclosing pressure.

δW_s

Figure 3.1 Safety valve chattering test. Pressure control system is on manual, $n_o = 77$ per cent, $W_{vo} = 100$ per cent; control rods add 50 cents at a rate of 5 cents/second. Safety valves first relieve at 100 per cent power. (A-C Dwg. 43-024-932)

to blowdown setting at a given power level. As a result of this experiment, the Pathfinder blowdown safety-valve settings will be set at 45 to 50 psi.

System Response to Manual Pressure-Set-Point Changes. A computer experiment was performed to determine a reasonable ΔP set-point change during pressurization. With the reactor initially operating at about 10 per cent power with 100 per cent recirculation flow, step changes of steam-line set-point were inserted.

Figure 3.2 shows that a change of 5 psi in the set-point does not result in excessive superheater temperatures. The superheater fuel hot-spot temperature rises to a maximum of 1010 F in this case.

Accident Studies. Several experiments were performed on the simulator to investigate the adequacy of the reactor protection system during various incidents. These investigations included the stuck-dump-valve incident, feedwater-flow-rate and feedwater-temperature disturbances, and withdrawal of the most valuable rod at several initial power levels.

The computer results of these studies are described in the Safeguards Report, Sections 5 and 7. They show that in each of the above cases, the Pathfinder safety system adequately protects the reactor.

3.2.2 MULTI-SECTION SUPERHEATER ANALOG

The Pathfinder superheater has been simulated in great detail by a non-linear analog with ten axial sections and a variable heat

Figure 3.2 Reactor response to a step change in P_2 set point $P_2 + T_2$ control. $n_0 = 10$ per cent, $w_{vo} = 100$ per cent, $P_{20} = 555$ psia $\Delta P_2 = 5$ psi.
 (A-C Dwg. 43-024-931)

transfer coefficient, for studying thermal transients. Results with the above model were compared with the Art-04 digital code for given inputs in power and steam flow corresponding to a turbine load dump with stuck dump valve. Results of the two methods compared very closely.

In view of the close comparison, the analog simulation will be used next quarter, because of its less costly operation, to evaluate additional superheater temperature transients.

It was found that a multisection (axial) simulation of the superheater predicted somewhat higher fuel temperatures than a one-lump simulation such as the superheater section of the Pathfinder Simulator. The error was found to increase as the speed and severity of the transient increased. During the next quarter, these studies will be completed, as follows:

- 1) Determination of the effect of the number of axial sections chosen.
- 2) Formulation of a single lumped-parameter model that conservatively computes the fuel temperature at the hot spot.

3.2.2 LABORATORY TEST ON FAST-RESPONSE THERMOCOUPLES

Testing of the first fast-response thermocouple considered for use in the Pathfinder control system has been completed. In general, the test revealed that performance of the thermocouple was inadequate because of the following reasons.

- 1) Inaccurate and unreproducible temperature measurements.
- 2) High sensitivity to electrical noise.

- 3) Bumpy response to sudden insertion in boiling water compared with smooth response of a standard bare-wire thermocouple.
- 4) Response was slower than that of a standard bare-wire thermocouple by a factor of 2.

Several other available thermocouples and resistance thermometers will be tested. An extensive test of the final transducer chosen is planned.

3.2.4 IN-CORE ION CHAMBERS

Studies have shown, as reported in the last quarterly report, that a bundle of three ion chambers and a flux-wire thimble are easily inserted manually into the in-core ion-chamber support structure. Figure 3.3 shows the supporting structure in place in the reactor. It consists of four separate sections. The lowest section extends from the bottom of the active core to the bottom of the demisters; the second section extends from the demisters up to the junction piece. The third section extends from the junction piece up to the pressure-seal vessel just below the shield-pool water level; the fourth section is the pressure seal assembly which extends above the water level.

To assemble the structure, the lower two sections are placed in the reactor vessel. The reactor vessel closure with the third section attached is lowered into position. The connection at the junction piece is adjusted from the pool surface. Then, the pressure-seal vessel is bolted in place, and the ion chamber assemblies are inserted

Figure 3.3 Ion-chamber support and guide structure.
(A-C Dwg. 43-002-464)

through their respective guide tubes. Finally, the pressure seal is made at the pressure-seal vessel at the pool surface. To disassemble the structure, the entire process is reversed.

In the process of design at this time is a tool that will be used to insert the flux-calibration wire into the flux-wire thimbles. Also being designed is a wire scanning system to be used in conjunction with counting the activated flux wire.

The instrumentation system for the ion chambers is shown in Figure 3.4. A total of three separate channels each consisting of a chamber power supply, amplifier, and recorder will simultaneously monitor any three chambers. No range changing devices are needed on the amplifiers since the ion-chamber sensitivity limits their usefulness to about the top two decades of power operation.

Figure 3.4 In-Core ion-chamber instrumentation block diagram and ion chamber.
(A-C Dwg. 43-024-933)

3.3 CRITICAL EXPERIMENTS

The objectives of this project are to conduct critical experiments to determine the nuclear characteristics of the selected fuel geometries for various fuel loadings with enrichment, moderator-to-fuel ratio, etc., as parameters and to determine flooding coefficient, conversion ratios, critical mass, neutron flux and power distribution, control rod worth, enrichment requirement, resonance escape probability, thermal utilization, and critical buckling for a Pathfinder core with an integral high-enrichment superheater region. Incident to these studies are the preparation of such safeguards reports for the facility as are necessary to meet the licensing requirements for the AEC.

In addition, the experiments will be used to determine the worth of reactivity changes in the boiler and superheater regions and relate the results to the kinetic behavior of the system. The results of the experiments will be used in the calculations analyzing the operating reactor.

3.3.1 FULL-CORE TEMPERATURE COEFFICIENT

Temperature coefficients have been calculated for the reference-Pathfinder clean, unrodded core over the operating temperature range of 68 to 489 F. The effects of control rod insertion on the temperature coefficient have been computed, and these results are presented in Section 3.1 of ACNP-6123. To provide data that could be used to check the methods used in these calculations, temperature coefficient measurements were made in the critical facility mock-up, over the limited temperature range of 60 to 150 F, with rodded and unrodded, uniformly poisoned, cores. The methods and results of these measurements are described in the following.

The core configuration and loading used for the rodded temperature coefficient measurement is described by Figure 3.5. The presence of 80 low-enrichment rod-type superheater fuel elements in the center of the normal loading of 428 high-enrichment superheater elements will

ITEM	BOILER CORE	SUPERHEATER CORE
NO. OF ELEMENTS	96	428
ELEMENT ASSEMBLY	8X8 PINS/BUNDLE-UPPER HALF 9X9 PINS/BUNDLE-LOWER HALF	348-2 FUEL TUBES 80-7 ROD CLUSTERS
ENRICHMENT	1.89 AVERAGE	348-93.5% U235 80- 3.5% U235
LATTICE POISON	NONE	SS POISON RODS IN 93.5% ELEMENTS
VOID DISTRIBUTION	NONE	FLOODED ELEMENTS ● UNFLOODED ELEMENTS ○
CRITICAL POSITIONS RR*1 24.3" OUT RR*2 NOT USED RR*3 24.4" OUT	SIMILAR TO EXP* RUN*	NOTES: 3.5% SUPERHEATER SECTION IS CROSSHATCHED

Figure 3.5 Core layout for full-core temperature-coefficient measurement with rodded core. (A-C Dwg 43-024-040)

have a negligible effect on the measured temperature coefficient. Moderator heating was achieved by immersion heaters which raised the core temperature from 60 F to 150 F in about 18 hours. The water was continuously agitated to assure a uniform temperature in the core. The temperature was measured with thermocouples and resistance thermometer bulbs located in the core. The measurement of positive reactor periods produced by withdrawing a regulating rod to a fixed supercritical position were made as a function of temperature. This yields an incremental reactivity measurement of the rod displacement which was independent of rod calibration.

Figure 3.6 Critical facility full-core temperature coefficient with rodged core. (A-C Dwg. 43-024-910)

ITEM	BOILER CORE	SUPERHEATER CORE
NO. OF ELEMENTS	96	428
ELEMENT ASSEMBLY	8 X 8 PINS/BUNDLE-UPPER HALF 9 X 9 PINS/BUNDLE-LOWER HALF	348-2 FUEL TUBES 80-7 PIN CLUSTERS
ENRICHMENT	1.89 AVERAGE	348-93.5% U235 80- 3.5% U235
LATTICE POISON	AVERAGE OF 4.42 B-AI SHEETS	SS POISON RODS IN 93.5% ELEMENTS
VOID DISTRIBUTION	NONE	FLOODED ELEMENTS ● UNFLOODED ELEMENTS ○
CRITICAL POSITIONS RR*1 NOT USED RR*2 30.0" OUT RR*3 30.0" OUT	SIMILAR TO EXP* RUN*	NOTES: 1, 5 B-AI SHEETS 2, 3 B-AI SHEETS 3, 3.5% SUPERHEATER SECTION IS CROSSHATCHED

Figure 3.7 Core layout for full-core temperature-coefficient measurement with unrodded core. (A-C Dwg. 43-024-039)

Reactivity change and temperature coefficients are plotted as a function of temperature in Figure 3.6. The measured coefficient is -3.6×10^{-3} percent $\Delta k/k/F$ at 85 F and -5.1×10^{-3} percent $\Delta k/k/F$ at 145 F.

Following this measurement, about 1700 thin (0.01 in. thick) boron-aluminum poison sheets were distributed throughout the boiler core allowing a criticality to be achieved with 18 rods fully withdrawn and two rods partially withdrawn. This unrodded core is described in Figure 3.7. The experiment was performed as before and the results are given in Figure 3.8. The measured coefficient is -0.3×10^{-3} percent $\Delta k/k/F$ at 85 F and -1.0×10^{-3} percent $\Delta k/k/F$ at 145 F.

Figure 3.8 Critical facility full-core temperature coefficient with unrodded core. (A-C Dwg. 43-024-911)

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POST CONSTRUCTION R&D

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1.0 INITIAL STABILITY AND PERFORMANCE TESTS

The objectives of this project are to design and fabricate a special oscillator control rod and drive mechanism together with suitable instrumentation and recording equipment to measure and record the resulting variations in neutron flux; and to conduct oscillator tests to verify dynamic performance calculations and to determine experimentally the stability of the reactor system.

1.01 OSCILLATOR ROD

The oscillator rod and drive is designed to produce sinusoidal reactivity changes in the core. This assembly is shown in Figure 1.01. The reactivity change is obtained by rotating a can with two strips of cadmium on it, inside a can with two sine-shaped cadmium strips. The stator can is supported on the grid plate. The rotor can is driven through a shaft by a motor with a variable speed transmission and is supported by the same support as the stator can through a bearing, which has been named the bottom bearing. The motor housing is supported by a flange on the vessel cover. A tower that is supported by the base plate of the motor housing, in turn, supports a mounting plate. The mounting plate serves as a connection for the electrical cable and tubing for the oscillator rod. The oscillator rod, when in use in the reactor, occupies one of the positions normally used for an internal boiler control rod.

Tests are being conducted to determine a suitable bottom bearing for the oscillator rod.

1.02 PATHFINDER INITIAL STARTUP TEST PROGRAM

The initial startup program consists of a series of tests designed to demonstrate the physics and systems performance of the integral boiler-superheater reactor. These tests are sequenced in their order

Figure 1.01 Oscillator rod and drive.
(A-C Dwg. 43-501-096, Sheet 1)

Figure 1.01 Oscillator rod and drive.
(A-C Dwg. 43-501-096, Sheet 2)

of increasing reactor power levels. Results from the design analysis and critical facility experiments are utilized in this program.

The initial reactor loading is made in the dry condition. Core structure, fuel boxes, control rods, and shims are loaded. The reference fuel is then loaded into the superheater and boiler progressing radially. With this poisoned loading and a safety increment of about 3 per cent $\Delta k/k$ of control cocked, the moderator level is raised to full height. If monitoring shows the possibility of criticality before full moderator height is reached, an increment of fuel will be removed from a dry core, and the operation will be re-initiated.

In Phase I tests, the reactor is brought to critical operation by control rod withdrawal. The reference core is established by manipulations of reactivity shims. Calibrations of the nuclear instruments and the reactor control system are performed. Reactivity changes associated with different reactor operations are measured.

Further operational reactivity changes are measured during Phase II and steam is produced by the reactor. The operation of the superheater and the process systems is investigated under these conditions. Nuclear instrumentation is again calibrated at these power levels and reactor fluxes are measured. The operational stability of the reactor is evaluated by means of an oscillator rod.

Reactor power is increased in levels to full power operation during Phases III and IV. At each level, further measurements of reactivity, and reactor flux and power are made. Superheated steam is produced by

by the reactor, and this mode of operation is evaluated. Plant radiation measurements and reactor stability are evaluated at the different power levels.

1.03 PATHFINDER POST-CONSTRUCTION PHYSICS TESTS

A preliminary list of the tests is given below.

Initial Core Loading

Phase I - 10 kw(th) or less

1. Initial Criticality
2. Initial cold core flooding coefficient.
3. Nuclear instrumentation calibration.
4. Establishment of the reference core.
5. Reference core temperature coefficient.
6. Reference core cold flooding coefficient.
7. Control system calibration.
8. Cold core pressurization.

Phase II - 5 mw(th) or less

1. Non-boiling temperature coefficient
2. Hot-core flooding coefficient.
3. Superheater radiative cooling ability.
4. Power coefficient.
5. Fluid dynamics reactivity effects.
6. Flux map.
7. Transfer function.
8. Process systems tests.

Phase III - 100 mw(th) or less and Phase IV - Full-Power or less

1. Initial power increase.
2. Power calibration.
3. Water level calibration.
4. Superheated steam operation.
5. Response to scram and runback.
6. Fluid dynamics reactivity effects.
7. Flux maps.
8. Radiation surveys.
9. Ion chamber noise analysis.

10. Transfer function.
11. Xenon reactivity.
12. Process systems tests.

A format for the tests has been established and detailed drafts of the initial core loading and the initial criticality procedures are completed.

4.0 MECHANICAL TESTS

The objectives of this project are to provide for the irradiation of samples of reactor structural materials, e.g., materials used in the superheater container tubes and reactor vessel, and periodically examine them for embrittlement. Additional objectives are to provide instrumentation for determining the liquid level within the reactor and for determining steam quality at the superheater inlet, and to conduct pre-startup tests on flow distribution within the reactor vessel and such other tests as may be agreed upon.

4.1 Superheating Calorimeter. In order to measure the quality of the steam coming out of the dryers, a superheating calorimeter was designed. Fabrication of the instrument is complete and testing is in progress. Evaluation of the test results will determine whether it meets design objectives.

4.2 Superheater Thermocouple Lead Connectors. The thermocouple leads from the superheater fuel element must be disconnected to re-fuel the boiler core. A disconnect plug has been designed and built to operate in the reactor steam dome. The thermocouple sheath is sealed to the plug and the electrically insulated jacks are sealed to the plug, thus preventing steam and water from entering the thermocouple sheath. A fabrication specification for the disconnect plugs and the necessary thermocouple lead wires has been prepared.